TRANSACTIONS
OF THE
AMERICAN INSTITUTE
OF
ELECTRICAL ENGINEERS

COLLEGE OF ENGINEERING
1[illegible] FEB 196[illegible]
GUINDY

VOLUME 68
1949
PART II

PUBLISHED BY THE
AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS
33 WEST 39TH STREET
NEW YORK 18, N. Y., U. S. A.

COPYRIGHT 1949
BY THE
AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Printed in the United States of America by
The Mack Printing Company, Easton, Pa.

Preface

The 1949 Transactions (volume 68) is published in two parts. The contents of Part I are listed on the preface page in Part I of the 1949 volume.

Part II contains technical program papers and related discussions presented at the General and District Meetings during the second half of the calendar year as follows:

1. Summer General Meeting, Swampscott, Mass., June 20–24, 1949.*
2. Pacific General Meeting, San Francisco, Calif., August 23–26, 1949.
3. Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949.

* Forty-two Summer General Meeting papers were published in Part I and the remainder are included in Part II.

Part I of volume 68 contained an index covering only the first half of the year. A complete index for 1949 (Parts I and II) is included in this volume beginning on page 1429. The Report of the Board of Directors, and the list of Officers and Committees for 1949–1950 also are included in Part II.

Statements and opinions given in papers and discussions published in TRANSACTIONS are the expressions of contributors for which the American Institute of Electrical Engineers assumes no responsibility.

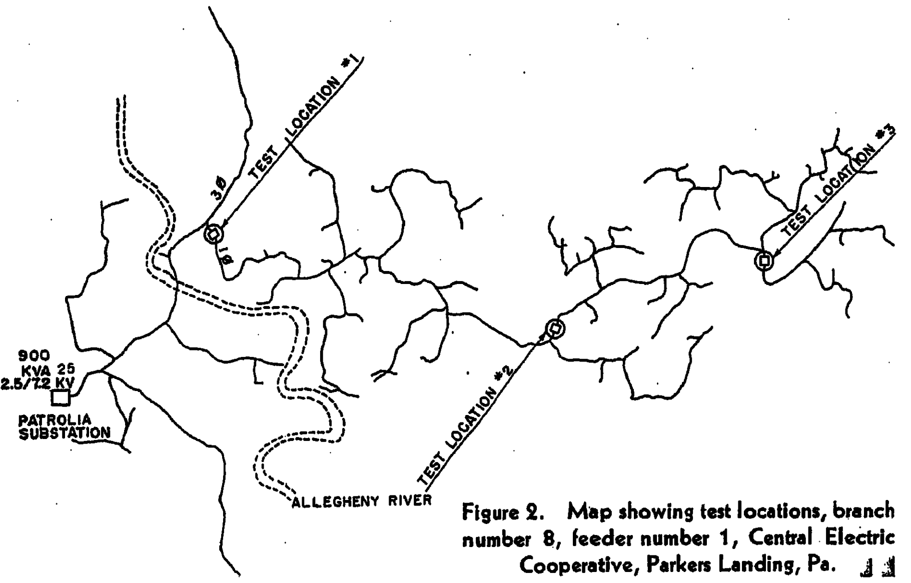


Figure 2. Map showing test locations, branch number 8, feeder number 1, Central Electric Cooperative, Parkers Landing, Pa.

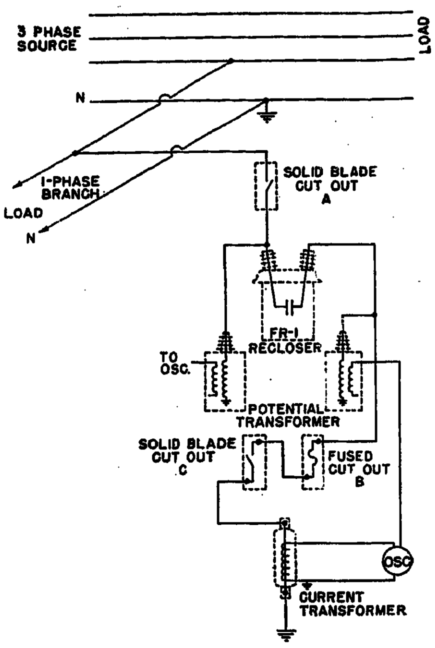


Figure 3. Test circuit diagram

The time-delay characteristic *B* and *C* of the recloser is plotted to both minimum and maximum test points, and, hence, all time-delay opening operations of the recloser will fall within the band of variations.

The minimum time-delay curve *B* serves as a basis for determining the co-ordination of the recloser to the total-clearing characteristics of fuses, to assure that permanent faults will be cleared by load-side fuses during the third or fourth (time-delay) opening operation before the recloser locks open.

The maximum time-delay curve *C* serves as the basis for determining the ability of the recloser to prevent operation of overcurrent relays or fuses on the source side of the recloser during its third and fourth (time-delay) opening operations to lock open.

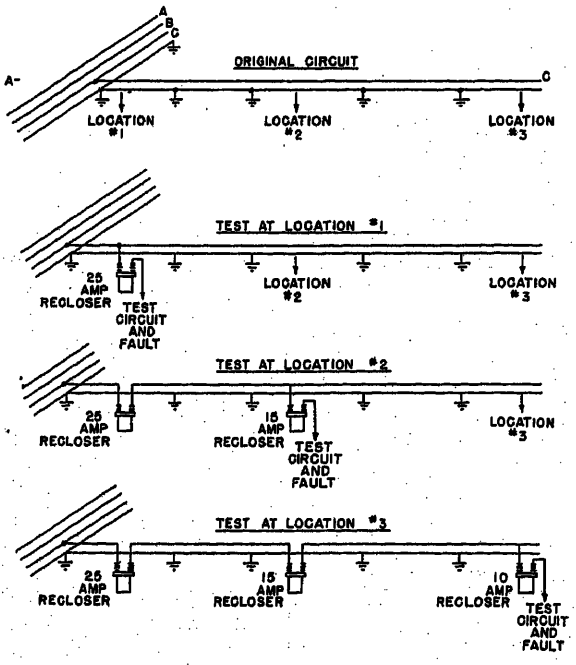
Total Heat Input to a Fuse Link During the Time Required for the Recloser to Open and Interrupt the Fault Current

The time–current characteristic curves of a recloser can be factored to include heating of a fuse link when comparing with time–current characteristics of fuses.

Let us assume that we wish to prevent a fuse from blowing during two instantaneous openings of the recloser when clearing temporary faults. We must consider the total amount of heat received by the fuse during these two instantaneous openings of the recloser. By doubling each time value on the instantaneous characteristic curve *A*, we arrive at a modified curve which represents the total length of time that short-circuit current flows through the fuse link during the first two recloser openings.

Assuming a permanent fault beyond the fuse, proper co-ordination requires that the fuse melt before the recloser locks open. In this case, we must determine the minimum heat input to the fuse link during the two instantaneous plus two time-delay openings of the recloser, to be sure that it is sufficient to melt the fuse before the recloser locks open. By doubling the time values on both the instantaneous characteristic curve *A* and the minimum time-delay characteristic curve *B* and adding the two together, we arrive at a modified curve which represents the minimum length of time that short-circuit current flows through the fuse link during four recloser operations.

In co-ordinating the recloser with a source-side fuse, it is necessary that the four operations of the recloser to lock open will not cause partial melting of the fuse. In this case, our modified curve would be equal to the sum of two instantaneous openings, curve *A*, plus two time-delay openings, curve *C*. This final curve then represents the maximum or total length of time that short-circuit current is permitted to flow through the fuse link.



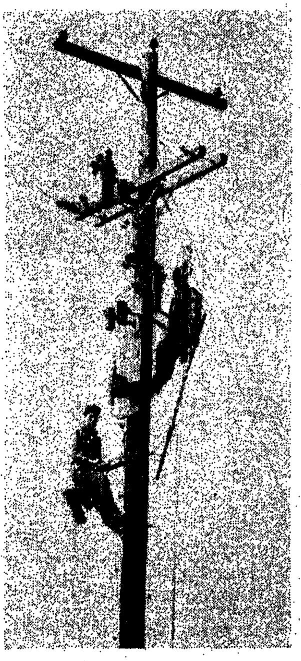


Figure 3(A). (left) Circuit diagrams for each test location

Figure 3(B). (right) Test location number 1

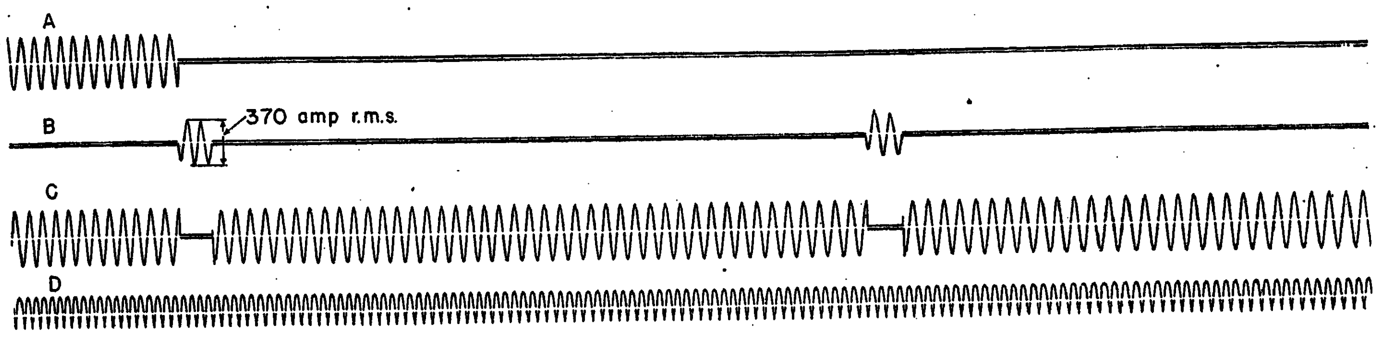
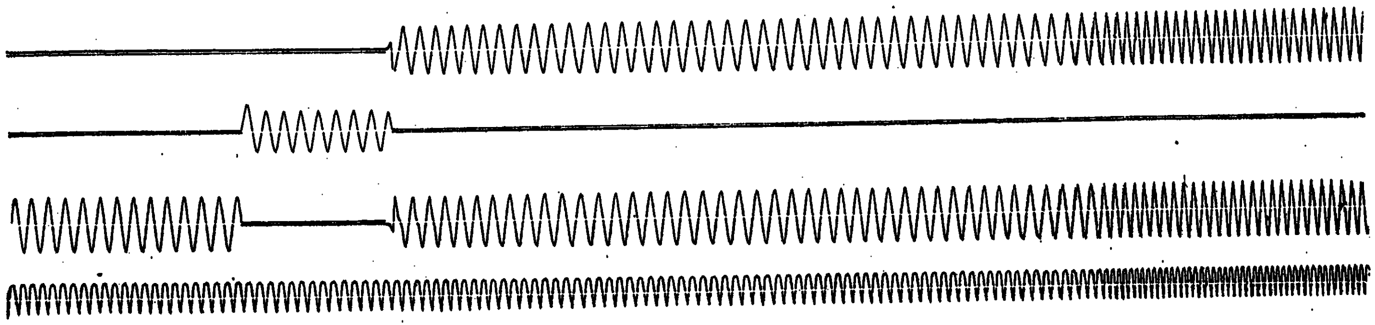


Figure 4. Oscillogram (in two parts) showing co-ordination of 25-ampere recloser with 50-ampere fuse

Curve A—Recloser load side voltage, 120 volts
Curve B—Short-circuit current, 370 rms amperes
Curve C—Recloser source side voltage, 120 volts
Curve D—Timing wave, 100 cycles



Allowance of 25 Per Cent in Melting Time–Current Characteristics of Fuses to Factor Operating Variables

The time–current characteristic curves of fuse links are based upon no initial load. Whenever fuses are preheated by load current, a 15 per cent reduction in melting time should be taken into consideration. An additional reduction of 10 per cent must also be included to take into account the progressive melting of the fusible element and the strain wire.[1] Therefore, the over-all factor of safety should be a 25 per cent reduction of the melting time–current characteristic of the fuse to assure that the fuse will not be

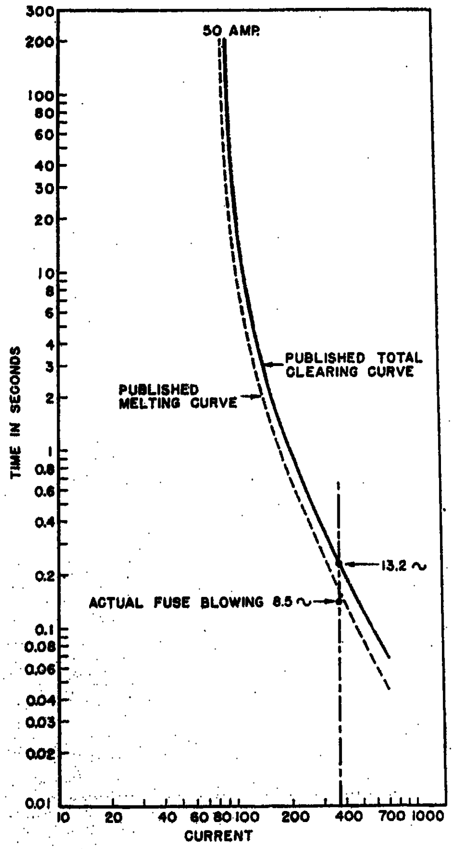


Figure 5 (left). Time-current characteristics of 50-ampere fuse link

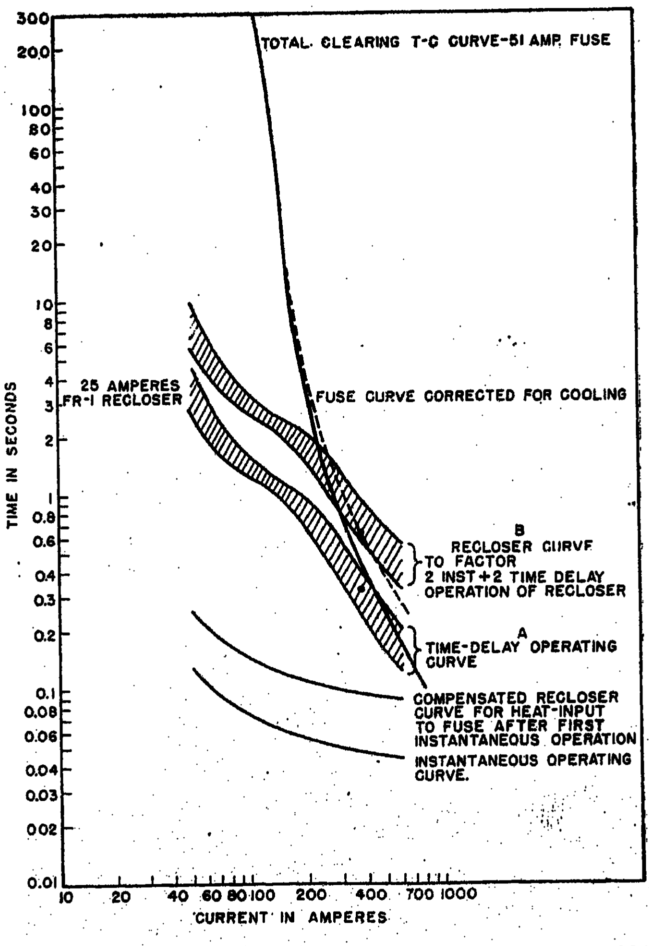


Figure 6 (right). Analytical co-ordination method of comparing characteristics of reclosers and fuses partially melted by the fault current during any instantaneous openings of the recloser.

Cooling of the Fuse Link During Intervals that the Recloser is Open

Each successive opening of the recloser is followed by an interval of approximately 40 cycles before reclosing. During this interval there is no current flowing through the fuse, and, consequently, the fusible element has an opportunity to cool. As a result, the fuse will be somewhat slower in melting than in the case where no cooling is permitted. The additional increment of time for cooling, which can be calculated, must be added to the melting and total clearing time–current characteristics of the fuse.

Test Circuit

This basic procedure for recloser-fuse co-ordination was substantiated by a series of field tests performed on a typical rural distribution circuit located on the Central Electric Cooperative System at Parkers Landing, Pa.

The circuit selected for these tests, Figure 2, was a 7,200-volt single-phase branch line approximately 25 miles long with three sectionalizing points. The first sectionalizing point, where the single-phase line tapped off the main 3-phase line, was six miles from a 900-kva substation. The maximum calculated fault current at this location was 430 amperes line-to-neutral. The second sectionalizing point was 12 miles from the first location; the calculated fault current was 140 amperes. The third and last sectionalizing point was eight miles from the second location, and the calculated fault current at this point was 90 amperes.

To perform these tests in daylight hours under maximum generation on the system and under normal voltage and load conditions, a test circuit was used, as shown in Figure 3. This permitted application of faults with a solid-blade enclosed cut-out and clearance of fault by fuse, or by recloser operation without any prolonged service interruption.

This test-circuit equipment was portable and was moved from one location to another.

Figure 3(B) shows the two enclosed-type cutouts of Figure 3 mounted side by side on the test board at location *1*, Figure 3(A).

One cutout was fused for the purpose of testing the operation of fuses in series with the recloser. The other cutout with the solid disconnecting blade was used as a switch to apply the fault.

A 300/5-ampere current transformer, mounted on the test board below the cutouts, was used to energize the current element in the oscillograph.

Two standard distribution transformers, 7,200/120-240 volts, were used to provide voltage traces on the oscillogram for sequential operation study; one was connected on the source side of the re-

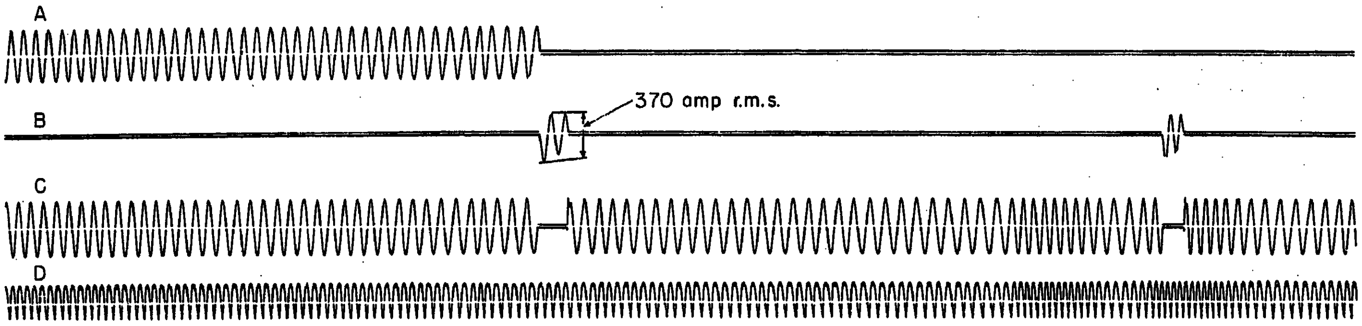
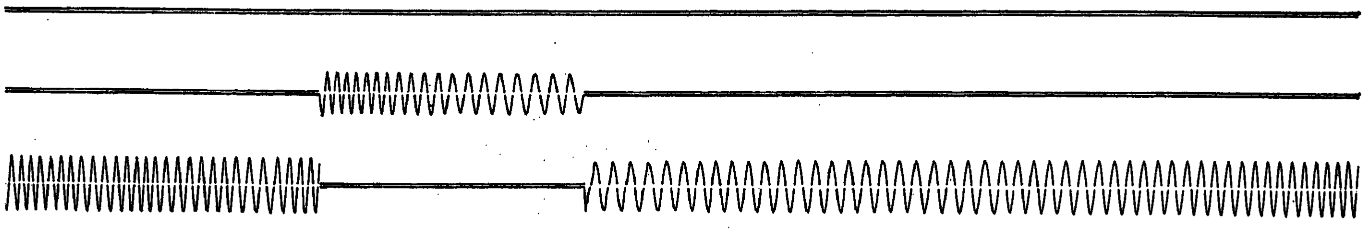
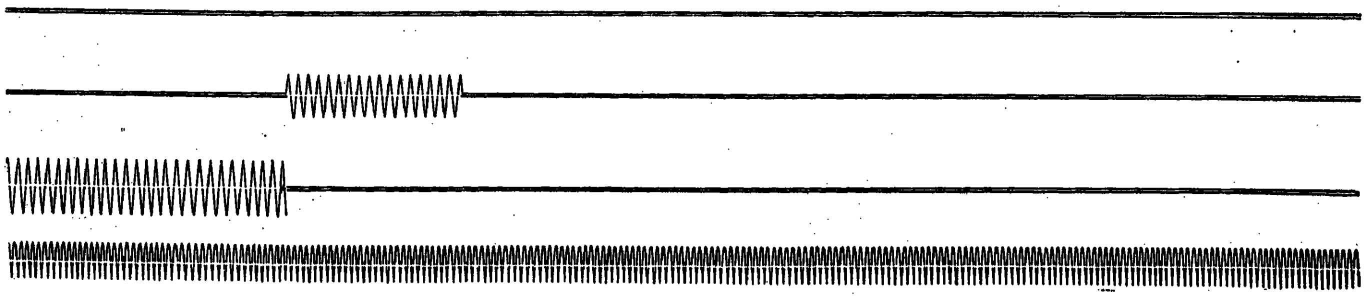


Figure 7. Oscillogram (in three parts) showing co-ordination of 25-ampere recloser with 51-ampere co-ordinating fuse link (equivalent to 75N fuse link)



Curve A—Recloser load side voltage, 120 volts
Curve B—Short-circuit current, 370 rms amperes
Curve C—Recloser source side voltage, 120 volts
Curve D—Timing wave, 100 cycles



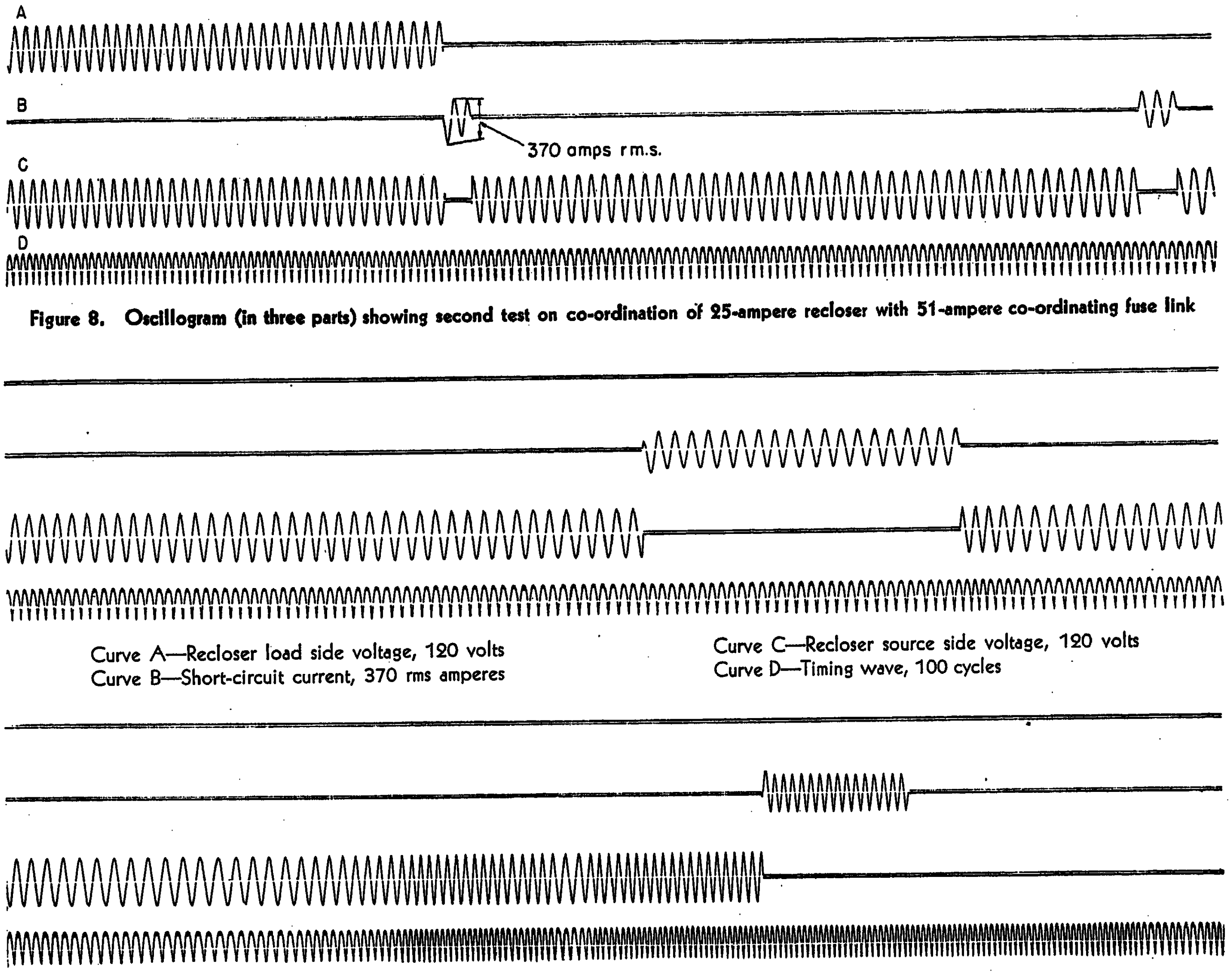


Figure 8. Oscillogram (in three parts) showing second test on co-ordination of 25-ampere recloser with 51-ampere co-ordinating fuse link

Curve A—Recloser load side voltage, 120 volts
Curve B—Short-circuit current, 370 rms amperes
Curve C—Recloser source side voltage, 120 volts
Curve D—Timing wave, 100 cycles

closer, and the other was connected to the load side of the recloser. Because of congestion on the pole and also to improve safety to the linemen, these transformers were omitted from the tests at locations *2* and *3*. A 100-cycle tuning fork was used to record the reference trace at these latter locations.

The ratings of the reclosers used at test locations *1*, *2*, and *3* were selected on the basis of the fault current and load current, resulting in a 25-ampere recloser at location *1*, a 15-ampere recloser at location *2*, and a 10-ampere recloser at location *3*.

The ratings of fuses used in these tests were selected on the basis of the procedure previously described for recloser-fuse co-ordination which took into consideration heating and cooling of the fuse link.

At location *1*, with the test circuit as shown in Figures 3 and 3(A), a 50-ampere fuse link was installed in cutout *B*, to be tested for co-ordinated series operation with the 25-ampere recloser.

Recloser-Fuse Co-ordination Test

At a predetermined time established by the oscillographer, cutout *C* was closed to apply a permanent fault from line-to-neutral on the test circuit.

The oscillogram in Figure 4 shows the magnitude of fault current, the sequence of operation, and the cycle timing.

The measured fault current was 370 amperes. The 25-ampere recloser made its first instantaneous opening in 2.5 cycles; reclosed after 44 cycles; made a second instantaneous opening in 2.5 cycles; and reclosed after 41 cycles for its first time-delay operation. This first time delay of the recloser allowed the 50-ampere fuse to blow in 8.5 cycles, thereby clearing the permanent fault from the circuit.

Referring to Figure 5, the standard published total clearing time–current characteristic of the 50-ampere fuse shows that 370 amperes would melt this link in a maximum time of 13.2 cycles. However, because heat was applied for two instantaneous openings of 2.5 cycles each with 44- and 41-cycle cooling intervals, the fuse cleared the circuit in 8.5 cycles instead of 13.2 cycles during the third (time-delay) operation of the recloser.

This test was repeated twice more at this location using the same rating fuse. The results substantiated the first tests; that is, the final duration of fault current to blow the fuse was 8.5 cycles and 8 cycles, respectively.

This approximately 36-per-cent difference in fuse curve time and actual fuse blowing time emphasizes the importance of the basic procedure in evaluating the heating of a fuse link when co-ordinating reclosers to fuses.

To go a step further in this study of fuse heating plus cooling with respect to its clearing time, a 51-ampere co-ordinating link (equivalent to a *75N* fuse) was selected for the next test at location 1. Taking into consideration the heating and cooling of this link, it should blow during the second time-delay operation of the

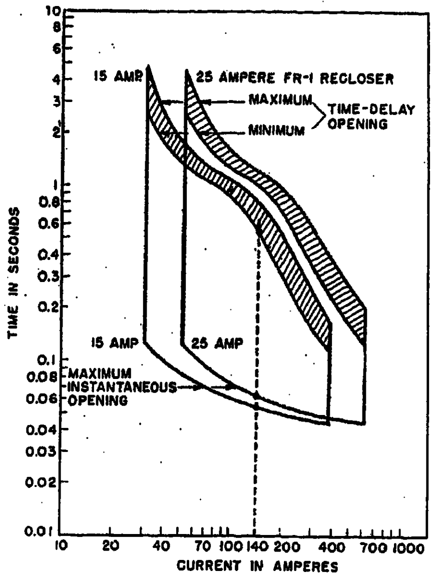


Figure 9. Comparison of characteristics of 25-ampere and 15-ampere reclosers to determine co-ordination between adjacent ratings of reclosers connected in series

25-ampere recloser; that is, the recloser should make two instantaneous openings, one complete time-delay opening, and then during the second time delay, as a result of previous heating, the fuse will blow before the recloser locks open.

Curve *A*, Figure 6, which neglects heating and cooling of the fuse link, indicates that the recloser will operate on time delay to clear the 370-ampere fault before the 51-ampere fuse blows. However, curve *B*, which factors the heat input to the fuse, indicates that before the recloser can complete its second time-delay opening, the fuse will melt and clear the fault.

The oscillogram shown in Figure 7 substantiates this latter analysis; that is, the recloser made two instantaneous openings in two cycles each, one time-delay opening in 19.5 cycles, and, after reclosing to the second time-delay operation, the fuse cleared the circuit in 17.5 cycles—making a total of 41 cycles which compares favorably with the 0.73 second (44 cycles) of the corrected fuse curve of Figure 6.

The oscillogram shown in Figure 8 is a repeat test with another 51-ampere fuse. The recloser made two instantaneous openings in two cycles and 2.5 cycles respectively, plus one time-delay opening in 19 cycles, and, after reclosing to the second time-delay operation, the fuse cleared the circuit in 16 cycles—making a total of 39.5 cycles which also compares favorably with the 0.73 second or 44 cycles indicated by the corrected curve in Figure 6.

These tests also indicate that neglecting the heating and cooling factors in a recloser-fuse co-ordination study can result in misperformances on actual installations which will cause unnecessary and prolonged circuit outages.

Recloser-to-Recloser Co-ordination Test

In reference to recloser operation, proper distribution-line fault sectionalizing depends upon the ability of one recloser to co-ordinate with its adjacent rating and all other reclosers within its range of protection. With repetitive operation of each recloser, the smallest rating adjacent to a permanent fault always must lock open to isolate the fault, just as with recloser-fuse co-ordination, a permanent fault on the load side of the fuse must always blow the fuse before the recloser locks open.

In co-ordinating adjacent ratings of reclosers having an operating sequence which involves both instantaneous and time-delay operations, two or more reclosers may operate instantaneously before the smallest rating adjacent to the permanent fault operates twice on time delay and locks open. This is inherent in all types of reclosers which operate instantaneously and arises from the fact that the instantaneous openings of the larger ratings may be faster than the time-delay opening of the smallest rating. However, if the time-delay openings are sufficiently accurate, co-ordination is not affected, because the smallest rating will always lock open and permit the larger rating to reset automatically to normal position.

It is evident that operations of reclosers other than the one nearest the fault could be prevented by decreasing the speed of the instantaneous openings. However, this seriously limits the ability of the recloser to prevent fuse blowing and conductor or apparatus damage and, therefore, greatly reduces its protective value. Figure 9 shows the opening characteristic curves of a 25-ampere and a 15-ampere recloser connected in series. With fault current of 140 amperes, both reclosers will open, either simultaneously or consecutively, at or below their curve of maximum time for instantaneous opening. After each has made its two instantaneous openings, only the smaller rating recloser will operate twice more on its time-delay characteristics to lock open. The oscillogram shown in Figure 10 shows the proper co-ordination of 25-ampere and 15-ampere reclosers connected in the circuit at locations *1* and *2*

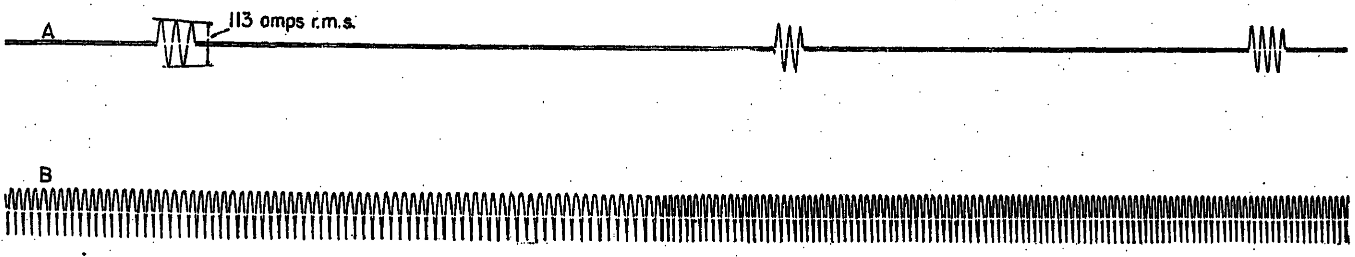
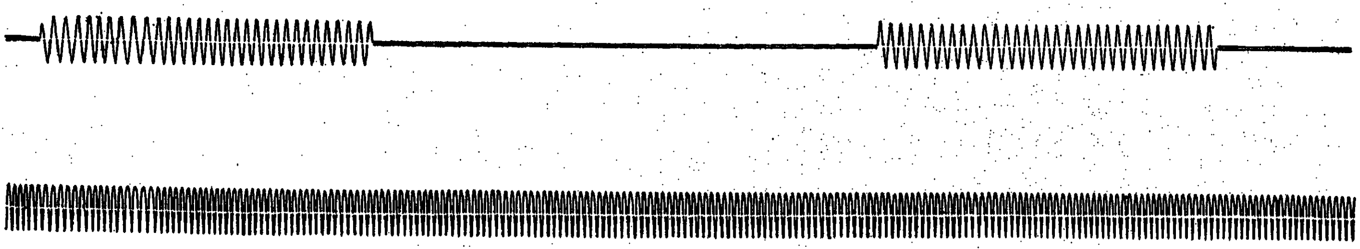


Figure 10. Oscillogram (in two parts) showing proper co-ordination of 25-ampere and 15-ampere reclosers connected in series

Curve A—Short-circuit current, 133 rms amperes

Curve B—Timing wave, 100 cycles



respectively, with a fault current of 133 amperes on the load side of location *2*. The circuit diagram is shown in Figure 3(A).

It is not possible to determine the exact sequence of operations, in view of the fact that the potential transformers could not be installed at location *2*. However, three instantaneous openings and two time-delay openings were indicated on the film.

Bearing in mind that neither recloser can operate on its time-delay characteristic until it has previously made two instantaneous openings, the five openings shown on the film might be analyzed as follows:

The first opening was made by the smaller rating (15-ampere) recloser alone; the second opening was a simultaneous opening of both 15-ampere and 25-ampere reclosers; the third opening was made by the 25-ampere recloser alone; and the fourth and fifth openings were time-delay openings of the 15-ampere recloser which locked open. The co-ordination between reclosers is accomplished during time-delay operation, and, to be successful, reliable characteristics must be inherent in the design.

The oscillogram shown in Figure 11 shows the test results with the 25-ampere recloser at location *1* in series with a 15-ampere recloser and a 30-ampere fuse at location *2*, and a fault applied on the load side of the fuse. The fault current was 133 amperes at this location. This is a typical application for a fused branch line beyond two reclosers in series. The objective, in this case, is to protect the branch fuse during the two instantaneous openings of the source-side recloser nearest this fuse and to blow the fuse before the recloser locks open. After the nearest recloser has completed two operations, instantaneous openings of other reclosers closer to the source may cause the fuse to blow.

However, the original objective has been accomplished and co-ordination is in no way affected.

This film shows three instantaneous openings, indicative of simultaneous and consecutive openings of both reclosers, and one time-delay opening which allowed blowing of the 30-ampere fuse link.

The second instantaneous opening shows clearance of the fault in one cycle, which is considerably faster than characteristic curves of either recloser indicate for 133 amperes of fault current. This fast opening, upon further investigation, was believed to have been influenced by inrush current after the 40-cycle open period. Between locations *1* and *2* were several large strip coal mines and a pumping station, each having a large number of motors in operation.

In analyzing this film, there is indication that the 25- and 15-ampere reclosers opened simultaneously when the 133-ampere fault was first applied. The 25-ampere unit reclosed first, thus energizing the line to location *2*. Inrush current to motor load in this section was of sufficient magnitude to cause partial pickup of the 25-ampere recloser. The 15-ampere recloser then reclosed; thus, the fault current added to the inrush current through the 25-ampere recloser, which was in partial pickup position, tripped in the first cycle of fault current. The 15-ampere recloser did not open in this second operation shown on the film. After this fast opening, the 25-ampere recloser had completed two instantaneous openings and would reclose on its time-delay characteristic. The 15-ampere recloser had made only one instantaneous opening; therefore the third instantaneous opening on the film was the second instantaneous opening of the 15-ampere recloser. The latter recloser then reclosed on time-delay characteristics, resulting in both reclosers being on time-delay, causing the 30-ampere fuse to blow in 25.5 cycles.

This test indicated that motor inrush currents on line sections between reclosers may affect the sequence of instantaneous openings of reclosers in series. This test also proved that possible difference in the instantaneous opening speed and variation in instantaneous opening sequence, will not affect proper co-ordination between adjacent reclosers or reclosers and fuses in clearing a permanent fault, if time-delay opening characteristics of the reclosers are dependable.

To study the performance of three reclosers in series, a 10-ampere recloser was installed at location *3*. The 15-ampere recloser was connected in series in the line at location *2*; the 25-ampere recloser was, as in the previous tests, at location *1*. Figure 3(A) shows the series connections in the 20-mile section of line.

The oscillogram of Figure 12 shows five instantaneous openings and two time-delay openings necessary to clear the permanent fault from the circuit when a 92-ampere fault current was flowing through the recloser. It is believed that the sequence of opening of the three reclosers in series as shown on the film is as follows:

The first instantaneous opening was made by the 10-ampere recloser alone; the second opening is simultaneous instantaneous operation of the 10- and the 15-ampere reclosers; the third opening is the second instantaneous operation of the 15-ampere recloser; the fourth opening is the first instantaneous operation of the 25-ampere recloser; the fifth opening is the second instantaneous operation of the 25-ampere recloser; and the sixth and

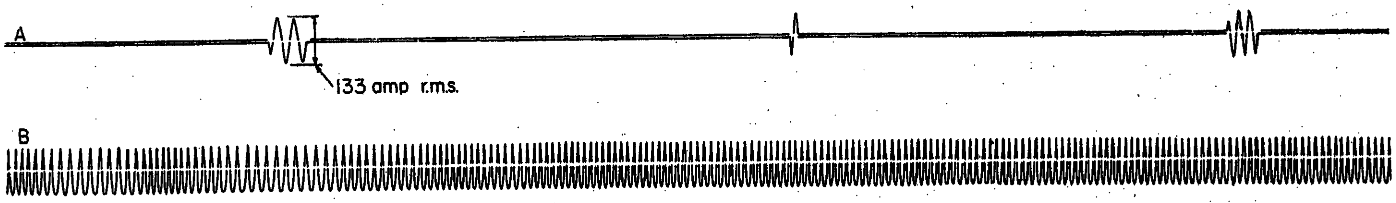
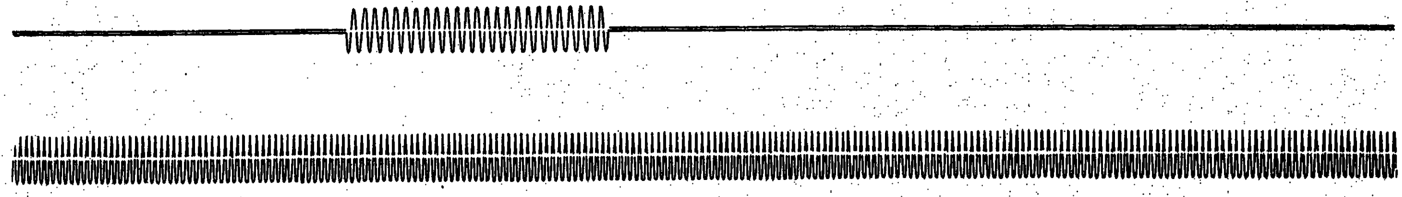


Figure 11. Oscillogram (in two parts) showing test results with 25-ampere recloser in series with a 15-ampere recloser and a 30-ampere fuse during a fault on the load side of the fuse

Curve A—Short-circuit current, 133 rms amperes

Curve B—Timing wave, 100 cycles



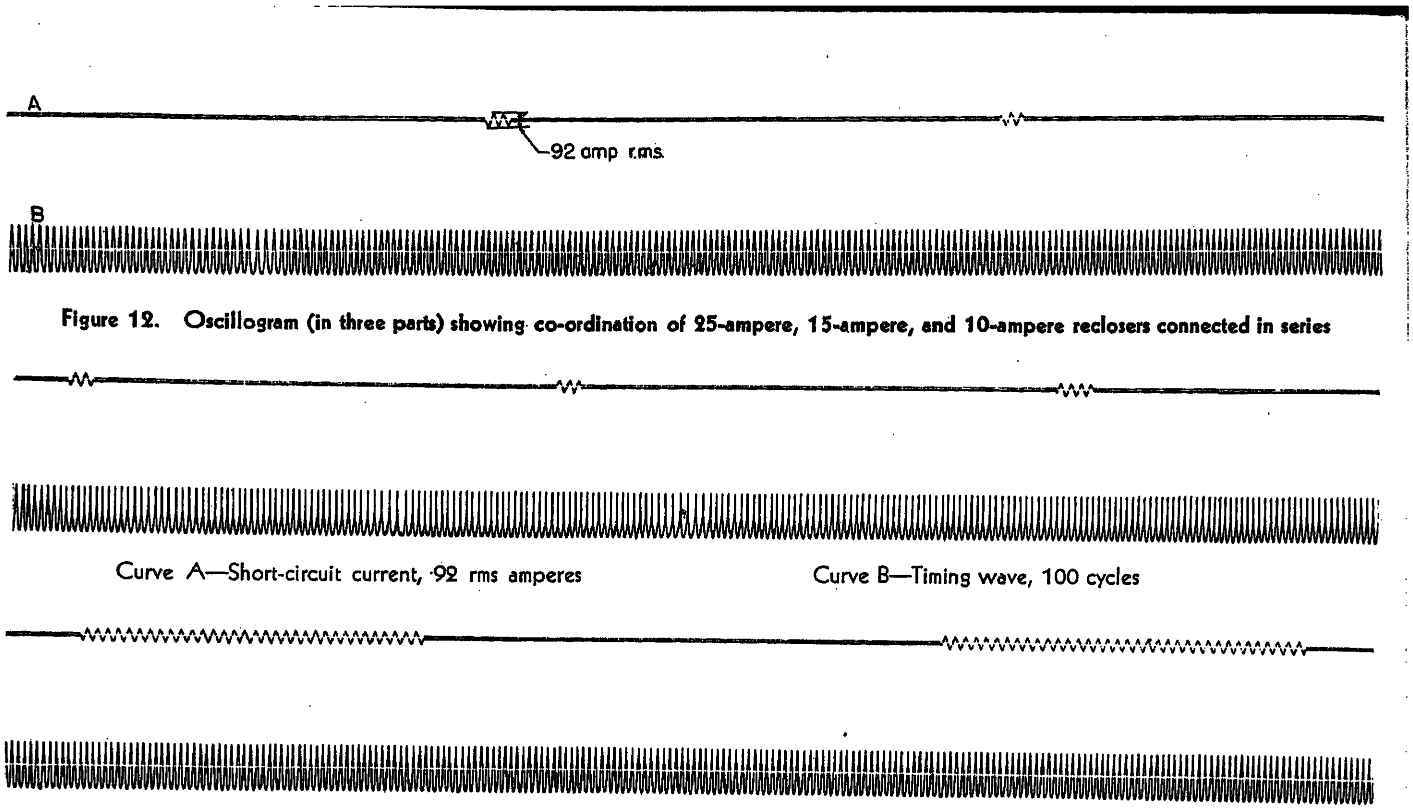


Figure 12. Oscillogram (in three parts) showing co-ordination of 25-ampere, 15-ampere, and 10-ampere reclosers connected in series

Curve A—Short-circuit current, 92 rms amperes

Curve B—Timing wave, 100 cycles

seventh openings are time-delay operations of the 10-ampere recloser which locked open.

This test proved that after each recloser had completed two instantaneous openings, it advanced to time-delay position, but only the smaller rating recloser adjacent to the fault opened on time delay and locked open to sectionalize the fault.

Discussion of Results

The data obtained in the field test show that co-ordination of reclosers with series fuses cannot be determined from direct comparison of the published characteristics of each device. However, a comparison of the test results with those obtained by the analytical co-ordination method set forth in this paper indicates that this method will provide adequate allowances for all manufacturing and operating variables to assure successful co-ordination.

Tests involving co-ordination of two or more reclosers in series show that several reclosers may perform instantaneous operations before the smallest rating of recloser adjacent to the fault locks open to sectionalize the fault. However, if the time-delay characteristics of the recloser are sufficiently accurate, dependable co-ordination of series reclosers can be obtained.

The field tests disclose that motor inrush currents on line sections between reclosers may affect the sequence of instantaneous operations of the reclosers.

Where reclosers and fuses are operated in series, the appreciable difference between published fuse characteristic curves and actual fuse clearing time, which results from intervals of heating and cooling of the fuse link during successive recloser operations, must be evaluated if successful operation is to be obtained. The analytical co-ordination method described affords a reliable means of factoring heating and cooling effect on the fuse link together with all other manufacturing and operating variables which affect co-ordination.

Reference

1. Modern Distribution Fuse Links, G. F. Lincks. *Power Plant Engineering* (Chicago, Ill.), volume 46, number 8, August 1942, pages 55–7.

Discussion

John B. Gmeiner (The Commonwealth and Southern Corporation, Jackson, Mich.): The author's paper presents a compilation of knowledge gained over the past few years in the application of oil circuit reclosers and brings out some facts that have not always been fully recognized.

The operation of reclosing fuses and their co-ordination problems are very similar to those of oil circuit reclosers. Heating and cooling periods of the back-up fuse must be considered in both cases. Proper consideration of the heating and cooling effect on fuses has presented one of the larger intangible problems in distribution co-ordination in recent years. The authors have considered this problem and mention that it will be the subject of a future paper. It is hoped this paper will present a simple method to take the heating and cooling period of fuses into consideration and that the method will be applicable to fuses and oil circuit reclosers of all manufacturers.

It is to be noted from Figure 6 of the paper when consideration is given to all the co-ordination factors, a rather small space is open for applying a line-side fuse. Short-circuit currents often occurring on line-side fuses cover a large portion of the interrupting range of reclosers. From inspection of Figure 6, it may be seen co-ordination would not always be possible. A faster instantaneous operation and a time-delay curve more nearly approaching the slope of a fuse curve would aid the solution of this problem.

Inrush current from mine motors is given as an explanation for the 1-cycle operation of the instantaneous operation of the recloser in the oscillogram shown in Figure 11. It seems a little unreasonable that under proper application where a recloser size is chosen to allow for future load growth, inrush current from motors would cause the fast operation pictured. Many motors have undervoltage controls that remove them from the line on loss of potential. In 40 cycles, the inertia of most motors, large and small, would be of such value that only partial reduction of speed should occur. This would substantially reduce the inrush current that would be present if locked rotor conditions existed. Field checks of several different types of oil circuit reclosers, including the *FR-1*, have revealed the instantaneous shots do not always conform to the published curve and are often faster.

Our field tests would indicate the 1-cycle operation is more likely the result of oil circuit recloser timing inaccuracies rather than motor inrush current. It would have been interesting to have observed one or two more tests performed under the conditions stated for Figure 11. It is agreed that, in any event, proper co-ordination will still be obtained.

The co-ordinated application of reclosers with fuses has been somewhat haphazard in the past. Operation engineers are realizing the need for analytical co-ordination studies before the applications of reclosers are made. For the application engineer this paper should provide a good summary of present-day recloser co-ordination methods.

S. R. Smith, Jr. (General Electric Company, Pittsfield, Mass.): Referring to the section on Cooling of the Fuse Link During Intervals That the Recloser is Open of the author's paper, the additional increment which is added to the fuse curves to factor fuse cooling during the open intervals of the recloser is

$$\frac{I_{300}^2}{i^2}\,T_d$$

where

I_{300} = 300-second current value from the fuse curve being used (or where the curve is asymptotic with the time axis)
T_d = total time the recloser is open for the operating cycle involved
i = circuit current.

To further explain the method of using this cooling correction, consider first the case where it is desired that the recloser go through a given sequence of operations without partial melting of a particular fuse. To ascertain the limit of co-ordination we start with the published fuse melting time curve and add $(I_{300}^2/i^2)\,T_d$ to points along the curve, simultaneously subtracting 25 per cent of the melting time to allow for operating variables (as explained in the paper) so as to arrive at a corrected fuse melting curve. This curve is superimposed on the curve of total opening time of the recloser for the sequence involved (sum of all instantaneous, or instantaneous plus time-delay opening times as determined from the published recloser curves). The point where these curves intersect indicates the current limit to which recloser will function without partially melting the fuse.

Conversely, for the case where the fuse is to clear the circuit before the recloser goes through a given operating sequence, we start with the published fuse total clearing time curve and add $(I_{300}^2/i^2)\,T_d$ to points along the curve so as to arrive at a corrected fuse clearing time curve (conservatism dictates omission of the minus 25 per cent factor for operating variables in this case). This curve is superimposed on the curve of total opening time of the recloser for the sequence involved (sum of all instantaneous, or instantaneous plus time-delay opening times as determined from the published recloser curves). The point where these curves intersect indicates the current limit to which fuse will clear before recloser completes its sequence of operations.

Study of the foregoing methods will show that the same results can be achieved by changing the sign of the corrections and applying them to the recloser curve instead of to the fuse curve.

The cooling correction given before derives from the fuse heat balance equations and we have used it for a number of years in applying reclosing fuses. The point of caution is on very fast fuses, for example where the 0.1-second melting current is only five times the 300-second melting current, in which case the correction may be too large if the individual reclosing intervals are longer than approximately 1.5 seconds.

Chase Hutchinson (Ebasco Services, Incorporated, New York, N. Y.): The benefits of selective operation between circuit breakers or reclosers and fuses has become recognized generally throughout the industry. On overhead distribution lines these benefits are usually manifest by improved service continuity and reduced operating cost. Maximum benefits from a given sequence of operations can be secured by use of accurate application procedures for devices having definite and known operating characteristics. Thus the application procedure described by the authors and checked by field tests should be helpful in obtaining a closer approach to perfect operation of reclosers.

When a fuse is exposed to fault current the heat input is I^2Kt where t is time. For several pulses of fault current of equal magnitude the heat input is

$$I^2K(t_1+t_2+t_3\ldots tn)$$

If no cooling occurs between the several pulses or if the amount of cooling is small the accumulated heat in the fuse may be taken as the heat produced by I amperes flowing for

$$T=(t_1+t_2+t_3\ldots tn)\text{ seconds}$$

The oscillogram of Figure 4 of the paper seems to bear out this reasoning in that (2.5 + 2.5 + 8.5) = 13.5 cycles was required to blow the fuse which apparently checks the 13.2 cycle value given by the authors as published clearing time.

From this reasoning and the test results it appears for practical applications involving a complete cycle of operations occurring in about two seconds, cooling of the fuse link is a negligible factor while heating of the link is substantially the sum of the several pulses of heat. Unfortunately the authors do not expand this point nor do they describe their method of estimating cooling of the link.

The tests on several reclosers in series are interesting and indicate that a degree of co-ordination is obtained. Perfect co-ordination would, for the sequence used, involve three or four recloser operations as contrasted to the eight recloser operations required to isolate the trouble. In the Figure 12 case the line section between the 25- and 15-ampere recloser was interrupted twice and the line section between the 15-ampere recloser and the 10-ampere recloser was interrupted four times. None of these interruptions were essential to the success of the scheme, but were by-products of the inability of the recloser control mechanism to properly determine fault location. This almost complete lack of co-ordination between instantaneous elements of the several reclosers is a characteristic which the writer feels should receive greater attention in future design.

Leonard M. Olmsted (Duquesne Light Company, Pittsburgh, Pa.): The authors have made a commendable effort to evaluate co-ordination between fuses and oil circuit reclosers, giving consideration to the effect of the fuses cooling partially during the open time of the reclosers. It is interesting to see, however, how much error is introduced if this correction is neglected and the time to melt the fuse taken as a summation of the times during each closed period. Taking the first 370-ampere case, the authors report 2.5 cycles during the first opening of the recloser, 2.5 cycles during the second opening of the recloser, and the fuse blowing in 8.5 cycles during the first time delay period of the recloser. If we add these three, we have a total of 13.5 cycles at 370 amperes to blow the 50-ampere fuse, which compares very favorably with its published time of 13.2 cycles. For this case, then, correction of the fuse curve to allow for cooling time does not appear essential. Applying similar reasoning to the case of the 51-ampere fuse which is intended to blow between the first and second time delay operations of the recloser, Figure 6 of the paper indicates that the fuse requires approximately 28 cycles at 370 amperes to clear. If we deduct from this value the two instantaneous openings at 2 cycles each and the first time delay operation in 19.5 cycles, a total of 23.5 cycles, we would expect the fuse to clear during the second time delay operation in approximately 4.5 cycles. The authors' tests show clearing in 17.5 cycles of the second time delay operation, which is longer than the predicted time but still gives the predicted co-ordination. From these two cases, it would seem reasonable to conclude that correction for cooling time in the fuse link may be desirable but that the error introduced by neglecting it should not be of great importance.

In discussing oil circuit reclosers, it should be kept clearly in mind that the requirements are distinctly different from those applying to most other types of switchgear. Whereas station-type switchgear is installed permanently and with protective characteristics more or less matched to the circuit as a whole, it must be anticipated that pole-mounted reclosers will be subject to frequent relocation as the requirements of the distribution circuits change. Co-ordination with only a single fuse size is not enough. They must cover a wide range of sizes determined by the transformer sizes required for service in some instances to only one residential customer, in other locations for schools, stores, shopping districts, and other sizable consumers. For such applications, it is still desirable that the instantaneous tripping of the recloser be fast enough to protect the smallest fuse link, and that the accumulated time before lockout be sufficient to blow the largest fuse link.

Most of the present reclosers have been designed with a view to application on rural circuits at 7,200 volts or more, where transformer fuses are small and short-circuit currents, limited. When they are installed on suburban circuits operating at 2,400 or 4,160 volts, the relationship between the recloser characteristics and the transformer fuse sizes becomes closer. Suburban service requirements are rigid, in many cases indicating the need for automatic sectionalizing equipment, but most of the reclosers now available only partially meet the requirements. The authors appear to have recognized this situation in their proposal that the recloser lock closed on its final operation, thereby throwing the final clearing of a fault on a line fuse which can be of whatever size is required for co-ordination in the circuit.

This feature seems to be distinctly different from previous thought on the matter, but it is my opinion that it is only a compromise until the designers find a way to incorporate sufficient time delay in the recloser mechanism itself. Considerable improvement has been accomplished during the past ten years in this type of equipment, and it is hoped that this improvement will continue until reclosers become available which fully meet all the requirements of suburban distribution.

G. G. Auer, R. A. Branflick, L. J. Woodward, and W. C. McKinley: The discussions of Mr. Olmsted, Mr. Hutchinson, and Mr. Gmeiner confirm the authors' opinion that maximum benefits in co-ordination can be expected when heating and cooling of fuse links is considered during recloser operation. There is general agreement that the heating factor is predominant while the cooling factor is not of too great importance to achieve co-ordination.

Mr. Gmeiner pointed out that by the necessity of adding the required correction factors, limitations were imposed on the application of line-side fuses. This limitation is determined by the characteristics of the overcurrent device on the source side. If this device is an overcurrent relay, the current tap setting based upon short circuit current values rather than load currents will provide room for co-ordination.

It also was pointed out that the instantaneous operations of the reclosers did not conform to the published curves. The published instantaneous curve only indicates the maximum time required for opening of the recloser. A unit might operate in accordance with this curve but, as tests have shown, they usually open faster than indicated. In a co-ordination study or fuse–recloser curve comparison, it is the maximum time in which recloser instantaneous operation can be expected that determines co-ordination possibilities. Faster time of operation is a definite assurance that co-ordination between fuse and recloser will be obtained.

Mr. Chase Hutchinson's thoughts on future design of reclosers which would provide co-ordination during instantaneous operation would be a big improvement from the standpoint of service continuity and elimination of momentary outages on the main line due to branch circuit faults, but to accomplish this, some sacrifice would have to be made in temporary fault protection on the main circuit or else expensive auxiliary equipment would have to be employed.

Mr. S. R. Smith's discussion of the formula to calculate the cooling increment of the fuse link was a great contribution to this paper and did furnish the necessary information to make the analytical procedure complete. Omission of the mathematical procedure and significance of the cooling factor was pointed out by Mr. Gmeiner, Mr. Hutchinson, and Mr. Olmsted.

Wide-Band D-C Amplifier Stabilized for Gain and for Zero

A. J. WILLIAMS, Jr.
MEMBER AIEE

W. G. AMEY
ASSOCIATE AIEE

WILL McADAM
ASSOCIATE AIEE

Synopsis: Previous technical literature has described direct-coupled d-c amplifiers having a wide frequency pass band but with appreciable zero drift,[1,2] and also has described contact-modulated d-c amplifiers having a relatively stable zero but with a narrow pass band.[3] This paper describes an arrangement of a direct-coupled amplifier together with a contact-modulated amplifier. This arrangement retains the wide band of the former and the zero stability of the latter. Block diagrams are used for explanation, and schematic circuits are given for an experimental setup. Records from a fast recorder and from an oscillograph disclose the results achieved, such as rapid response to a step input and greatly reduced, nonpermanent output disturbances following large, unbalanced changes of heater voltage.

ALMOST any electron tube is fundamentally a d-c amplifier having a wide pass band but, in practice, when it is used simply as a "direct-coupled" d-c amplifier, precautions must be taken to retain stability in the face of changes in supply voltage, cathode temperature, cathode emission characteristics, and contact potentials of the tube electrodes, especially of the control grid.[1,2]

A sufficiently stable gain generally can be obtained by the use of negative feedback.[4]

A sufficiently stable zero (zero output for zero input) is more difficult to obtain, some of the precautions being:

1. Stabilized B supply for anodes, screens, and so forth.[5]
2. Balanced, bridge, or push-pull arrangement of tubes.[6,7]
3. Stabilized heater (or filament) supply.[5]
4. Adjustable compensation for changes in supply voltage (or for changes in thermionic emission).[8–18]

Because of these zero-stability difficulties with direct-coupled d-c amplifiers, contact-modulated d-c amplifiers have been found useful, first in systems using a mechanical link in the feed-back loop,[19–22] subsequently without a mechanical link and without feedback,[23,24] and eventually without a mechanical link but with feedback (to stabilize gain).[3] The contact-modulated d-c amplifier is useful because its zero is stable to a fraction of a microvolt.[3] Unfortunately, its frequency pass band is limited by the modulation frequency, for instance, with 60 cycle per second modulation and with gain stabilized to about one part in 1,000, the pass band is only about one cycle per second.

This one cycle per second band width (approximately one second response time) applies equally well to the system with the mechanical link (the self-balancing recorder) and to the system without the mechanical link (the contact-modulate d-c amplifier).

Figure 1 shows that the recorder responds completely in less than one second to a step change in input.[22] With the contact-modulated d-c amplifier (narrow-band)[3] ahead of the recorder, the amplifier response time together with the response time of the recorder results in an over-all response time somewhat greater than a second.

The third curve of Figure 1 was taken with the wide-band amplifier which is the subject of this paper. The response time for the third curve is again less than a second, and is essentially the same as for the recorder alone; hence, the amplifier response time is indicated to be small compared with a second. Figure 2 shows a cathode-ray oscillogram of the response of the wide-band zero-stabilized amplifier alone. Since the time markers at the bottom are spaced by 0.01 second, the reponse time can be conservatively stated as less than 0.005 second. In round numbers, therefore, the response time has been reduced by a factor of 200; hence, the band width has been increased by a factor of 200. At this point it should be stated that the particular amplifier used for these tests is only experimental and is not comparable in gain with the more conventional narrow-band amplifier.[3] The

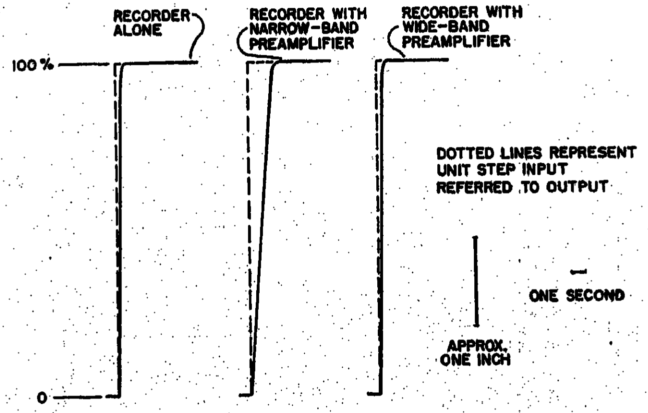


Figure 1. Time response of recorder without and with preamplifiers

Wide-band preamplifier does not contribute appreciable delay

Paper 49-132, recommended by the AIEE Joint Subcommittee on Electronic Instruments and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20–24, 1949. Manuscript submitted March 21, 1949; made available for printing May 4, 1949.

A. J. Williams, Jr., W. G. Amey, and Will McAdam are all in the Research Department, Leeds and Northrup Company, Philadelphia, Pa.

The authors are grateful to I. M. Stein, Director of Research, Leeds and Northrup Company, for the general support of amplifier development, and for the specific encouragement resulting in the presentation of these results.

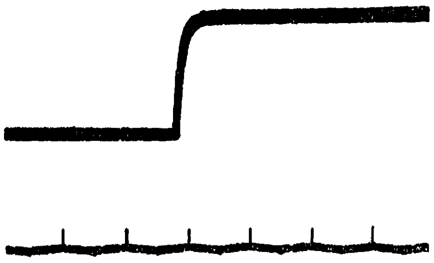


Figure 2. Response of wide-band zero-stabilized amplifier alone

Time markers at bottom are spaced by 0.01 second; hence response is seen to be complete in 0.005 second

principle, however, appears to be applicable to amplifiers of high gain, working down to the natural limits of noise. The discussion which follows is an attempt to explain that principle.

The Problem and the Principle Used for Its Solution

In a simple direct-coupled d-c amplifier, the presence of zero drift cannot be detected (and hence cannot be corrected for) readily without temporarily taking the amplifier out of service, as by shorting the input and observing the output.[25] However, in a direct-coupled amplifier with gain stabilized to a considerable degree (as by feedback), a comparison of input with output divided by gain tells of the presence of zero disturbance at any and all times, with or without signal. The zero stabilization is made on the basis of this comparison.

Figure 3 gives in block form a conventional direct-coupled amplifier with gain stabilized by feedback. The zero drift or disturbance is symbolized by an equivalent voltage e_d entering in an additive way, symbolized by the small block Σ_2. e_x is the input, and e_o is the output. μ is the forward gain, and β is the attenuation in the feed-back circuit. We may write for this circuit

$$\text{Input}=\text{feedback}+\text{error} \tag{1}$$

$$e_x=e_f+e_r \tag{2}$$

$$e_x=e_o\beta+e_r \tag{3}$$

$$e_x=e_o\beta+e_i-ed \tag{4}$$

$$e_x=e_o\beta+e_o/\mu-ed \tag{5}$$

The performance is most apparent when referred to the input, as indicated by equations 1, 2, 3, 4, and 5. The two right-hand terms in equation 5 are the error terms. e_o/μ is the result of limited forward gain and can be reduced below any assigned quantity by increasing the forward gain, μ. The term $-e_d$ represents the troublesome zero disturbance, the effect of which cannot be reduced by increasing the forward gain.

Figure 3 and equation 3 show that the error e_r, which comes from the summing circuit Σ_1, is the result of comparison of input, e_x, with output divided by gain, $e_o\beta$. The error e_r, therefore, should be used to correct for the zero disturbance, as shown in Figure 4.

It is futile to attempt to detect this error with another direct-coupled d-c amplifier (itself subject to zero disturbance), but a contact-modulated d-c amplifier is well suited to this service because of its freedom from zero disturbance. As shown in Figure 4, such an amplifier is employed and is followed by an integrator whose output may be injected at any convenient point, to the right or to the left of the injection of e_d, but, of course, not to the left of the "take off" for the contact-modulated amplifier. Thus, the action will be the same for a zero disturbance occurring at any point between the take off for the contact-moduated amplifier and the output. For the circuit of Figure 4 we may write

$$e_r=e_x-e_f \tag{6}$$

$$=e_x-e_o\beta \tag{7}$$

$$=e_x-e_i\mu\beta=e_x-(e_r+e_d+e_g)\mu\beta= e_x-(e_r+e_d+\alpha\int e_r dt)\mu\beta \tag{8}$$

$$=e_x-e_r\mu\beta-e_d\mu\beta-\mu\beta\alpha\int e_r dt \tag{9}$$

$$e_r+\mu\beta e_r+\mu\beta\alpha\int e_r dt=e_x-\mu\beta e_d \tag{10}$$

For Figure 4 equations 6, 7, 8, 9, and 10 are developed about the error e_r, since it is the error and its reduction with which we are concerned. Equation 10 gives a clue to the performance because of the presence of the integral term, $\mu\beta\alpha\int e_r\,dt$. The nature of this term and the simplicity of the other terms suggest that the error cannot be very large for very long.

Theoretical Performance

Equation 10 provides a basis for computation of the theoretical performance corresponding to prescribed time forms of e_x and e_d.

For convenient manipulation, equation 10 may be rewritten as

$$e_r+K\alpha\int_0^t e_r dt=\frac{K}{\mu\beta}e_x-Ke_d \tag{11}$$

where e_r, e_x, and e_d are functions of time, t, and are assumed to have zero value for all times prior to the reference time $t = 0$; K is equal to $\mu\beta/(1+\mu\beta)$ and μ, β, and α are constants of the system. The Laplace transform of equation 11 then may be written

$$E_r+K\alpha\frac{E_r}{s}=\frac{K}{\mu\beta}E_x-KE_d \tag{12}$$

where E_r, E_x, and E_d are the Laplace transforms of e_r, e_x, and e_d respectively, as functions of the complex variable, s. Solving this equation for the error voltage

$$E_r=\frac{K}{\mu\beta}\left(\frac{s}{s+K\alpha}\right)E_x-K\left(\frac{s}{s+K\alpha}\right)E_d \tag{13}$$

As one of the interesting prescribed time forms, let e_x and e_d be step functions having values of zero, for t less than zero, and constant values $|e_x|$ and $|e_d|$ respectively for t equal to or greater than zero. The Laplace transforms of e_x and e_d are then

$$E_x=\frac{|e_x|}{s}$$

and

$$E_d=\frac{|e_d|}{s} \tag{14}$$

Figure 3. Direct-coupled d-c amplifier stabilized for gain only

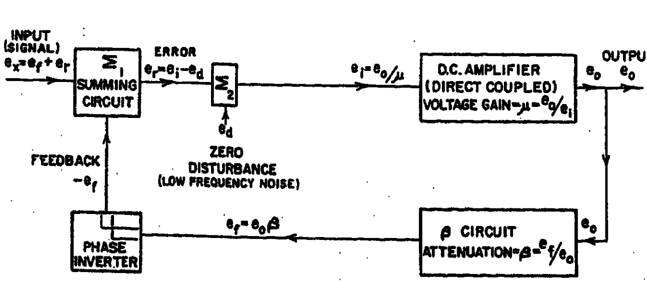
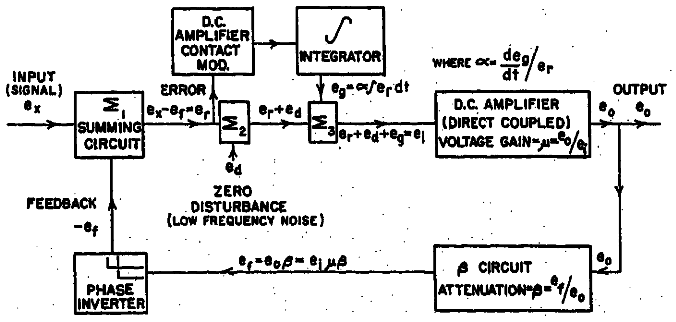

INPUT (SIGNAL)
$e_x=e_f+e_r$
Σ_1 SUMMING CIRCUIT
ERROR
$e_r=e_i-e_d$
Σ_2
e_d
ZERO DISTURBANCE (LOW FREQUENCY NOISE)
$e_i=e_o/\mu$
D.C. AMPLIFIER (DIRECT COUPLED) VOLTAGE GAIN=μ=e_o/e_i
e_o
OUTPUT e_o
FEEDBACK $-e_f$
PHASE INVERTER
$e_f=e_o\beta$
β CIRCUIT ATTENUATION=β=e_f/e_o
e_o

Figure 4. Direct-coupled d-c amplifier stabilized for gain and for zero


D.C. AMPLIFIER CONTACT MOD.
$\int$ INTEGRATOR
WHERE $\alpha=\frac{de_g}{dt}/e_r$
INPUT (SIGNAL)
e_x
Σ_1 SUMMING CIRCUIT
ERROR
$e_x-e_f=e_r$
Σ_2
e_r+e_d
$e_g=\alpha\int e_r dt$
Σ_3
$e_r+e_d+e_g=e_i$
D.C. AMPLIFIER (DIRECT COUPLED) VOLTAGE GAIN=μ=e_o/e_i
e_o
OUTPUT e_o
e_d
ZERO DISTURBANCE (LOW FREQUENCY NOISE)
FEEDBACK $-e_f$
PHASE INVERTER
$e_f=e_o\beta=e_i\mu\beta$
β CIRCUIT ATTENUATION=β=e_f/e_o
e_o

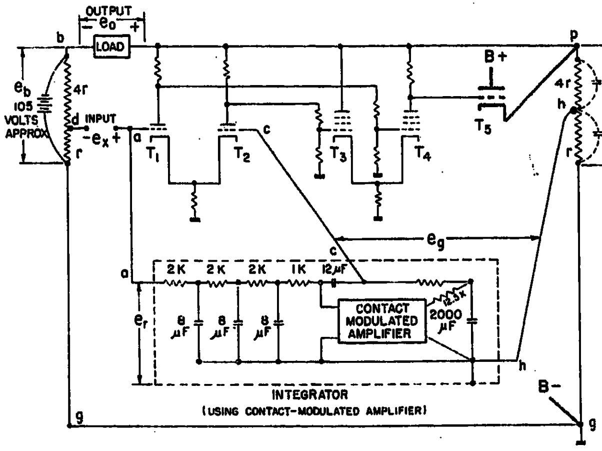


Figure 5. Circuit for experimental setup

respectively, and equation 13 becomes

$$E_r = \frac{K}{\mu\beta}|e_x|\left(\frac{1}{s+K\alpha}\right) - K|e_d|\left(\frac{1}{s+K\alpha}\right) \tag{15}$$

Returning now to the time plane

$$e_r = \frac{K}{\mu\beta}|e_x|\epsilon^{-K\alpha t}K - |e_d|\epsilon^{-K\alpha t} \tag{16}$$

Since the principle of this amplifier is based on the product $\mu\beta$ being large compared with unity, K is nearly equal to unity, and a convenient approximation of equation 16 may be written as

$$e_r = \frac{|e_x|}{\mu\beta}\epsilon^{-\alpha t} - |e_d|\epsilon^{-\alpha t} \tag{17}$$

From equation 16, and more readily from equation 17, it can be seen that the error resulting from an abrupt change of signal is small compared with the signal. Initially it is neither greater nor less than that without the zero stabilizing components; it does, however, vanish with time as a result of the stabilizing components. Again from equation 16, and more readily from equation 17, it can be seen that the error resulting from an abrupt application of a zero disturbance is initially equal to the zero disturbance. If zero disturbances of this abrupt nature were common in practice, the stabilizing components would be of little value, even though the effect of a single such disturbance ultimately would vanish. Fortunately, the more common forms of zero disturbance develop slowly, and the stabilizing components are effective. This can be seen from analysis of the response to uniformly changing or "ramp" functions for e_x and e_d.

In this case, e_x and e_d have values of zero for t less than zero, and values expressed by

$$e_x = a_x t$$

and

$$e_d = a_d t \tag{18}$$

for t equal to, or greater than, zero. The Laplace transforms of e_x and e_d are

$$E_x = \frac{a_x}{s^2}$$

and

$$E_d = \frac{a_d}{s^2} \tag{19}$$

respectively, and equation 13 becomes

$$E_r = \frac{K}{\mu\beta}a_x\frac{1}{s(s+K\alpha)} - Ka_d\frac{1}{s(s+K\alpha)} \tag{20}$$

Returning to the time plane

$$e_r = \frac{K}{\mu\beta}\frac{a_x}{\alpha}\left(1-\epsilon^{-K\alpha t}\right) - K\frac{a_d}{\alpha}\left(1-\epsilon^{-K\alpha t}\right) \tag{21}$$

Since K is nearly equal to unity (as before), a convenient approximation for equation 21 is

$$e_r = \frac{1}{\mu\beta}\frac{a_x}{\alpha}\left(1-\epsilon^{-\alpha t}\right) - \frac{a_d}{\alpha}\left(1-\epsilon^{-\alpha t}\right) \tag{22}$$

From equation 21, and more readily from equation 22, it can be seen that the error resulting from a uniformly changing or ramp-type signal is, at all times, quite small.

Again, from equation 21 and more readily from equation 22, if a ramp-type zero disturbance has a rate of rise a_d which is small compared with the integrator coefficient α, its contribution to the error will, at all times, be small.

Experimental Apparatus

An experimental setup was assembled from two items which were on hand:

1. A contact-modulated d-c amplifier[3]
2. A direct-coupled d-c amplifier

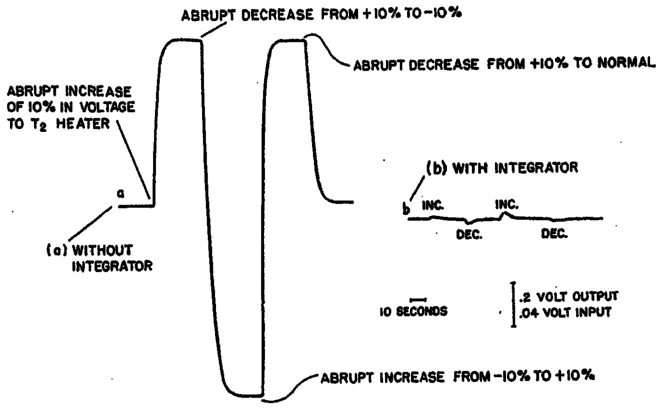


Figure 6. Response to abrupt changes in one heater voltage

The one heater is that of 1/2 of the input stage of the direct-coupled amplifier

Figure 5 shows the direct-coupled voltage amplifier in tube sections T_1, T_2, T_3, T_4, and T_5. The rectifier-filter elements, (which are not shown), supply about 400 direct volts across the B+ and B− terminals.

The principal negative feedback is from the point p through the 5-to-1 voltage divider p-h-g, through the low impedance output of the integrator to the control grid of T_2. More exactly, the feedback is from both terminals of the output load, using both voltage dividers to the two grids of the differential input stage comprising T_1 and T_2. T_1 and T_2 are direct-coupled to T_4 and T_3 respectively to provide effective differential action.

It will be noted that the input signal e_x is connected in series with the grid of T_1 from the divider point d as shown. The integrator output, on the other hand, is connected in series with the grid of T_2 from the other divider point h. Since the voltage dividers p-h-g and b-d-g have exactly the same ratios, the output load voltage bears the proper relation to the input signal (that is, $e_o = e_x/\beta$) only if points a and h are at the same potential. The integrator input terminals are connected to points a and h to measure the error voltage, $e_r = e_x - e_o\beta$, and to effect corrective action by modifying grid potential of T_2 in proper amount, $e_g = \alpha\int e_r dt$, between the integrator output terminals h and c. Since the corrective action is applied to the grid of T_2 rather than the grid of T_1, point c must become negative with respect to point h whenever point a tends to become positive with respect to point h.

The integrating amplifier is the contact-modulated amplifier described in a previous paper,[3] and modified as shown. The principal modification is the insertion of the 12-microfarad capacitor in series in the local feedback circuit, within the integrator. Since there is feedback through this capacitor only when the integrator output is changing, the voltage across the output terminals *c* and *h* changes at a rate proportional to the voltage between the integrator input terminals *a* and *h*. Consequently, at any instant, the voltage e_g is proportional to the time integral of the values of voltage e_r up to and including that instant. The integrator coefficient or proportionality constant, α, is determined by the 7,000 ohms of resistance in the input filter, and the 12 microfarads of capacitance in the feed-back circuit. The determining relation is $\alpha = 1/RC$, so that

$$\frac{1}{\alpha} = RC = 7{,}000 \times 12 \times 10^{-6} = 0.08 \text{ second}$$

for the integrator shown.

Observed Performance

The experimental setup of Figure 5 was used to obtain the third curve of Figure 1 and the oscillogram of Figure 2.

Since changes of cathode temperature are known to produce zero disturbances, the heater voltage of T_2 was varied abruptly ±10 per cent from the rated value, while that of T_1 was held constant at rated value. Figure 6 shows a record of the output voltage. With the contact-modulated amplifier and the integrator inoperative, the output swung more than 1.6 volts. The effect persisted as long as the heater voltage was held off normal. With the contact-modulated amplifier and the integrator operative, the output voltage swing was only 0.03 volt, or a reduction of 50-to-1; moreover, after about 10 seconds, the output disturbance had vanished.

Figure 7 shows the effect of changing the heater voltage of T_2 at a uniform rate of 20 per cent per minute, the changes again being ±10 per cent from the rated value. With the contact-modulated amplifier and the integrator inoperative, the output disturbance was more gradual than before, but of the same ultimate magnitude as for the abrupt change. With the contact-modulated amplifier and the integrator operative, the effect on the output was reduced by a factor of several hundred, since that effect is barely perceptible and certainly is less than 0.005 volt. Referred to the input, this is less than 0.001 volt.

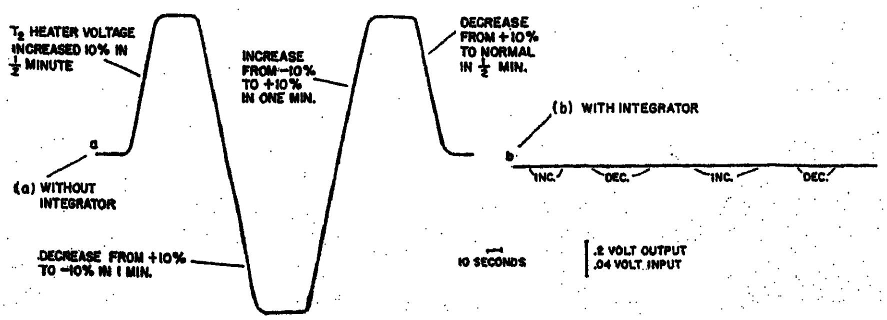


Figure 7. Response to slow changes in one heater voltage

This small order of error is predictable from equation 22. The first curve of Figure 7, with the contact-modulated amplifier and the integrator inoperative, shows the output voltage changing at about 0.04 volt per second, which is 0.008 volt per second when referred to the input. That is, $a_d = 0.008$ volt per second. $1/\alpha$, as determined by the resistance and capacitance constants of the experimental apparatus, was previously calculated as 0.08 second. Extracting from equation 22 the term applicable to the maximum error from a ramp-type zero disturbance

$$e_{r_{max}} = \frac{a_d}{\alpha} = a_d \frac{1}{\alpha}$$

$$= (0.008 \text{ volt per second})$$
$$(0.08 \text{ second})$$
$$\cong 0.0006 \text{ volt}$$

Hence the calculated value of error checks the observed value, in that both are less than 0.001 volt.

The direct-coupled amplifier with which this experimental work was done had definite limitations, such as fluctuations in its *B* supply; hence, no serious attempt was made to operate down to voltage levels where the contact modulator would be the limiting factor.

Conclusion

From the exploratory work herein described, it may be concluded that the zero of a wide-band direct-coupled d-c amplifier can be stabilized by the use of a contact modulator without reduction of the over-all frequency pass band, which is limited by the response characteristics of the direct-coupled amplifier alone, and which is independent of the modulation frequency.

References

1(a). A High-Gain D-C Amplifier for Bio-Electric Recording, Harold Goldberg. *AIEE Transactions*, volume 59, 1940, pages 60–4.

1(b). Direct-Coupled Amplifiers, Harold Goldberg. *Thesis*, University of Wisconsin Library (Madison, Wisc.), 1936.

2. Distributed Amplification, Edward L. Ginzton, William R. Hewlett, John H. Jasburg, Jerre D. Noe. *Proceedings*, Institute of Radio Engineers (New York, N. Y.), volume 36, 1948, pages 956–69.

3. D-C Amplifier Stabilized for Zero and Gain, A. J. Williams, Jr., R. E. Tarpley, W. R. Clark. *AIEE Transactions*, volume 67, 1948, pages 47–57.

4. An Improved Vacuum Tube Microammeter, A. W. Vance. *Review of Scientific Instruments* (New York, N. Y.), volume 7, 1936, pages 489–93.

5. The "Speedomax" Power-Level Recorder, William Russell Clark. *AIEE Transactions*, volume 59, 1940, pages 957–64.

6. The Measurement of Ionization Currents by Three-Electrode Valves, J. Bretano. *Nature* (London, England), volume 108, 1921, page 532.

7. A Valve-Amplifier for Ionization Currents, C. E. Wynn-Williams. *Proceedings*, Cambridge Philosophical Society (London, England), volume 23, 1927, pages 811–28.

8. The Application of a Valve Amplifier to the Measurement of X-Ray and Photo-Electric Effects, C. E. Wynn-Williams. *Philosophical Magazine* (London, England), volume 6, 1928, pages 324–34.

9. A Triode Amplifier for Small Direct Currents, Alexander Marcus. *Physical Review* (New York, N. Y.), volume 31, 1928, pages 302–3.

10. The Application of a Valve Amplifier to the Measurement of X-Ray and Photo-Electric Effects, J. Bretano. *Philosophical Magazine* (London, England), volume 7, 1929, pages 685–91.

11. A Bridge Grid Resistor Amplifier, Joseph Razek, Peter J. Mulder. *Journal*, Optical Society of America (New York, N. Y.), volume 19, 1929, pages 390–403.

12. Measurement of Small D-C Potentials and Currents in High Resistance Circuits by Using Vacuum Tubes, W. B. Nottingham. *Journal*, Franklin Institute (Philadelphia, Pa.), volume 209, 1930, pages 287–348.

13. 1-Tube Balanced Circuit for D-C Vacuum Tube Amplifiers of Very Small Current, Walter Soller. *Review of Scientific Instruments* (New York, N. Y.), volume 3, 1932, pages 416–22.

14. An Improved D-C Amplifying Circuit, Lee A. DuBridge, Hart Brown. *Review of Scientific Instruments* (New York, N. Y.), volume 4, 1933, pages 532–6.

15. Uber ein neuartiges Rohrengalvanometer, G. Barth. *Zeitschrift fur Physik* (Berlin, Germany), volume 87, 1934, pages 399–408.

16. Direct-Current Amplifier Circuits for Use with Electrometer Tube, D. B. Penick. *Review of Scientific Instruments* (New York, N. Y.), volume 6, 1935, pages 115–20.

17. The Measurement of Small Potential Differences by Means of a Double Triode Valve in a Bridge Circuit, J. C. M. Bretano, P. Ingleby. *Journal of Scientific Instruments* (London, England), volume 16, 1939, pages 81–4.

18. Improvements in the Stability of the *FP-54* Electrometer Tube, J. M. Lafferty, K. H. Kingdon. *Journal of Applied Physics* (New York, N. Y.), volume 17, 1946, pages 894–900.

19. New Instruments: "Speedomax," Leeds and Northrup Company. *Instruments* (New York, N. Y.), volume 6, 1933, page 211.

20. Recorder and Control Circuits, A. J. Williams, Jr. United States Patent 2,113,164, April 5, 1938.

21. Combined Thyratron and Tachometer Speed Control of Small Motors, A. J. Williams, Jr. *AIEE Transactions*, volume 57, 1938, pages 565–8.

22. Electronically Balanced Recorder for Flight Testing and Spectroscopy, A. J. Williams, Jr., W. R. Clark, R. E. Tarpley. *AIEE Transactions*, volume 65, 1946, pages 205–8.

23. Some Experiments on the Amplification of Thermocouple Electromotive Forces, Ross Gunn. *Review of Scientific Instruments* (New York, N. Y.), volume 9, 1938, pages 267–9.

24. A Contact Modulated Amplifier to Replace Sensitive Suspension Galvanometers, Max D. Liston, C. E. Quinn, W. E. Sargeant, G. G. Scott. *Review of Scientific Instruments* (New York, N. Y.), volume 17, 1946, pages 194–8.

25. Measuring Systems, A. J. Williams, Jr. United States Patent 2,312,945, March 2, 1943.

Discussion

Edwin A. Goldberg (RCA Laboratories, Princeton, New Jersey): The principle of the wide-band zero-stabilized amplifier disclosed by Mr. Williams has been employed for quite some time in amplifiers developed under a government contract at the RCA Laboratories for the Special Devices Center, Office of Naval Research for computing purposes. This development was declassified on June 15, 1949. It is also used by the Reeves Instrument Company in amplifiers employed in some models of the *REAC* analog computer. Figure 1 of the discussion is a block diagram of the version used. It utilizes current-summation feedback rather than voltage-summation feedback, and provides for both a grounded input and grounded output without utilizing any floating power sources. The d-c amplifier portion is quite conventional. A is the input grid terminal of the first tube, B is the amplifier output terminal, and C is a point within the amplifier where a zero setting voltage normally is applied. Z_f is the feedback impedance, and Z_i the input impedance. A sample of the voltage at A is chopped, amplified by an a-c amplifier, rectified by a synchronous rectifier, filtered, and applied to point C. If the d-c amplifier has the same gain, $G_1(\omega)$, for signals applied at either point A or point C, and the gain of the chopper, amplifier, rectifier, and filter from point A to point C is $G_2(\omega)$, the gain between points A and B is

$$G_{AB}(\omega)=G_1(\omega)G_2(\omega)+G_1(\omega)$$

At d-c ($\omega=0$), $G_1(0)=50{,}000$ and $G_2(0)=3{,}000$ giving a value for the forward gain $G_{AB}(0)$ of 150×10^6 in a typical computing amplifier, and making possible very high feed-back loop gain to stabilize net over-all gain with feedback against variations in gain within the amplifier itself.

As the frequency increases from direct current, $G_2(\omega)$ decreases about six decibels per octave due to the time constant of 10 seconds for the filter. At 50 cycles, the $G_2(\omega)$ is about unity while $G_1(\omega)$ is still about 50,000, resulting in a forward gain of 100,000. Thus, it can be seen that in addition to minimizing zero offset and drift, the stabilizer also increases the net forward gain even for low a-c frequencies. At high frequencies, $G_2(\omega)$ becomes negligible, and the forward gain is the same as the gain of the d-c amplifier without the stabilizer. The tube complement of this amplifier consists of a *5692*, a *6SH7*, and a *6V6* in the d-c amplifier, and two *5693*'s in the a-c amplifier.

The mechanical chopper is an unpolarized vibrating-reed device which is driven from a 193-cycle source and vibrates at 386 cycles. It has two pairs of contacts, one pair being used as a chopper, the other pair as a synchronous rectifier. The second-harmonic mode of operation is used to minimize false d-c offset due to possible stray 193-cycle pickup from the vibrator drive in the a-c amplifier. The synchronous rectifier is insensitive to frequencies other than 386 cycles.

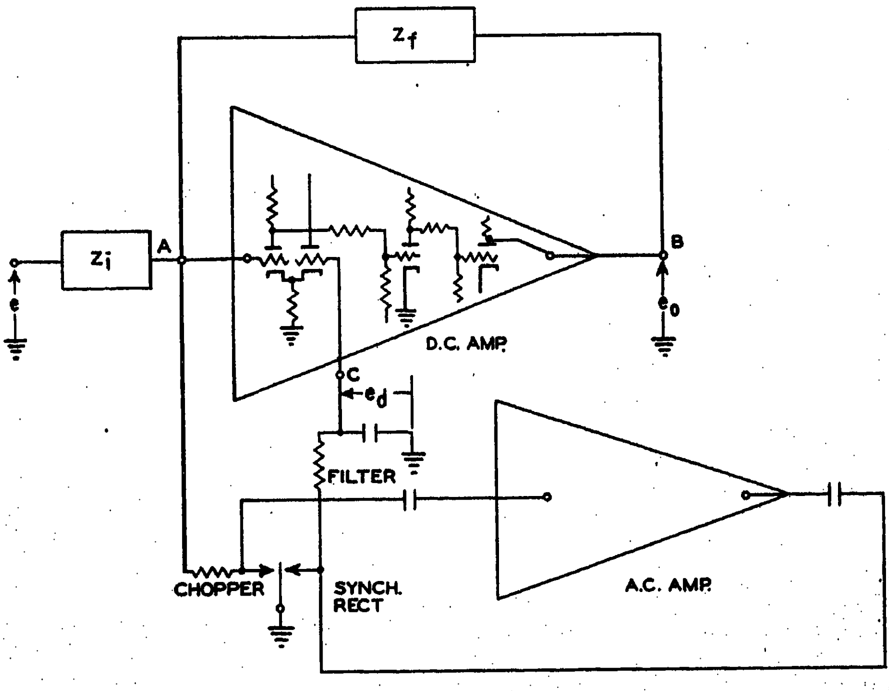


Figure 1. Block diagram of wide-band amplifier

The gain of the amplifier with feedback is the ratio Z_f/Z_i. If Z_f and Z_i are both resistors, the amplifier has a flat frequency response. By supplying several inputs through separate resistors, the amplifier becomes an adding device. If Z_f is made a capacitor and Z_i a resistor, the device becomes an integrator which has an extremely low inherent drift rate. If Z_i is made a capacitor and Z_f a resistor, the device becomes an excellent differentiator with a negligible d-c offset.

The magnitude of d-c offset referred to input grid of the stabilized amplifier depends on three factors:

1. The inherent effective d-c offset of the chopper itself and associated circuits
2. The value of $G_2(0)$
3. The voltage e_d which would normally have to be applied at C to zero the amplifier.

The offset due to (1) is normally negligible, provided the chopper is carefully made and utilized. The offset due to (2) and (3) to first order approximation is $e_d/G_2(0)$. Offsets of about 50 microvolts or less may be obtained fairly easily in computing amplifiers utilizing input and feed-back resistors in the megohm range.

This principle of zero drift elimination may be applied to extremely broad band amplifiers such as video amplifiers with flat response from direct current to extremely high frequencies. The response characteristics of the computing amplifiers used by RCA Laboratories depends on the nature of Z_f and Z_i; however, if both Z_f and Z_i are resistors, the response is extremely flat from direct current to 100 kc.

The accuracy, utility, and ease of operation of d-c-type analog computers have been increased by a large factor through the incorporation of these amplifiers, because the frequent manual adjustments of the amplifier d-c zeros have been eliminated entirely.

A. J. Williams, Jr.: It is gratifying to learn from Mr. Goldberg that he and others have found utility in a wide-band zero-stabilized amplifier similar in principle to the amplifier we have described. Over 20 years of experience with chopper amplifiers at Leeds and Northrup Company have given us considerable confidence in them; hence we are inclined to think up new ways to use them and are pleased that others find new ways to use them too.

A-C Resistance of Segmental Cables in Steel Pipe

L. MEYERHOFF
MEMBER AIEE

G. S. EAGER, JR.
ASSOCIATE AIEE

Synopsis: In recent years, there has been a very great increase in the use of pipe-type systems for long, high-voltage impregnated-paper transmission feeders. Thus, a detailed knowledge of the factors influencing the a-c losses of large size conductors with relatively low skin-proximity effect characteristics is of value to all cable and operating engineers. This paper reports the results of a study of the effective a-c resistance of a compact type of segmental conductor in sizes from 1,000,000 to 2,000,000 circular mils, in air and in steel pipe, with equilateral triangular and cradle configurations. From analysis of the data, an approximate separation is made of losses occurring in the conductor, the outer shield structure and the steel pipe. These component losses are compared with accepted and derived formulas, and application of the data is thus broadened by use of mathematical methods. Based upon the suggested methods of computation, tables are given of losses of cables in steel pipe over a range of conductor sizes and voltage ratings.

SINCE the war there has been a great increase in the use of impregnated paper insulation under high oil or gas-pressure in steel pipe for long high-voltage transmission feeders. Thus, the need for reliable data on carrying capacities of such systems has been greatly enhanced.

The data here presented are not claimed or believed to constitute a complete answer to the problem and are submitted with the hope that they will be found useful to the industry and serve to stimulate other investigators in the accumulation of further data. In presenting this paper, the authors do so with a full appreciation of the benefits accruing to the entire industry from a free discussion of the design factors involved. Sufficient operating and test data have not been and are not available to justify the making of warranties.

While considerable data on the purely thermal elements of the problem have been given in a previous publication[10] only very meager data are available in the technical literature on the a-c loss aspect of cable assemblies of practical interest in pipe-type systems.

Wiseman[1] has made available to the industry data on conventional concentric strand conductors in steel pipe, and also has presented measurements on a single size of segmental conductor. However, in all cases the cable assembly employed was designed primarily to simulate that used in practice on nonleaded low-voltage rubber or varnished cambric cables installed in steel conduit in power stations or industrial plants rather than that used on high-voltage pipe-type systems.

Since the losses in large size conventional concentric strand conductors are relatively high for conductor sizes of 1,000,000 circular mils and larger, all high-voltage pipe-type systems which have been installed in the past few years with conductor sizes of 1,000,000 circular mils or more have employed segmental-type conductors to obtain lower a-c losses.

In the construction of high-voltage pipe-type cables it has been the invariable practice to provide mechanical protection over the individual cables in the form of helically applied, D-shaped copper wires, sometimes termed skid wires. These are usually applied over a surface wrapping consisting of one or more copper tapes intercalated with nonconducting tapes. In some constructions, the nonconducting tape has been a heavy cotton, duck, or paper tape. In others, a polyethylene tape has been used because of its nonhygroscopic nature. In some cable which has been manufactured the surface wrapping has been comprised of two intercalated copper tapes. (None of these cables had a permanent lead sheath.) The protective covering just described, which will be termed the "shield" in what follows, contributes a significant fraction of the total a-c loss of the system, largely because of the circulating currents set up by the induced voltage.

Cables Tested

The cable samples tested and their construction were those shown in Table I. All conductors consisted of four segments, three of which were insulated (insulation consisted of three paper tapes), bound together by a slightly overlapped bronze tape of about 40 per cent conductivity. The individual 90-degree segments were of the Compack type, the essential characteristic of which is that all layers are stranded in one direction, each layer being rolled as applied. In all cases the cable samples were subjected to the normal drying and impregnating process used in cable practice.

The samples in Table I were tested in air and in pipe, with the three conductors arranged both in equilateral triangular and cradle (that is, conductors laid side by side on the bottom of the pipe) configurations. The shields were generally insulated from each other by four layers of heavy paper (total separation approximately 40 mils) and measurements were made both with the shields electrically connected together at the ends, and with the shields not connected together.

For samples *1A*, *1B*, *3A* and *3B*, the pipe used was a length of electrically-welded steel pipe 20 feet 6 inches long having an internal diameter of 6.10 inches and a wall thickness of 0.28 inch and having electrical characteristics as shown in Table II. The cable lengths

---

Paper 49-135, recommended by the AIEE Insulated Conductors Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20–24, 1949. Manuscript submitted November 16, 1948; made available for printing April 26, 1949.

L. MEYERHOFF and G. S. EAGER, JR. are both with the General Cable Corporation, Bayonne, N. J.

used were 25 feet, potential points being placed 20 feet apart.

For samples *2A* and *2B*, the pipe used was a length of lap-welded steel pipe 15 feet 6 inches long having an internal diameter of 8.02 inches and a wall thickness of 0.32 inch and having electrical characteristics as shown in Table II. The cable lengths used were 20 feet, potential points being placed 15 feet apart.

Copper lugs were soldered to each end of each cable length. At each potential point the insulation, the outer shield, the binder, and the segment insulation were removed for a longitudinal distance of one inch. Potential leads were soldered to the conductor, and the segments tied together at the potential points by binding wire.

Measurements—Methods and Accuracy

The apparatus and methods of measurement are described in detail in Appendix I. This Appendix also discusses the accuracy of the resistance measurements and gives results of comparative tests with other laboratories or equipment.

Cable 3A, together with the 6-inch pipe used in the present series of measurements was sent to the Electrical Testing Laboratories (ETL) for comparative measurements. A-c to d-c ratios compared as follows:

	Cable in Air	Cable in Pipe
Authors	1.19	1.63
ETL	1.205	1.65

While making a joint study with the Philadelphia Electric Company, losses were measured both by the method described in Appendix I and by use of a deflecting wattmeter supplied by Philadelphia Electric Company. Comparative values of watts loss were as follows:

	At Room Temperature	At 68.5 Degrees Centigrade
Authors	714	732
Philadelphia Electric Company	713	736

These comparisons are described in greater detail in Appendix I.

Table I. Cables With Segmental Conductors

Designation	Conductor Size, Circular Mils	Conductor Diameter, Inches	Insulation Wall, Inches	Type of Shield	Dimensions of Skid or D-Wires, Inches
1A	1,000,000	1.12	0.500	HR*	0.070×0.200
1B	1,000,000	1.12	0.500	LR†	0.070×0.200
2A	1,500,000	1.36	0.600	HR**	0.100×0.200
2B	1,500,000	1.36	0.600	LR†	0.100×0.200
3A	2,000,000	1.58	0.315	HR**	0.100×0.200
3B	2,000,000	1.58	0.315	LR†	0.100×0.200

HR = high resistance. LR = low resistance.

* Consists of three 3-mil copper tapes, each intercalated with a 7-mil polyethylene tape. Thereover two skid wires, double start, spaced 1 to 1¹/₄ inches.

** Consists of two 3-mil copper tapes, each intercalated with a 7-mil polyethylene tape. Thereover two skid wires, double start, spaced 1 to 1¹/₄ inches.

† Consists of one 3-mil copper tape, intercalated with a 7-mil polyethylene tape and two 3-mil copper tapes intercalated with each other. Thereover two skid wires, double start, spaced 1 to 1¹/₄ inches.

Table II. Electrical Characteristics of Steel Pipes at 30 Degrees Centigrade

Pipe, Inside Diameter, Inches	Wall Thickness, Inches	D-C Resistance, Microhms Per Foot	Resistivity, Microhm Inches	A-C Resistance, Microhms Per Foot at Various Currents (Amperes) 50	100	150	200	250	300
6.10	0.28	12.2	5.62	72	108	149	158	159	157
8.02	0.32	7.65	5.15	90	144	152	149	143	137

Return conductor inside of pipe for a-c measurements.

Table III. Skin Effect (Y_0) Measurements 9-Inch Spacing

Cable Designation	Conductor Size, Circular Mils, (Segmental)	Type of Shield	D-C Resistance at 30 Degrees Centigrade, Microhms Per Foot	Skin Effect (Y_0) Measured	Skin Effect (Y_0) Computed
1A	1,000,000	HR	10.75	0.02	0.02
1B	1,000,000	LR	10.75	0.02	0.02
2A	1,500,000	HR	7.30	0.03	0.04
3A	2,000,000	HR	5.32	0.07	0.07

Table IV. A-C Losses in Air—Triangular Configuration

Designation	Conductor Size, Circular Mils (Segmental)	Type of Shield	S, Inches	Shields Insulated (SI) or Shields Short Circuited (SS)	R_{ac}/R_{dc} Measured	R_{ac}/R_{dc} Computed*
1A	1,000,000	HR	2.38	SI	1.03	1.03
				SS	1.04	1.06
1B	1,000,000	LR	2.38	SI	1.05	1.06
				SS	1.15	1.17
2A	1,500,000	HR	2.83	SI	1.04	1.06
				SS	1.07	1.11
2B	1,500,000	LR	2.80	SI	1.12	1.10
				SS	1.28	1.27
3A	2,000,000	HR	2.42	SI	1.14	1.13
				SS	1.19	1.20
3B	2,000,000	LR	2.46	SI	1.17	1.19
				SS	1.41	1.46

S = center-to-center spacing of adjacent conductor in inches.

* On the basis of mathematical and empirical equations offered here under Development of Method for Computation of Losses. For proximity loss, $K = 3.8$.

Results of Experimental Work

Measurements were made for each size and configuration of cable, at five currents—160, 320, 480, 640 and 800 amperes. Temperatures during the measurements were in the range of 26 to 32 degrees centigrade. The results reported in the following tables, unless otherwise noted, are at ambient temperature somewhere in this range, and at a conductor current of 800 amperes for 1,500,000 and 2,000,000-circular-mil conductor sizes, and 640 amperes for the smaller sizes.

SINGLE-PHASE MEASUREMENTS—SKIN EFFECT

In Table III are listed measured skin effect values of the various cables tested, in comparison with values computed on the basis that the skin effect of a segmental conductor is equal to that of a round conductor having 2.3 times the d-c resistance of the segmental.

THREE-PHASE MEASUREMENTS—IN AIR AND IN PIPE

Measured loss ratios for triangular configuration in air are given in Table IV; for triangular configuration in steel pipe in Table V; and for cradle configuration in steel pipe in Table VI. In each table there are shown for comparison values computed as later explained.

CALORIMETRIC MEASUREMENTS OF PIPE LOSS (Y_t)

Five calorimetric measurements of pipe loss were made by a method paralleling that used by Salter[2] but with certain refinements to give greater assurance of accuracy. The method used is described in detail in Appendix I. The results of these measurements are shown in Table VII.

Table VII. Pipe Loss (Y_t)—Calorimetric Measurements

Designation	Conductor Size, Circular Mils	Configuration	S, Inches	p, Inches	Measured Pipe Loss†	Computed K_1 for Equation 15
2A	1,500,000	Triangular*	2.83	8.02	0.19	0.0178
2A	1,500,000	Cradle*	2.83	8.02	0.28	0.0162
3A	2,000,000	Triangular*	2.42	6.10	0.25	0.0191
Special**	1,500,000	Triangular	2.07	6.10	0.16	0.0208
Special**	1,500,000	Cradle	2.07	6.10	0.26	0.0202

Average K_1 for 8.02-inch pipe—0.017
Average K_1 for 6.10-inch pipe—0.020

* With shields insulated.

** Uninsulated conductors (segmental), assembled with desired spacing and configuration by means of mechanical separators.

† In terms of d-c conductor loss.

Table V. A-C Losses in Pipe—Triangular Configuration

Designation	Conductor Size, Circular Mils (Segmental)	Type of Shield	S, Inches	p, Inches	Shields Insulated (SI) or Shields Short Circuited (SS)	R_{ac}/R_{dc} Measured	R_{ac}/R_{dc} Computed*
1A	1,000,000	HR	2.38	6.10	SI	1.19	1.19
					SS	1.25	1.24
1B	1,000,000	LR	2.38	6.10	SI	1.22	1.26
					SS	1.46	1.46
2A	1,500,000	HR	2.83	8.02	SI	1.26	1.29
					SS	1.33	1.37
2B	1,500,000	LR	2.80	8.02	SI	1.34	1.39
					SS	1.65	1.69
3A	2,000,000	HR	2.42	6.10	SI	1.53	1.51
					SS	1.62	1.63
3B	2,000,000	LR	2.46	6.10	SI	1.68	1.66
					SS	2.20	2.16

p = Inside diameter of pipe in inches.

* On the basis of mathematical and empirical equations here offered under Development of Method for Computation of Losses. For proximity loss, $K=3.8$. For pipe loss, $K_1=0.020$ for 6-inch pipe and $K_1=0.017$ for 8-inch pipe.

Table VI. A-C Losses in Pipe—Cradle Configuration

Designation	Conductor Size, Circular Mils (Segmental)	Type of Shield	S, Inches	p, Inches	Shields Insulated (SI) or Shields Short Circuited (SS)	R_{ac}/R_{dc} Measured	R_{ac}/R_{dc} Computed*
2A	1,500,000	HR	2.83	8.02	SI	1.38	1.40
					SS	1.50	1.53
2B	1,500,000	LR	2.80	8.02	SI	1.45	1.49
					SS	2.01	1.96
3A	2,000,000	HR	2.42	6.10	SI	1.69	1.63
					SS	1.82	1.79
3B	2,000,000	LR	2.46	6.10	SI	1.79	1.78
					SS	2.37	2.42

* On the basis of mathematical and empirical equations here offered under Development of Method for Computation of Losses. For proximity loss, $K=3.8$. For pipe loss, $K_1=0.020$ for 6-inch pipe and $K_1=0.017$ for 8-inch pipe.

INDUCED VOLTAGE IN SHIELD

Induced shield voltage was measured on one type of cable in air and in pipe to establish the ratio between the two. The measured value in air agreed very satisfactorily with the value computed from the equation

$$e=0.0529\ I\ \log_{10}\frac{S}{c} \qquad (1)$$

where

e = induced voltage, volts to neutral, per 1,000 feet
S = center-to-center spacing of conductors, inches
c = mean shield radius, inches
I = current in amperes

The ratio between the induced voltages in pipe and in air was found to be 1.35 and this value has been used in the computations of shield circulating current. It is interesting to note in this connection, though not directly pertinent, that Del Mar[9] reports a ratio of 1.3 between the reactance of cables in pipe and in air. Results are shown in Table VIII.

SHIELD RESISTANCE

Resistance of some of the shields and components was measured and values obtained as in Table IX. These measured values were used as a basis for calculating the shield and component resistances of the various shields.

Determination of Component Losses from the Data

Measurements having been made in air and in pipe, with shields insulated and with shields short circuited, on cables with low-resistance shields and on similar cables with high-resistance shields, it is possible to arrive by differences at reasonable approximations of the component losses in air and in pipe. For the separation of the losses in pipe it was necessary first to calculate the pipe loss itself, which was done by use of semi-empirical equation 15, with $K_1=0.020$ for the 6.10-inch inside diameter pipe and 0.017 for the 8.02-inch inside diameter pipe, determination of which is shown in Table VII.

The results of the separation of losses for the six segmental cables here reported are shown in Table X, together with the method used in arriving at the various values.

Table VIII. Induced Shield 3-Phase 60-Cycle Voltage

Cable Used–number *2B*–1,500,000–circular-mil Segmental, Low-Resistance Shield; Configuraton–triangular; Center-to-Center Spacing–S=2.83 inches; Mean Shield Radius (less skid wires) c=1.31 inches; Conductor Current, I=802 amperes

	Volts Per 1,000 Feet
Measured shield voltage in air (average of three cables)	—14.0
Calculated shield voltage (by Equation (1))	—14.2
Measured shield voltage in 8.02-inch pipe (average of three cables)	—19.0
Ratio of voltage in pipe to voltage in air, 1.35	

Development of Method for Computation of Losses

Based upon accepted equations for computation of the component losses in air, modified formulas have been developed for the computation of corresponding component losses in pipe, the modifications being those indicated by the data. Additionally, a formula for pipe loss in nonmagnetic pipe has been developed and this also has been modified, as indicated by the data, for magnetic pipe.

In a 3-phase system of the type here discussed, with current, I, flowing through the conductors, the a-c loss may be expressed as I^2R_{ac}, where R_{ac} is the effective a-c resistance. R_{ac} may be regarded as consisting of several components and may be expressed for convenience in terms of R_{dc}, the d-c resistance, as

$$\frac{R_{ac}}{R_{dc}}=1+Y_c+Y_s+Y_i \qquad (2)$$

where

Y_c = increment in ratio due to increased losses in the conductor

Y_s = increment in ratio due to losses in the shield

Y_i = increment in ratio due to losses in the pipe

Y_c and Y_s may be further subdivided as follows

$$Y_c=Y_o+Y_{cp} \qquad (3)$$

and

$$Y_s=Y_{sc}+Y_{sp} \qquad (4)$$

where

Y_o = increment in ratio due to skin effect in the conductors

Y_{cp} = increment in ratio due to proximity effect in the conductors

Y_{sc} = increment in ratio due to circulating current in the shield

Y_{sp} = increment in ratio due to proximity effect in the shield

If the system is operated in air instead of in pipe, Y_i vanishes. To avoid confusion, the symbols for component losses in air will be shown, respectively, as Y_c', Y_s', and so forth.

Each of the foregoing components must be evaluated separately, and may be evaluated in the manner given below. It should be noted that absolute accuracy for each component is not claimed, as there is doubtless some influence of each component on each of the others. However, the total loss as calculated fits satisfactorily the total losses determined on the samples actually measured, and there is also fairly satisfactory correlation between calculated component losses and corresponding component losses as measured directly or as derived from measurements. It is believed that the scheme of evaluation here given provides a rational basis for evaluation of losses within the practical range of segmental high-voltage cables in pipe.

Inasmuch as all measurements were made at 26 to 32 degrees centigrade, the values obtained in the below calculations are on the basis of a temperature of 30 degrees centigrade. Later it will be indicated how the results may be translated to other temperatures.

The forms of the equation used in the following are, as far as possible, based on those of equations generally accepted, but the constants are in general obtained empirically on the basis of component losses derived from the data.

CONDUCTOR SKIN EFFECT (Y_o)

Skin effect of segmental conductors may be evaluated reasonably closely by use of the more or less generally accepted rule that it is equal to the skin effect of a round solid conductor having 2.3 times the d-c resistance of the segmental conductor. For convenience, the values for conductors from 1,000,000 to 2,000,000 circular mils are given in Table XI.

CONDUCTOR PROXIMITY EFFECT (Y_{cp} AND Y_{cp}')

The proximity effect, resulting from superposition of the magnetic fields due to the individual currents in the three conductors, has been worked out for round solid conductors by various in-

Table IX. Resistance of Shield and Components

Cable Designation	Conductor Size, Circular Mils	Type of Shield	Approximate Over-all Diameter, Inches	Resistance, Microhms Per Foot of Cable: Entire Shield R_s	Skid Wires	Calculated* for Shield Less Skid Wires R_s'
1A	1,000,000	HR	2.30	955		
1B	1,000,000	LR	2.36	240	1,310	295
2A	1,500,000	HR	2.78	845		
2B	1,500,000	LR	2.78	242	1,010	318
3A	2,000,000	HR	2.40	890		
3B	2,000,000	LR	2.40	217	880	289

* Calculated on basis of parallel resistances.

Table X. Separation of Losses

	Cable Designations: *1A*	*1B*	*2A*	*2B*	*3A*	*3B*
Triangular Configuration in Air (Table IV)						
Measured R_{ac}/R_{dc}—shields insulated (*RSI*)	*1.03*	*1.06*	*1.04*	*1.12*	*1.14*	*1.17*
Measured R_{ac}/R_{dc}—shields short circuited (*RSS*)	*1.04*	*1.15*	*1.07*	*1.28*	*1.19*	*1.41*
Skin plus proximity effect in conductor (Y_c')	0.03	0.03	0.04	0.04	0.14	0.14
Shield circulating current loss (Y_{sc}')	0.01	0.10	0.03	0.16	0.05	0.24
Shield proximity loss (Y_{sp}')	0.	0.02	0.	0.08	0.	0.03
Triangular Configuration in Pipe (Table V)						
Measured R_{ac}/R_{dc}—shields insulated (*RSI*)	*1.19*	*1.22*	*1.26*	*1.34*	*1.53*	*1.68*
Measured R_{ac}/R_{dc}—shields short circuited (*RSS*)	*1.25*	*1.46*	*1.33*	*1.65*	*1.62*	*2.20*
Pipe loss (Y_i) (by equation 15)	0.13	0.13	0.18	0.18	0.26	0.26
Skin plus proximity effect in conductor (Y_c)	0.06	0.06	0.08	0.08	0.27	0.27
Shield circulating current loss (Y_{sc})	0.06	0.24	0.07	0.31	0.09	0.52
Shield proximity loss (Y_{sp})	0.	0.03	0.	0.08	0.	0.15
Cradle Configuration in Pipe (Table VI)						
Measured R_{ac}/R_{dc}—shields insulated (*RSI*)			*1.38*	*1.45*	*1.69*	*1.79*
Measured R_{ac}/R_{dc}—shields short circuited (*RSS*)			*1.50*	*2.01*	*1.82*	*2.37*
Pipe loss (Y_i) (by equation 15)			0.29	0.29	0.87	0.37
Skin plus proximity effect in conductor (Y_c)			0.09	0.09	0.32	0.32
Shield circulating current loss (Y_{sc})			0.12	0.56	0.13	0.58
Shield proximity loss (Y_{sp})			0.	0.07	0.	0.10

Y_c' = *RSI* (*A* sample) − 1. Assumed the same for *B* sample.
Y_c = *RSI* (*A* sample)—Y_i—1. Assumed the same for *B* sample.
Y_{sc}' and Y_{sc} = *RSS*—*RSI*.
Y_{sp}' and Y_{sp} = *RSI* (*B* sample)—*RSI* (*A* sample). Assumed 0 for *A* sample.

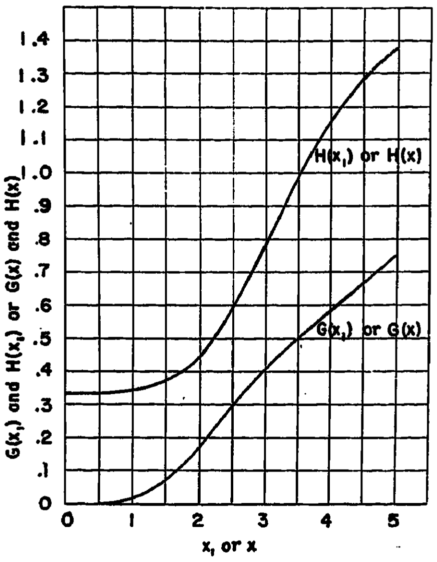


Figure 1. *G* and *H* functions to be used with equation 6 for determination of conductor proximity effect (Y_{cp}')

vestigators and is expressed by the following equation, as given by Arnold[4] for the 3-phase case in air for balanced 3-phase currents, the conductors being in equilateral triangular configuration.

$$Y_{cp}'=\frac{1.5\alpha^2G(x)}{1-0.208\alpha^2H(x)} \tag{5}$$

where

$\alpha=d/S$
d = diameter of the conductor in inches
S = center-to-center spacing of the conductors in inches
$G(x)$ and $H(x)$ are Bessel functions, which are given in the curves of Figure 1
$x=6.80/\sqrt{R_{dc}}$ at 60 cycles
R_{dc} is the d-c resistance of each conductor in microhms per foot

For segmental conductors Arnold's equation has been modified as follows

$$Y_{cp}'=\frac{1.5\alpha^2G(x_1)}{1-0.208\alpha^2H(x_1)} \tag{6}$$

where all symbols are as in equation 5 and

$x_1=K/\sqrt{R_{dc}}$

where K is a factor having a value of 3.8 for segmental conductors with three insulated segments, such as used in the cables here reported.

Limited data on other segmental cables, not here reported, indicate that K is influenced considerably by whether or not the conductors were "treated" (that is, had received the drying and saturating treatment usual for impregnated paper cables), by whether two or three of the segments were insulated and by the type of binder holding the segments together.

Table XI. Skin Effect of Segmental Conductors at 30 Degrees Centigrade

Circular Mils	Y_o
1,000,000	0.017
1,250,000	0.026
1,500,000	0.038
1,750,000	0.050
2,000,000	0.065

Based on d-c resistance values as given in Table XVI-A of Reference 3,* adjusted to 30 degrees centigrade. Skin effect = that of round solid conductor having 2.3 times the d-c resistance of the segmental conductor.
* See References at end of paper.

With three segments insulated these variables have little effect. With two segments insulated, however, a K value as high as 6.0 was obtained for an untreated conductor having a bronze binder applied with a slight lap. It may be of interest to note that on the 2,500,000-circular-mil segmental cable, measurements on which were reported by Wiseman,[1] the reported proximity effect in air corresponds to a K value of 5.4. These values compare with a K value of 6.80 for solid conductors.

For $K=3.8$ proximity effect in air may be obtained from Table XII. For other values of K, equation 6 and Figure 1 may be used.

TOTAL CONDUCTOR LOSS INCREMENT (Y_c AND Y_c')

The total conductor loss increment in air (Y_c') is the sum of Y_o and Y_{cp}'. From all the data on segmental conductors which were available to the authors it appears that the conductor loss increment in pipe (Y_c) bears the following empirical relation to Y'_c.

$$\frac{Y_c}{Y_c'}=1+\frac{28d(S-d)}{p^2} \tag{7}$$

The data given by Wiseman[1] for the 2,500,000-circular-mil segmental cable also fit equation 7. This equation also applies with fair accuracy to the data given by Wiseman for concentric conductors in triangular configuration in conduit, but not in steel pipe.

In practical cables Y_c/Y_c' generally will be found to have a value of about two.

With cradle configuration in pipe it is conservative to use the same Y_c value as for triangular configuration.

SHIELD CIRCULATING CURRENT LOSS (Y_{sc} AND Y_{sc}')

A well accepted equation,[7] for the losses in the shield, in air, due to circulating current is

$$Y_{sc}'=\frac{R_s}{R_c}\,\frac{X_s^2}{R_s^2+X_s^2} \tag{8}$$

where

R_s = longitudinal resistance of the shield including skid wires in microhms per foot
R_c = conductor resistance in microhms per foot
X_s = reactance of the shield in microhms per foot
= $52.9\log_{10}(S/c)$ at 60 cycles
c = mean shield radius in inches

For the usual case of cables bunched together and equipped with skid wires, S/c is about 2.20, whose logarithm to base 10 is 0.343, and $X_s=18.2$.

It may also be noted that X_s for close spacing is small compared to R_s and may be neglected in the denominator. Thus, for close equilateral spacing, equation 8 may be rewritten as

$$Y_{sc}'=\frac{X_s^2}{R_sR_c}=\frac{330}{R_sR_c} \tag{9}$$

In practical cases the value of R_s is approximately 300 microhms per inch of diameter over the skid wires, per foot of cable, where the shield is of the construction which we have designated as "high resistance" with two 100 by 200 mil copper D-wires applied double start with about 1-inch longitudinal spacing. The value of R_s is approximately one-quarter of the above value where the shield consists of two intercalated 3-mil copper tapes, with skid wires as above.

Table XII. Proximity Effect of Segmental Conductors in Air, Triangular Configuration, Three Segments Insulated, Lapped Bronze Binder

Calculated by Equation 6 with K = 3.8

	Proximity Effect (Y_{cp}') Circular Mils				
S, Inches	1,000,000	1,250,000	1,500,000	1,750,000	2,000,000
1.8	0.016				
2.0	0.013	0.025			
2.2	0.011	0.021	0.034	0.053	
2.4	0.009	0.017	0.029	0.045	0.064
2.6	0.008	0.015	0.024	0.038	0.054
2.8	0.007	0.013	0.021	0.033	0.047
3.0			0.018	0.028	0.040
3.2				0.025	0.035

For equilateral triangular spacing in pipe our data show that X_s is 1.35 times its value in air, see Table VIII. Thus, for equilateral triangular spacing in pipe, equation 9 becomes

$$Y_{sc}=\frac{600}{R_sR_c}\text{ (triangular)} \tag{10}$$

For cradle configuration, the spacing may be taken as

$$S'=\sqrt[3]{S_1S_2S_3}$$

where S_1, S_2 and S_3 are the respective separations between each pair of conductors.

The ratio of S' to S is in general equal to about 1.16.

Thus

$$Y_{sc}=\frac{850}{R_sR_c}\text{ (cradle)} \tag{10A}$$

However, for low resistance shield it is important to obtain the actual value of S' and evaluate the constant in equation 10(A) directly.

SHIELD PROXIMITY LOSS (Y_{sp} AND Y_{sp}')

The shield proximity loss is due to eddy currents induced in the shield by the main currents. The equation for this loss, based on an equation given by Wedmore,[6] is

$$Y_{sp}'=\frac{398}{R_s'R_c}\left(\frac{c}{S}\right)^2 \tag{11}$$

in which c, S and R_c have the same meaning as in equation 8, and R_s' is the resistance of the shield minus skid wires, in microhms per foot.

For shields in which the copper tapes are intercalated with nonmetallic tapes R_s' is so high (order of 5,000) that Y_{sp} and Y_{sp}' are negligible. For shields in which two copper tapes are intercalated with each other, however, R_s' is only of the order of 300 microhms per foot and Y_{sp}' may be quite large.

Equation 11 may be simplified by substituting in place of c/S its usual value of about 0.46. With this substitution, equation 11 becomes

$$Y_{sp}'=\frac{84}{R_s'R_c}\text{ (triangular)} \tag{12}$$

For triangular configuration in pipe the shield proximity loss Y_{sp} is about 2.5 times Y_{sp}'.

For cradle configuration it may be taken as the same as for triangular configuration.

Thus

$$Y_{sp}=\frac{210}{R_s'R_c}\text{ (triangular or cradle)} \tag{13}$$

PIPE LOSS (Y_t)

An equation for the loss in nonmagnetic pipe, under 3-phase operation, has been derived by one of the authors upon the assumption that the conductor currents may be considered as concentrated at the centers of the conductors and that the various circulating and eddy currents existing in the system do not affect the pipe loss. The derivation of this equation is indicated in Appendix II.

The equation for loss in nonmagnetic pipe, as derived, is

$$Y_t\left(\begin{matrix}\text{nonmagnetic}\\ \text{at 60 cycles}\end{matrix}\right)=\frac{1.51p't\ (CI)\ Q}{\rho\rho_c} \tag{14}$$

where

p' = mean pipe diameter in inches
t = pipe thickness in inches
(CI) = area of one conductor in circular inches
ρ = resistivity of pipe in microhm-inches
ρ_c = resistivity of conductor in microhm-inches

$$Q=\sum_{n=1}^{\infty}\frac{a^{2n}+b^{2n}+c^{2n}-a^nb^n\cos n\underline{|AB}-b^nc^n\cos n\underline{|BC}-c^na^n\cos n\underline{|CA}}{n^2}$$

$a=2A/p'$; $b=2B/p'$; $c=2C/p'$
A, B, C = distances in inches between the center of the pipe and the respective centers of the three conductors
$\underline{AB}$, $\underline{|BC}$, $\underline{|CA}$ = the angles formed between the respective lines joining the center of the pipe and the centers of the three conductors

For magnetic pipe it is assumed that the equation can be used with an empirical modification of the term in front of the summation. It has been determined that the following applies for the several cases in which pipe loss was measured with the cables in triangular and cradled configurations.

$$Y_t\left(\begin{matrix}\text{magnetic}\\ \text{at 60 cycles}\end{matrix}\right)=K_1p(CI)Q \tag{15}$$

In equation 15 all notations are as in equation 14 except that p' is replaced by p, the inside diameter of the magnetic pipe, and K_1 is a constant depending on the material of the pipe.

K_1 has been determined empirically as 0.020 for the 6-inch pipe used in the measurements, and as 0.017 for the 8-inch pipe, see Table VII. It is recommended in the interest of conservatism that for estimating purposes a value of 0.020 for K_1 be used.

Thus, for estimating purposes equation 15 becomes simply

$$Y_t\left(\begin{matrix}\text{magnetic}\\ \text{at 60 cycles}\end{matrix}\right)=0.020\ p(CI)Q \tag{16}$$

where

Q = a series having values shown in Table XIII for most practical cases and in Figure 2 for more general application.

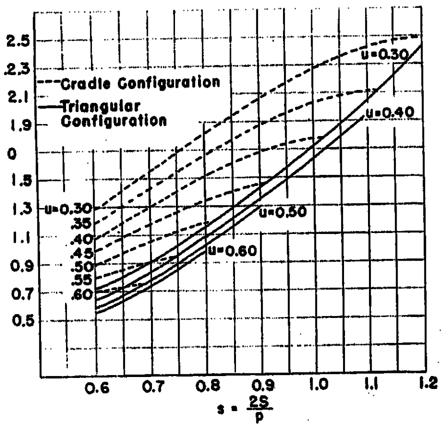


Figure 2. Factor Q to be used with pipe loss equation 16 for triangular and cradle configurations

S = center-to-center conductor spacing in inches
p = inside diameter of pipe in inches
$u=2U/p$ where U = distance between inner pipe surface and center of contiguous conductor in inches

Table XIII. Values of Q for Equation 16

$s=\frac{2S}{p}$	Triangular Configuration	Cradle Configuration
0.65	0.78	1.36
0.70	0.87	1.43
0.75	0.97	1.47
0.80	1.08	1.51
0.85	1.20	1.50
0.90	1.32	1.46

EVALUATION FOR TEMPERATURES OTHER THAN 30 DEGREES CENTIGRADE

As stated previously, all the data in the present series were obtained at temperatures in the neighborhood of 30 degrees centigrade. Thus, the ratio of a-c to d-c resistance as evaluated above is at 30 degrees centigrade.

Table XIV gives data for calculation of skin effect of round conductors at 60, 65, 70, and 75 degrees centigrade if the skin effect at 30 degrees centigrade is known. It has been found convenient to use these data for estimation of all a-c losses. Wiseman[1] has found that this method may be used with good accuracy in connection with the data reported by him. One isolated check which we have made indicates that using the table to evaluate the ratio at a higher temperature from the ratio at a lower temperature is conservative. The evaluation may be carried out for the entire loss or for the loss up to any

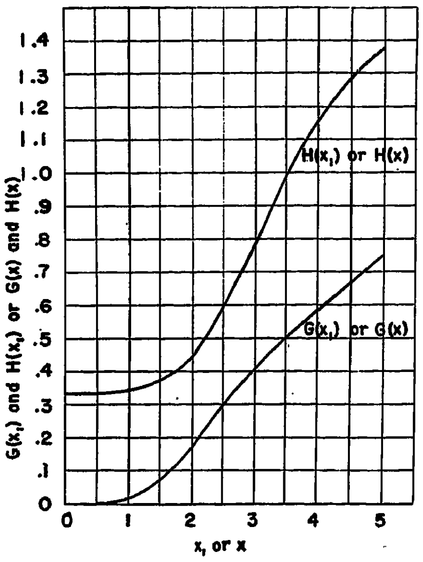


Figure 1. G and *H* functions to be used with equation 6 for determination of conductor proximity effect (Y_{cp}')

vestigators and is expressed by the following equation, as given by Arnold[4] for the 3-phase case in air for balanced 3-phase currents, the conductors being in equilateral triangular configuration.

$$Y_{cp}'=\frac{1.5\alpha^2G(x)}{1-0.208\alpha^2H(x)} \tag{5}$$

where

$\alpha=d/S$
d = diameter of the conductor in inches
S = center-to-center spacing of the conductors in inches
$G(x)$ and $H(x)$ are Bessel functions, which are given in the curves of Figure 1
$x=6.80/\sqrt{R_{dc}}$ at 60 cycles
R_{dc} is the d-c resistance of each conductor in microhms per foot

For segmental conductors Arnold's equation has been modified as follows

$$Y_{cp}'=\frac{1.5\alpha^2G(x_1)}{1-0.208\alpha^2H(x_1)} \tag{6}$$

where all symbols are as in equation 5 and

$$x_1=K/\sqrt{R_{dc}}$$

where *K* is a factor having a value of 3.8 for segmental conductors with three insulated segments, such as used in the cables here reported.

Limited data on other segmental cables, not here reported, indicate that *K* is influenced considerably by whether or not the conductors were "treated" (that is, had received the drying and saturating treatment usual for impregnated paper cables), by whether two or three of the segments were insulated and by the type of binder holding the segments together.

Table XI. Skin Effect of Segmental Conductors at 30 Degrees Centigrade

Circular Mils	Y_o
1,000,000	0.017
1,250,000	0.026
1,500,000	0.038
1,750,000	0.050
2,000,000	0.065

Based on d-c resistance values as given in Table XVI-A of Reference 3,* adjusted to 30 degrees centigrade. Skin effect = that of round solid conductor having 2.3 times the d-c resistance of the segmental conductor.
* See References at end of paper.

With three segments insulated these variables have little effect. With two segments insulated, however, a *K* value as high as 6.0 was obtained for an untreated conductor having a bronze binder applied with a slight lap. It may be of interest to note that on the 2,500,000-circular-mil segmental cable, measurements on which were reported by Wiseman,[1] the reported proximity effect in air corresponds to a *K* value of 5.4. These values compare with a *K* value of 6.80 for solid conductors.

For $K=3.8$ proximity effect in air may be obtained from Table XII. For other values of *K*, equation 6 and Figure 1 may be used.

TOTAL CONDUCTOR LOSS INCREMENT (Y_c AND Y_c')

The total conductor loss increment in air (Y_c') is the sum of Y_o and Y_{cp}'. From all the data on segmental conductors which were available to the authors it appears that the conductor loss increment in pipe (Y_c) bears the following empirical relation to Y'_c.

$$\frac{Y_c}{Y_c'}=1+\frac{28d(S-d)}{p^2} \tag{7}$$

The data given by Wiseman[1] for the 2,500,000-circular-mil segmental cable also fit equation 7. This equation also applies with fair accuracy to the data given by Wiseman for concentric conductors in triangular configuration in conduit, but not in steel pipe.

In practical cables Y_c/Y_c' generally will be found to have a value of about two.

With cradle configuration in pipe it is conservative to use the same Y_c value as for triangular configuration.

SHIELD CIRCULATING CURRENT LOSS (Y_{sc} AND Y_{sc}')

A well accepted equation,[7] for the losses in the shield, in air, due to circulating current is

$$Y_{sc}'=\frac{R_s}{R_c}\ \frac{X_s^2}{R^{22}+X_s^2} \tag{8}$$

where

R_s = longitudinal resistance of the shield including skid wires in microhms per foot
R_c = conductor resistance in microhms per foot
X_s = reactance of the shield in microhms per foot
= 52.9 $\log_{10}(S/c)$ at 60 cycles
c = mean shield radius in inches

For the usual case of cables bunched together and equipped with skid wires, S/c is about 2.20, whose logarithm to base 10 is 0.343, and $X_s=18.2$.

It may also be noted that X_s for close spacing is small compared to R_s and may be neglected in the denominator. Thus, for close equilateral spacing, equation 8 may be rewritten as

$$Y_{sc}'=\frac{X_s^2}{R_sR_c}=\frac{330}{R_sR_c} \tag{9}$$

In practical cases the value of R_s is approximately 300 microhms per inch of diameter over the skid wires, per foot of cable, where the shield is of the construction which we have designated as "high resistance" with two 100 by 200 mil copper D-wires applied double start with about 1-inch longitudinal spacing. The value of R_s is approximately one-quarter of the above value where the shield consists of two intercalated 3-mil copper tapes, with skid wires as above.

Table XII. Proximity Effect of Segmental Conductors in Air, Triangular Configuration, Three Segments Insulated, Lapped Bronze Binder

Calculated by Equation 6 with K = 3.8

	Proximity Effect (Y_{cp}') Circular Mils				
S, Inches	1,000,000	1,250,000	1,500,000	1,750,000	2,000,000
1.8	0.016				
2.0	0.013	0.025			
2.2	0.011	0.021	0.034	0.053	
2.4	0.009	0.017	0.029	0.045	0.064
2.6	0.008	0.015	0.024	0.038	0.054
2.8	0.007	0.013	0.021	0.033	0.047
3.0			0.018	0.028	0.040
3.2				0.025	0.035

For equilateral triangular spacing in pipe our data show that X_s is 1.35 times its value in air, see Table VIII. Thus, for equilateral triangular spacing in pipe, equation 9 becomes

$$Y_{sc}=\frac{600}{R_sR_c}\text{ (triangular)} \tag{10}$$

For cradle configuration, the spacing may be taken as

$$S'=\sqrt[3]{S_1S_2S_3}$$

where S_1, S_2 and S_3 are the respective separations between each pair of conductors.

The ratio of S' to S is in general equal to about 1.16.

Thus

$$Y_{sc}=\frac{850}{R_sR_c}\text{ (cradle)} \tag{10A}$$

However, for low resistance shield it is important to obtain the actual value of S' and evaluate the constant in equation 10(A) directly.

Shield Proximity Loss (Y_{sp} and Y_{sp}')

The shield proximity loss is due to eddy currents induced in the shield by the main currents. The equation for this loss, based on an equation given by Wedmore,[6] is

$$Y_{sp}'=\frac{398}{R_s'R_c}\left(\frac{c}{S}\right)^2 \tag{11}$$

in which c, S and R_c have the same meaning as in equation 8, and R_s' is the resistance of the shield minus skid wires, in microhms per foot.

For shields in which the copper tapes are intercalated with nonmetallic tapes R_s' is so high (order of 5,000) that Y_{sp} and Y_{sp}' are negligible. For shields in which two copper tapes are intercalated with each other, however, R_s' is only of the order of 300 microhms per foot and Y_{sp}' may be quite large.

Equation 11 may be simplified by substituting in place of c/S its usual value of about 0.46. With this substitution, equation 11 becomes

$$Y_{sp}'=\frac{84}{R_s'R_c}\text{ (triangular)} \tag{12}$$

For triangular configuration in pipe the shield proximity loss Y_{sp} is about 2.5 times Y_{sp}'.

For cradle configuration it may be taken as the same as for triangular configuration.

Thus

$$Y_{sp}=\frac{210}{R_s'R_c}\text{ (triangular or cradle)} \tag{13}$$

Pipe Loss (Y_t)

An equation for the loss in nonmagnetic pipe, under 3-phase operation, has been derived by one of the authors upon the assumption that the conductor currents may be considered as concentrated at the centers of the conductors and that the various circulating and eddy currents existing in the system do not affect the pipe loss. The derivation of this equation is indicated in Appendix II.

The equation for loss in nonmagnetic pipe, as derived, is

$$Y_t\begin{pmatrix}\text{nonmagnetic}\\ \text{at 60 cycles}\end{pmatrix}=\frac{1.51p't\,(CI)\,Q}{\rho\rho_c} \tag{14}$$

where

p' = mean pipe diameter in inches
t = pipe thickness in inches
(CI) = area of one conductor in circular inches
ρ = resistivity of pipe in microhm-inches
ρ_c = resistivity of conductor in microhm-inches

$$Q=\sum_{n=1}^{\infty}\frac{a^{2n}+b^{2n}+c^{2n}-a^nb^n\cos n\underline{|AB}-b^nc^n\cos n\underline{|BC}-c^na^n\cos n\underline{|CA}}{n^2}$$

$a=2A/p'$; $b=2B/p'$; $c=2C/p'$
A, B, C = distances in inches between the center of the pipe and the respective centers of the three conductors
$\underline{AB}$, $\underline{|BC}$, $\underline{|CA}$ = the angles formed between the respective lines joining the center of the pipe and the centers of the three conductors

For magnetic pipe it is assumed that the equation can be used with an empirical modification of the term in front of the summation. It has been determined that the following applies for the several cases in which pipe loss was measured with the cables in triangular and cradled configurations.

$$Y_t\begin{pmatrix}\text{magnetic}\\ \text{at 60 cycles}\end{pmatrix}=K_1p(CI)Q \tag{15}$$

In equation 15 all notations are as in equation 14 except that p' is replaced by p, the inside diameter of the magnetic pipe, and K_1 is a constant depending on the material of the pipe.

K_1 has been determined empirically as 0.020 for the 6-inch pipe used in the measurements, and as 0.017 for the 8-inch pipe, see Table VII. It is recommended in the interest of conservatism that for estimating purposes a value of 0.020 for K_1 be used.

Thus, for estimating purposes equation 15 becomes simply

$$Y_t\begin{pmatrix}\text{magnetic}\\ \text{at 60 cycles}\end{pmatrix}=0.020\,p(CI)Q \tag{16}$$

where

Q = a series having values shown in Table XIII for most practical cases and in Figure 2 for more general application.

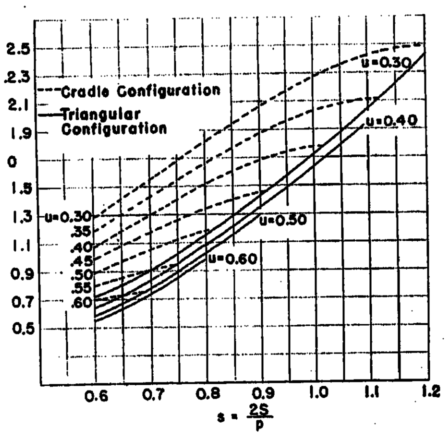


Figure 2. Factor Q to be used with pipe loss equation 16 for triangular and cradle configurations

S = center-to-center conductor spacing in inches
p = inside diameter of pipe in inches
$u=2U/p$ where U = distance between inner pipe surface and center of contiguous conductor in inches

Table XIII. Values of Q for Equation 16

$s=\frac{2S}{p}$	Triangular Configuration	Cradle Configuration
0.65	0.78	1.36
0.70	0.87	1.43
0.75	0.97	1.47
0.80	1.08	1.51
0.85	1.20	1.50
0.90	1.32	1.46

Evaluation for Temperatures Other than 30 Degrees Centigrade

As stated previously, all the data in the present series were obtained at temperatures in the neighborhood of 30 degrees centigrade. Thus, the ratio of a-c to d-c resistance as evaluated above is at 30 degrees centigrade.

Table XIV gives data for calculation of skin effect of round conductors at 60, 65, 70, and 75 degrees centigrade if the skin effect at 30 degrees centigrade is known. It has been found convenient to use these data for estimation of all a-c losses. Wiseman[1] has found that this method may be used with good accuracy in connection with the data reported by him. One isolated check which we have made indicates that using the table to evaluate the ratio at a higher temperature from the ratio at a lower temperature is conservative. The evaluation may be carried out for the entire loss or for the loss up to any desired point. For example, assume the 30 degree ratios of a-c to d-c resistance to be 1.10 at the conductor, 1.20 at the shield and 1.40 at the pipe. From Table XIV, the ratios at 70 degrees centigrade conductor temperature will be 1.08 at the conductor, 1.16 at the shield and 1.33 at the pipe.

Table XIV. R_{ac}/R_{dc}

Temperature, Degrees Centigrade				
30	60	65	70	75
1.000	1.000	1.000	1.000	1.000
1.100	1.083	1.081	1.079	1.077
1.200	1.168	1.164	1.160	1.156
1.300	1.255	1.349	1.243	1.237
1.400	1.344	1.336	1.328	1.320
1.500	1.435	1.425	1.415	1.405
1.600	1.528	1.516	1.504	1.492
1.700	1.622	1.608	1.595	1.582
1.800	1.717	1.702	1.688	1.674
1.900	1.813	1.798	1.783	1.768
2.000	1.91	1.895	1.88	1.865

Brief Summary of Computation of Losses

1. Obtain Y_o from Table XI.
2. Obtain Y_{cp}' from Table XII.
3. Add Y_o and Y_{cp}' to obtain Y_c'.
4. Multiply Y_c', by factor from equation 7 to obtain Y_c for both triangular and cradle configurations.
5. Calculate Y_{sc} by equation 10 for triangular configuration, or by equation 10(A) for cradle configuration.
6. Calculate Y_{sp} by equation 13. This is negligible for cable with high-resistance shield.
7. Add Y_{sc} and Y_{sp} to obtain Y_s.
8. Obtain Y_t from Table XIII and equation 16.
9. $1 + Y_c + Y_s + Y_t$ = ratio of a-c to d-c resistance at 30 degrees centigrade.
10. Obtain ratio at desired temperature from Table XIV.

Attention is called to the fact that in making computations to compare "computed" loss values with measured values, as shown in Tables IV, V, and VI, the short-cut methods indicated in the equations given in this section were generally not used, but rather the more exact values determined from actual measured dimensions. Thus, the computed values shown in the tables will in some instances be slightly different than would be obtained from the short-cut equations. In all cases the short-cut equations give the more conservative values.

Sample Computation of Loss Ratio

Given a 2,000,000 circular mil segmental in 6.10-inch (inside diameter) pipe, triangular configuration, $d=1.58$, $p=6.10$, $S=2.42$, $s=2S/p=0.794$, $R_c=5.5$, $R_s=890$.

From Table XI, $Y_o=0.065$. From Table XII, $Y_{cp}=0.064$, and, combining $Y_c'=Y_o+Y_{cp}'=0.129$. From equation 7, $Y_c/Y_c'=1+[(28\times 1.58\times 0.84)/37.2]=2.00$. Therefore $Y_c=2.00\times 0.13=0.26$.

From equation 10, $Y_{sc}=600/(890\times 5.5)=0.12$. As Y_{sp} is negligible for this cable, $Y_s=0.12$. From Table XIII, $Q=1.07$, and therefore, from equation 16, $Y_t=0.02\times 6.10\times 2\times 1.07=0.26$.

Adding, $Y_c+Y_s+Y_t=0.64$. Thus, R_{ac}/R_{dc} at 30 degrees centigrade = 1.64. Consulting Table XIV, we see that R_{ac}/R_{dc} at 75 degrees centigrade = 1.53.

Discussion of Results

It is evident from the data that the construction of the protective covering, which for convenience we have called the "shield," may have a large effect on the losses. With a shield of very low resistance, the losses due to circulating currents and proximity effect in the shield may constitute 30 per cent of the total loss, or 50 per cent of the excess of a-c over d-c loss in the case of a 2,000,000-circular mil conductor. With smaller conductors this effect is much smaller, but is still of an important magnitude. Ordinarily, there is no justification in using a shield of resistance low enough to contribute to the losses to so large an extent. However, there are conditions, such as for example, to provide a path for carrier currents, which might make a continuous low resistance shield highly desirable and warrant acceptance of the resulting higher loss.

It is appropriate to develop the matter of shield resistance further. The "shields" on the cables here involved have generally consisted of two 3-mil copper tapes intercalated with non-metallic tapes or with each other, and two D-shaped copper wires, or skid wires, applied thereover, double start. The d-c resistance of the two 3-mil intercalated copper tapes is of the order of 300 microhms per foot of cable, which approximates closely the resistance of a copper tube with 6-mil wall, indicating that the contact resistance of copper to copper is extremely low. A method of correcting this condition is to intercalate the tapes with nonmetallic tapes, which results in producing a resistance in the two 3-mil copper tapes alone of about 5,000 microhms per foot of cable. However, the two skid wires (100 by 200 mils), which form a parallel path to the copper tapes, have a resistance of about 1,000 microhms per foot of cable, and the over-all resistance becomes about 250 microhms per foot of cable for the lower resistance shield and about 835 microhms per foot of cable for the higher resistance shield. In other words, in the case of the lower resistance shield the copper tapes are the main factor in determining the over-all resistance, whereas in the case of the higher resistance shield the skid wires are the main factor.

The matter of contact resistance between metal surfaces has not, in the opinion of the authors, received the attention that it merits. From limited experimental data not reported here, it appears that while two bright copper tapes intercalated with each other have very low contact resistance, two oxidized copper tapes have so high a contact resistance that the resistance of two such tapes intercalated with each other is no different than if they were separately intercalated with nonmetallic tapes. These data indicate further that there also is a satisfactorily high-contact resistance between bright copper and bright aluminum and between two bright aluminum tapes. Thus, if an entirely metallic shield is desired, any one of the last three mentioned alternatives is probably practical. Still another alternative would be to use tapes of higher resistivity, such as a high-resistance bronze or one of the nickel alloys, with attendant higher cost, however.

With any of the arrangements just mentioned, paralleled by two 100 by 200 mil copper skid wires, the resistance would be no higher than of the order of 850 microhms per foot of cable, because, as stated already, the skid wires are the chief determining factor of the resistance. In the case of the 2,000,000-circular-mil cable (number *3A*) the circulating current loss with a shield of this resistance is about 12 per cent of the d-c conductor loss. Suppose that with the same skid wire arrangement only one copper tape, with suitable intercalation, were used, the circulating loss would be reduced by only about 1 per cent in the case cited. Suppose that instead of using two skid wires, double start, only one skid wire were used, with one-half the lay (that is, the same separation between successive convolutions), the skid wire resistance would then be about 4,000 microhms per foot of cable and the parallel resistance with a tape structure having a resistance of 5,000 microhms per foot of cable would be about 2,200 microhms per foot of cable and the circulating current loss would be reduced from 12 per cent to about 4.5 per cent of the d-c conductor loss. One might go further and substitute skid wires of smaller cross-section or higher resistivity, but it should be noted that the largest gain possible is that represented by the loss due to the shield circulating current, which, with the higher resistance shields used in the measurements, is a relatively small portion of the total loss.

Mention has been made of the proximity effect loss in the shield. This is important only in the case of the low-resistance shields and is entirely negligible with those of higher resistance. It should be noted that there is no proximity loss in the skid wire portion of the shield because of the open spiral application.

The tests on segmental conductors reported here have been confined to treated segmental strand having three insulated segments and a bronze binder of about 40 per cent conductivity applied with a slight lap. A limited amount of experimental work also has been done on "treated" and "untreated" segmental strand, with various type binders and with both 2-segment and 3-segment insulation. The indications from these limited data are that with three insulated segments the conductor losses are affected very little by the foregoing variables, whereas when only two segments are insulated the effect of some of the variables on the conductor loss may be important. In this connection, it may be noted that the 2,500,000-circular mil segmental conductor reported by Wiseman,[1] which has two segments insulated and is untreated, has very much higher losses than would be obtained on treated strand with three insulated segments, based on data reported here. It is hoped that further study along the lines indicated in this paragraph can be pursued at an early date. However, segmental strand with three insulated segments is an effective means of keeping down conductor proximity losses.

It also may be stated that some measurements have been made on experimental segmental strand in which an insulating tape is inserted between the outer layer of wires in each segment and the inner layers. Preliminary measurements indicate that it may be possible by this means to reduce the conductor skin and proximity effect losses by a very material amount even from that obtainable with ordinary 3-segment insulation. Further work will have to be done before a definite report can be made. It is well to note, however, that in pipe-type cables the loss in the pipe itself is so large a part of the total loss that it is questionable whether there would be sufficient practical value in obtaining losses in the conductors much below what is obtainable with treated normal segmental strand having three insulated segments.

Consideration also might be given to the use of concentric conductors stranded with enamelled wires, which should have no proximity effect. With conductor proximity loss eliminated, the over-all loss in steel pipe may approach that with segmental conductors. The cost of such a conductor may be reduced by omitting the enamel on alternate wires of alternate layers, thereby still retaining at least one layer of enamel at every junction of adjacent wires.

At times the question has been raised as to the effect of short circuits occurring between segments of a segmental conductor. Theoretical considerations indicate that a single short circuit can have no effect on the losses, and multiple short circuits on the same segments, if spaced several lay lengths from each other, can have but inappreciable effect. The worst condition should theoretically occur if the short circuits are exactly one-half lay length apart, but even with such a condition the increased loss would occur only in the few feet between short circuits and would be insignificant with respect to the over-all loss. The matter was investigated with results shown in Appendix III. In these measurements, short circuits were deliberately inserted at each half-lay point of the entire 20-foot section of one of the conductors under test, and under this condition a maximum increase of 1.5 per cent of the d-c loss was noted with the cable in pipe. The short circuits were of a very low order of resistance. It may be concluded from these data that accidental short circuits between segments of segmental strand are of no practical consequence.

Data are given in Appendix IV of losses of pipe-type cable with 800,000-circular-mil Compack round conductors. Compack is a trade name describing a conductor in which all strands are applied with the same direction of lay and heavily rolled at each layer to produce a very compact assembly of wires. Analysis of the data indicates that the over-all losses are somewhat lower than would be indicated by Wiseman's[1] data. The measured ratio of a-c to d-c resistance in 6-inch pipe with closed triangular configuration is 1.23 at 30 degrees centigrade. Computation from Wiseman's formulas yields a value of 1.28 after addition of 0.033 for shield circulating current loss, which Wiseman's cables did not have. Further analysis of the data shows that the indicated proximity loss in pipe is about the same as in air for this cable, instead of being nine times as great by Wiseman's formula. It will doubtless be desirable to investigate this matter further. Theoretical considerations indicate that proximity loss in a unidirectional strand should be much lower than in the usual construction of alternating direction of lay for successive layers of strand.

On the basis of the data and equations presented a tabulation has been prepared of losses in pipe of segmental conductor cables ranging from 1,000,000 to 2,000,000-circular mil in steps of 250,000 circular-mil, with insulation thicknesses of 315, 480 and 560 mils and the following construction details:

1. Number of insulated segments—3.
2. Binder—5-mil 40 per cent or lower conductivity bronze tape intercalated with a paper tape.
3. Shield—one or two copper tapes, each

Table XV. Estimated Losses of Segmental Cables in Steel Pipe

Triangular Configuration—At 70 Degrees Centigrade

Conductor** Size, Circular Mils	Insulation Wall Thickness, Inches	Pipe, Inside Diameter, Inches	R_{ac}/R_{dc}		
			At Conductor	At Shield	At Pipe
1,000,000*	0.315	5.047	1.05	1.11	1.20
1,000,000	0.480	6.125	1.05	1.10	1.19
1,000,000	0.560	6.125	1.05	1.11	1.22
1,000,000	0.560	8.125	1.03	1.10	1.19
1,250,000*	0.315	6.125	1.07	1.13	1.24
1,250,000	0.480	6.125	1.07	1.14	1.29
1,250,000	0.480	8.125	1.06	1.14	1.25
1,250,000	0.560	8.125	1.06	1.14	1.26
1,500,000*	0.315	6.125	1.10	1.21	1.35
1,500,000	0.480	8.125	1.08	1.17	1.33
1,500,000	0.560	8.125	1.09	1.17	1.34
1,750,000*	0.315	6.125	1.14	1.25	1.44
1,750,000	0.480	8.125	1.11	1.21	1.42
1,750,000	0.560	8.125	1.12	1.21	1.43
2,000,000*	0.315	6.125	1.20	1.31	1.56
2,000,000*	0.315	8.125	1.15	1.28	1.48
2,000,000	0.480	8.125	1.16	1.27	1.51
2,000,000	0.560	8.125	1.17	1.27	1.54

* The ratio R_{ac}/R_{dc} for the wall thickness of 315 mils is given at 75 degrees centigrade.

** Conductor diameter assumed to be the same as for conventional concentric strand and conductor resistance values based on those given in Table XVI-A of Reference 3.

Table XVI. Estimated Losses of Segmental Cables in Steel Pipe

Cradle Configuration—At 70 Degrees Centigrade

Conductor** Size, Circular Mils	Insulation Wall Thickness, Inches	Pipe, Inside Diameter, Inches	R_{ac}/R_{dc} At Conductor	At Shield	At Pipe
1,000,000*	0.315	5.047	1.05	1.15	1.28
1,000,000	0.480	6.125	1.05	1.14	1.29
1,000,000	0.560	6.125	1.05	1.12	1.27
1,000,000	0.560	8.125	1.03	1.12	1.30
1,250,000*	0.315	6.125	1.07	1.20	1.39
1,250,000	0.480	6.125	1.07	1.17	1.36
1,250,000	0.480	8.125	1.06	1.17	1.40
1,250,000	0.560	8.125	1.06	1.16	1.39
1,500,000*	0.315	6.125	1.10	1.25	1.48
1,500,000	0.480	8.125	1.08	1.21	1.50
1,500,000	0.560	8.125	1.09	1.20	1.50
1,750,000*	0.315	6.125	1.14	1.29	1.57
1,750,000	0.480	8.125	1.11	1.26	1.60
1,750,000	0.560	8.125	1.12	1.24	1.60
2,000,000*	0.315	6.125	1.20	1.37	1.69
2,000,000*	0.315	8.125	1.15	1.32	1.70
2,000,000	0.480	8.125	1.16	1.31	1.73
2,000,000	0.560	8.125	1.17	1.31	1.74

* The ratio R_{ac}/R_{dc} for the wall thickness of 315 mils is given at 75 degrees centigrade.

** Conductor diameter assumed to be the same as for conventional concentric strand and conductor resistance values based on those given in Table XVI-A of Reference 3.

intercalated with a nonmetallic tape.

4. Skid wires—D-shaped copper 200 by 100 mils, applied double start with spacing of 1 to $1^1/_4$ inches between successive convolutions.

The losses have been computed with $K=3.8$ for conductor proximity loss and $K_1=0.02$ for pipe loss and are shown for 75 degree centigrade copper temperature for 315-mil insulation and 70 degrees centigrade for 480- and 560-mil insulation. These values are given in Table XV for triangular configuration and Table XVI for cradle configuration.

Analysis of the losses as shown in Tables XV and XVI shows that the overall losses for a given conductor size and configuration are surprisingly alike for the different insulation thicknesses. However, there are small, but perhaps important differences in the distribution of the losses.

Appendix I. Measurements—Methods and Accuracy

Apparatus and Methods

D-C Measurements

A standard Kelvin Bridge was employed for making all d-c resistance measurements.

A-C Measurements

A null a-c potentiometer method was employed for measuring the a-c resistance of the cables. The circuit is shown in Figure 3. The conductors under test were connected in a wye for 3-phase measurements and in series for single-phase measurements. The power was received from a bank of three single-phase transformers with secondaries connected in delta. The primaries of these transformers were connected through Variacs to a 3-phase step-type voltage regulator, the regulator being used for relatively coarse adjustments of current and the Variacs for fine adjustments. Currents in the three phases were accurately balanced. However, even a relatively large unbalance has generally been found not to affect the average a-c to d-c resistance ratio to any significant extent.

For both single-phase and 3-phase measurements, the reactive components of the conductor voltage drops are balanced out by means of astatic mutual inductors with primaries carrying the full conductor current. The resistance component is balanced with a resistance potentiometer carrying current from the secondary of a 160-to-1 current transformer, the primary of which carries the conductor current. The detector employed is an a-c galvanometer. The galvanometer field is energized by a phase shifter which permits a balance to be obtained first with the field in quadrature to the main current (inductance balance) and then with a field in phase with the main current (resistance balance). The standard resistances used with the potentiometer are noninductive. In the measurements reported here, three different standard resistances, of approximately 0.035, 0.065 and 0.10 ohm were used. In making the resistance determinations, the apparent a-c resistance was measured for each conductor, the average of the two or three measurements being taken as the average a-c resistance of the conductors.

The a-c potentiometer reads in steps of 0.1 per cent, but there is ample sensitivity, especially at the higher currents, to interpolate to one-fifth of this value. As the potentiometer readings were generally in the middle third of the scale, the over-all sensitivity was of the order of 0.04 per cent.

At the balance of the potentiometer, the following expression holds for the a-c resistance of the conductor

$$R_{ac}=\frac{Mr}{v}\cos\delta$$

where

R_{ac} = apparent a-c resistance of conductor under test

M = potentiometer setting, expressed as a decimal of the standard resistance

r = resistance of potentiometer in ohms

v = current transformer ratio

δ = phase angle of current transformer. This is about one degree at 160 amperes and about 30 minutes at 800 amperes in the transformers used. Thus, $\cos\delta=1$ for all practical purposes

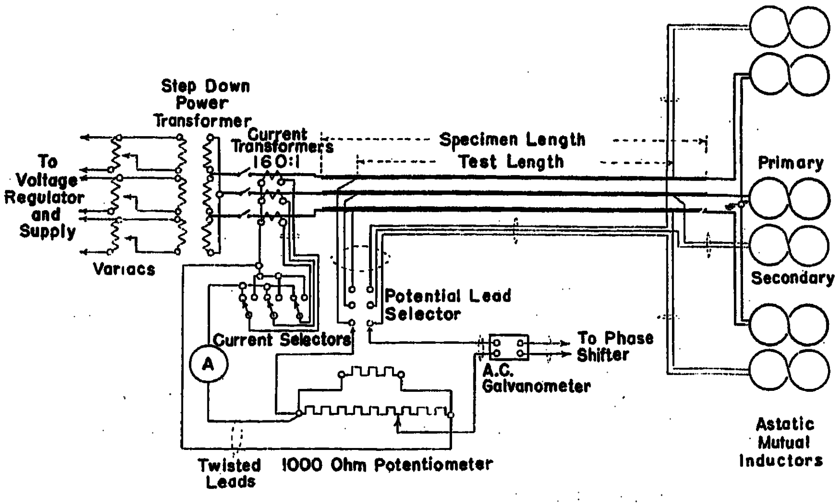
A simplified vector diagram for the balanced condition is shown in Figure 4.

Skin effect measurements were made single-phase with two conductors placed parallel to one another and spaced nine inches between opposing surfaces. For 3-phase measurements, three test conductors were employed, the operation of the circuit being substantially the same as in the single phase case. The three conductors were placed in triangular or cradle configuration as desired.

A typical set of 3-phase data is given in Table XVII.

Figure 3. Schematic diagram of connections for a-c resistance measurements under 3-phase load

For single-phase measurements, one conductor and one mutual inductor are omitted



The wide variation in the apparent resistance of the three conductors has been observed whenever 3-phase measurements have been made. Indeed, with cradle configuration one of the readings is often negative. This has been observed by other investigators[2] and is due to dissymmetry of conductor and lead arrangement. However, while the individual readings do not truly reflect the division of losses in the individual conductors, the average value is the true average a-c resistance as in any other 3-wattmeter method of 3-phase measurement. Heat runs with triangular configuration have shown no significant differences in the temperature rise of the three conductors, indicating no large differences in loss actually generated in the individual conductors.

For single-phase measurements, potential leads from the east potential points were carried along the tops of the north and south conductors and contiguous thereto, to the west potential points, where they were twisted together and carried to the measuring circuit. This method of carrying the potential leads to the measuring circuit was found to give true skin effect indication in cases where these could be checked against accepted theoretical values.

Salter[2] in his recent paper discusses in detail the accuracy of various methods of disposing the potential leads. Our experience over a period of some 13 years has paralleled Salter's and we have arrived independently at the same solution.

For 3-phase measurements in air, the potential leads from the three potential points at the east end were twisted together, and carried perpendicularly to a point $1^1/_2$ feet above the cables. From there they were carried parallel to the cables to a point perpendicularly above the west end potential points where they were joined by the three twisted leads from the west end potential points. From this point, all six leads were twisted together and carried to the measuring circuit.

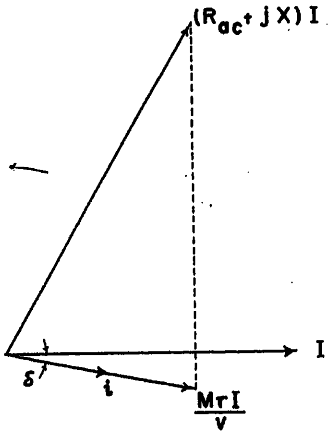


Figure 4. Vector diagram of a-c potentiometer circuit at balance

X = reactance of conductor, ohms
R_{ac} = a-c resistance of conductor, ohms
r = resistance of potentiometer, ohms
M = potentiometer setting expressed as a decimal
I = conductor current, amperes
i = current in current transformer secondary, amperes
v = current transformer ratio I/i
δ = phase angle of current transformer
At Balance: $R_{ac}I = Mr\ i \cos\delta$. $\cos\delta \cong 1$

Table XVII. A-C Resistance Determinations of Cable 2A 1,500,000-Circular-Mil Segments in 8.02 Inch Inside Diameter Steel Pipe, Triangular Configuration

Resistance of Standard—0.0658 Ohm; Current Transformer Ratio—160.1; Temperature—30 Degrees Centigrade

A-C Measurements

Shields Short Circuited. Distance between Potential Points, 15 Feet

Current, Amperes	Potentiometer Settings at Balance Phase *A*	Phase *B*	Phase *C*	Average	Average Resistance, Microhms	Average Ratio, R_{ac}/R_{dc}
160	0.343	0.275	0.475	0.364	149.5	1.37
320	0.318	0.252	0.495	0.355	146.0	1.34
480	0.318	0.252	0.495	0.355	146.0	1.34
640	0.320	0.262	0.479	0.354	145.7	1.34
800	0.325	0.268	0.462	0.352	144.8	1.33

D-C Measurements

D-c Resistance of Conductors in Microhms

Immediately Before A-C Run Phase A	Phase B	Phase C	Immediately After A-C Run Phase A	Phase B	Phase C
109.1	109.3	108.6	109.4	109.5	108.8

When the cables were placed in the pipe, the potential leads from the three potential points at each end were twisted together and carried away perpendicularly from the cables through holes in the pipe opposite the potential points. They were then carried to the measuring circuit in the manner described for 3-phase measurements in air.

CALORIMETRIC DETERMINATION OF PIPE LOSS

The portion of increase in the a-c resistance due to the pipe loss was determined by a calorimetric method.

The three cables were placed in the pipe, which was in a closed area, free of air currents. With the desired configuration of conductors, and shields insulated from one another, 3-phase current was passed through the conductors for a period of 10 minutes. By means of the Kelvin Bridge, the pipe resistance was measured each minute for a period of 10 minutes before the current was turned on; and each minute for a period of 10 minutes after the current was turned off. From these readings, the rise in the pipe resistance during the 10-minute period that the current was on, was determined.

After a suitable rest period there was passed through the pipe for 10 minutes direct current of such magnitude as to produce substantially the same resistance increase as was produced during the a-c run. In order to predetermine the necessary magnitude of this current it was usually necessary to make one or more trial runs. As in the case of the a-c run, pipe resistance was measured by means of the Kelvin Bridge each minute during the 10-minute period immediately preceding and that immediately following the current run, and from these measurements the resistance increase determined.

During the a-c run, heat was being generated in the conductors as well as in the pipe and some of the heat from the conductors was being transferred to the pipe. Thus, the increase in pipe resistance was due to both the heat generated in the pipe and (to a small extent) that transferred to the pipe from the conductors. In order to reproduce this condition during the d-c run, while direct current was being passed through the pipe, a direct current of a different magnitude was simultaneously passed through the three conductors in series. The magnitude of the conductor current was such as to produce substantially the same heating effect in the conductors as did the alternating current. From previous a-c resistance measurements, an estimate of the skin-proximity loss of the conductors, sufficiently accurate for this purpose, can be made, and the necessary value of direct current to be passed through the conductors calculated. Thus, estimating 10 per cent skin-proximity effect in a given case, the direct current which must be passed through the conductors should be $\sqrt{1.10}$ times the current used during the a-c run.

The watts fed into the pipe during the d-c run being known, the watts generated in the pipe during the a-c run can be calculated readily by multiplying the d-c value by the ratio of the pipe resistance increase during the a-c run to that during the d-c run.

The probable accuracy of this calorimetic method is believed to be about 3 per cent of the pipe loss.

A set of data and a sample calculation for the determination of pipe loss are given in the example below.

EXAMPLE

Given three 1,500,000-circular-mil segmental conductors in 6.10-inch inside diameter steel pipe; triangular configuration; 2.09 inch center-to-center spacing.

A-c Run. 783 amperes, 3-phase, through conductors for 10 minutes. Average d-c conductor resistance during run—106.9 microhms.

D-c conductor watts $=3\times\overline{783}^2\times106.9\times10^{-6}=197$ watts. Increase in pipe resistance in 10 minutes—0.29 microhm.

D-c Run. 430 amperes, direct current, through pipe for ten minutes. (Simultaneously 840 amperes were passed through the three conductors in series on the basis of estimated 15 per cent skin-proximity loss, that is, $783\times\sqrt{1.15}=840$.)

Average pipe resistance during d-c run—183.2 microhms, and therefore watts fed into pipe during d-c run $=430^2\times 183.2\times 10^{-6}$, or 33.8 watts. The increase in pipe resistance in 10 minutes was 0.32 microhm. Calculating, the loss in pipe during a-c run $=33.8\times(0.29/0.32)$, or 30.6 watts. D-c conductor loss during a-c run equals 197 watts, and therefore pipe loss equals 30.6/197 or 0.16 in terms of d-c resistance of the conductor.

Accuracy of Resistance Measurements

D-C MEASUREMENTS

A Leeds and Northrup standard Kelvin Bridge, with a suitable d-c galvanometer and scale, was employed for making the d-c resistance measurements. Numerous checks against known laboratory resistances established the accuracy of these measurements to be better than ±0.1 per cent.

A-C MEASUREMENTS

Possible sources of error in the a-c measurements and how they are taken care of will be indicated.

1. The out-of-phase component of the conductor voltage drop is balanced out by voltage from the secondary of a mutual inductor, the primary of which carries the full conductor current. The primary lead and the primary coil are made of a cable employing a litz-wire construction. Thus, quadrature between the secondary voltage and conductor current is assured. Both the primary and secondary windings are astatic, thus assuring a minimum of interference from stray fields.

The use of a mutual inductor taking its primary current directly from the line is believed to be unique. Salter,[2] following common practice, takes the primary current for his mutual inductor from the secondary of a current transformer with primary carrying the full conductor current. With such an arrangement, correction must be made for the phase angle of the current transformer, and the phase angle must be known very accurately.

2. The in-phase component of the conductor voltage drop is balanced by a resistance potentiometer which takes its current from a current transformer, the primary of which is in series with the conductor. The cosine of the phase angle of this current transformer appears in the resistance balance formula as a multiplying factor. However, the phase angle of the current transformer used is no greater than one degree under the conditions of the test, and thus the cosine is 0.9998 or higher and may be regarded as unity for all practical purposes. The ratio of the current transformers, as calibrated recently, ranges from 160.0 to 160.2 over the range of currents used in the measurements and a ratio of 160.1 has been used for computing resistances at all currents, introducing a maximum error of ±0.06 per cent.

The standard resistances used with the potentiometer are essentially noninductive and are calibrated by means of the same Kelvin Bridge as was used for the d-c measurements. It should be noted that a small phase angle in the standard resistance is unimportant for the same reason as given for the phase angle of the current transformers.

3. A possible source of error is variation in the temperature of the conductors between the start and finish of a series of measurements. A technique was developed whereby the current was turned on only momentarily for each adjustment of the potentiometer or of the mutual inductor when balancing, thus heating up the conductor to an insignificant extent during a given series of measurements. D-c resistance measurements were made just before and just after each series of a-c measurements and, as illustrated in Table XVII, changed by only a small fraction of 1 per cent.

Several checks and comparative measurements with other laboratories were made for greater assurance of accuracy of the a-c resistance measurements, as detailed later.

When the equipment used in the present tests was first assembled, about ten years ago, a calibration measurement was made on copper tubes of 1.90 inch outside diameter and 1.49 inch inside diameter. A-c to d-c resistance ratio was measured with 30-inch separation at 20 degrees centigrade. The measured values were 1.005 and 1.006 for the two conductors, as compared with a theoretical skin effect value of 1.010.

The conductors were then brought together to a center-to-center separation of 2.26 inches. Two determinations of the ratio gave values of 1.307 and 1.310, respectively, at 400 amperes. The computed ratio from accepted theory[5] is 1.310.

In the foregoing measurements the distance between potential points was 18 feet.

During the course of the present series of measurements cable *3A* (2,000,000 circular mils) together with the 6-inch (nominal) inside diameter steel pipe were sent to the Electrical Testing Laboratories for comparative measurements with their equipment. Comparative values are given in Table XVIII.

Table XVIII. Comparative Tests with ETL 2,000,000-Circular-Mil Segmental Cable
Spacing 2.42 Inches, Center-to-Center, Shields Short Circuited

	A-c to D-c Resistance Ratio Authors	ETL
Triangular configuration, in air, 3-phase measurement	1.19	1.205
Triangular configuration, in 6-inch inside diameter steel pipe, 3-phase measurement	1.63	1.65

The ETL measurements in pipe were made with 15 feet between potential points, according to their standard method. Our measurements were made with 20 feet between potential points. The ETL measurements were at about 600 amperes, while ours were at 640 amperes. The agreement is satisfactory considering that the measurements were made with two separate setups.

Another check on our apparatus was obtained while making joint studies with Philadelphia Electric Company. An electrodynamometer-type wattmeter was supplied by Philadelphia Electric Company and connected into the circuit so as to measure the watts loss in each conductor simultaneous to measurement by the a-c potentiometer. The cable was installed in a 6-inch inside diameter steel pipe, in equilateral triangular formation. The shields, including skid wires, were touching throughout the test length. Comparative values between the wattmeter and the potentiometer method are shown in Table XIX. It is to be observed that the two methods checked each other extremely well. The watts loss listed under the a-c potentiometer column were calculated from the measured value of the a-c resistance, by using the measured value of conductor current.

Table XIX. Comparative Wattmeter and Potentiometer Readings

	Total Watts Loss Wattmeter	A-c Potentiometer
Room temperature (31 degrees centigrade)	713	714
At final conductor temperature (68.5 degrees centigrade)	732	736

Note also the measurement of skin effect of a round conductor reported in Appendix IV, on which the measured value was 1.056, as compared with a theoretical value of 1.057.

The check measurements given appear to give an excellent check on the a-c measurements. It is believed that the probable accuracy of these measurements is within ±0.5 per cent or better in all cases.

Appendix II. Pipe Loss in a Balanced 3-Phase Pipe-Type System

Nonmagnetic Pipe

Consider three conductors in random arrangement in a cylindrical pipe and carrying balanced 3-phase current, I. (See Figure 5.) Assume that the currents are concentrated at the centers of the respective conductors and that shield and pipe currents do not affect the flux density or distribution. For convenience of computation, the return currents of all three phases are assumed to be at the center of the pipe. This is permissible since the three currents add up to 0. The voltage induced in pipe element $rtd\theta$, expressed as a complex quantity, is

$$e=5.08\omega I\times 10^{-9}\left(\log_e \frac{X_1}{r}+\frac{-1+j\sqrt{3}}{2}\log_e \frac{X_2}{r}+\frac{-1-j\sqrt{3}}{2}\log_e \frac{X_3}{r}\right)$$

$$=5.08\omega I\times 10^{-9}\left(\log_e x_1+\frac{-1+j\sqrt{3}}{2}\log_e x_2+\frac{-1-j\sqrt{3}}{2}\log_e x_3\right) \quad (17)$$

or

$$e=5.08\omega I\times 10^{-9}\times \sqrt{(\log_e x_1)^2+(\log_e x_2)^2+(\log_e x_3)^2-\log_e x_1\log_e x_2-\log_e x_2\log_e x_3-\log_e x_3\log x_{1e}} \quad (18)$$

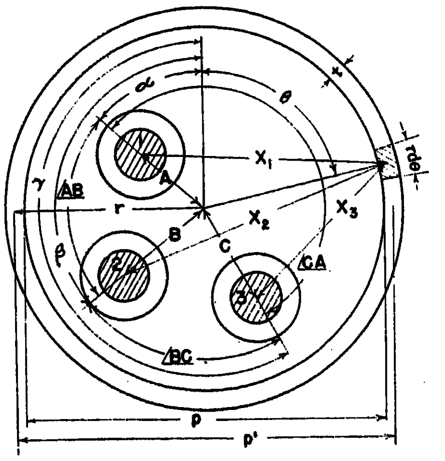


Figure 5. Graphic representation of notation in deriving pipe loss formula

$a=A/r;\ b=B/r;\ c=C/r;\ x_1=X_1/r;\ x_2=X_2/r;$
$x_3=X_3/r$

The resistance of pipe element $rtd\theta$, in ohms per inch length, being $\rho\times10^{-6}/rtd\theta$, the watts loss per inch length in the element $rtd\theta$ is

$$dw=\frac{e^2rtd\theta}{\rho\times10^{-6}}=\frac{25.8\omega^2I^2rt\times10^{-12}}{\rho}\times$$
$$[(\log_\epsilon x_1)^2+(\log_\epsilon x_2)^2+(\log_\epsilon x_3)^2-\log_\epsilon x_1\log_\epsilon x_2-\log_\epsilon x_2\log_\epsilon x_3-\log_\epsilon x_3\log_\epsilon x_1]d\theta \tag{19}$$

and

$$w=\frac{25.8\omega^2I^2rt\times10^{-12}}{\rho}\times$$
$$\int_0^{2\pi}[(\log_\epsilon x_1)^2+(\log_\epsilon x_2)^2+(\log_\epsilon x_3)^2-\log_\epsilon x_1\log_\epsilon x_2-\log_\epsilon x_2\log_\epsilon x_3-\log_\epsilon x_3\times\log_\epsilon x_1]d\theta \tag{20}$$

This may be written as

$$w=\frac{6.45\omega^2I^2rt\times10^{-12}}{\rho}\times$$
$$\int_0^{2\pi}[(\log_\epsilon x_1^2)^2+(\log_\epsilon x_2^2)^2+(\log_\epsilon x_3^2)^2-\log_\epsilon x_1^2\log_\epsilon x_2^2-\log_\epsilon x_2^2\log_\epsilon x_3^2-\log_\epsilon x_3^2\log_\epsilon x_1^2]d\theta \tag{21}$$

By the law of cosines

$$x_1^2=1+a^2-2a\cos(\theta+\alpha)=[1-a\epsilon^{j(\theta+\alpha)}][1-a\epsilon^{-j(\theta+\alpha)}]$$
$$x_2^2=1+b^2-2b\cos(\theta+\beta)=[1-b\epsilon^{j(\theta+\beta)}][1-b\epsilon^{-j(\theta+\beta)}]$$
$$x_3^2=1+c^2-2c\cos(\theta+\gamma)=[1-c\epsilon^{j(\theta+\gamma)}][1-c\epsilon^{-j(\theta+\gamma)}] \tag{22}$$

Substituting these values of x_1^2, x_2^2 and x_3^2 in equation 21 we have

$$w=\frac{6.45\omega^2I^2rt\times10^{-12}}{\rho}\times$$
$$\int^{2\pi}[g_1^2+2g_1g_2+g_2^2+h_1^2+2h_1h_2+h_2^2+m_1^2+2m_1m_2+m_2^2-g_1h_1-g_2h_2-g_2h_1-g_1h_2-g_1m_1-g_1m_2-g_2m_1-g_2m_2-h_1m_1-h_1m_2-h_2m_1-h_2m_2]d\theta \tag{23}$$

The 21 terms of equation 23 may be integrated separately and will yield

$$w=\frac{25.8\pi\omega^2I^2rt\times10^{-12}}{\rho}\times$$
$$\sum_{n=1}^{\infty}\frac{a^{2n}+b^{2n}+c^{2n}-a^nb^n\cos n\underline{|AB}-b^nc^n\cos n\underline{|BC}-c^na^n\cos n\underline{|CA}}{n^2}$$

or

$$w=\frac{25.8\pi\omega^2I^2rt\times10^{-12}}{\rho}Q \tag{24}$$

Now

$$Y_i=\frac{w}{w_c}$$

and

$$w_c=\frac{3I^2\rho_c\times10^{-6}}{0.25\pi(CI)}=\frac{3.82I^2\rho_c\times10^{-6}}{(CI)}$$

Therefore

$$Y_i=\frac{25.8\pi\omega^2rt(CI)Q\times10^{-6}}{3.82\rho\rho_c}$$

or

$$Y_i(\text{nonmagnetic})=\frac{1.51p't(CI)Q}{\rho\rho_c}\quad\text{at 60 cycles} \tag{14}$$

Magnetic Pipe

If the pipe material is magnetic, only the inner portion of the wall thickness of the pipe will carry induced current. As a first approximation it may be assumed that the portion of wall thickness carrying the current is the same as the depth of penetration. From the data in Table II the depth of penetration at the current giving the highest a-c resistance is 12.2×0.28/159=0.0215 inch for the 6-inch pipe, and 7.65×0.32/152 =0.0161 inch for the 8-inch pipe. Substituting in equation 14 the above depths of penetration for t, the values given in Table II for ρ and 0.72 for ρ_c (resistivity of copper at 30 degrees centigrade, adding 2 per cent for stranding and cabling of segments), and replacing p' by p, the right-hand member of equation 14 becomes 0.0079 p (CI) Q for the 6-inch pipe and 0.0066 p (CI) Q for the 8-inch pipe, as first approximations.

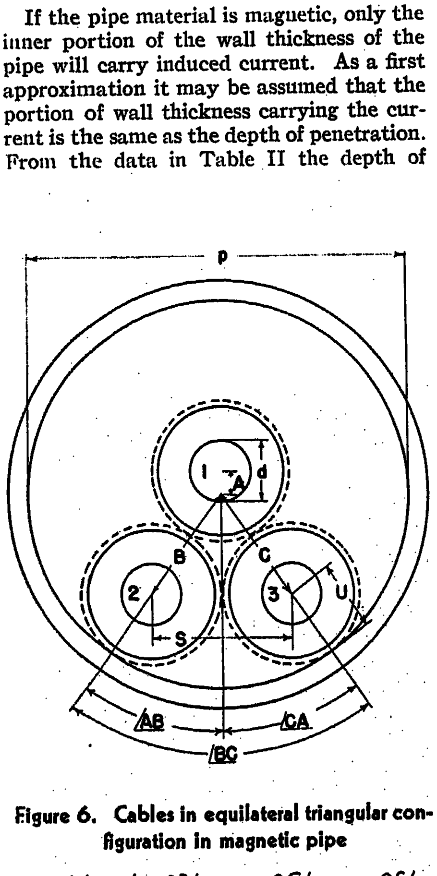


Figure 6. Cables in equilateral triangular configuration in magnetic pipe

$a=2A/p;\ b=2B/p;\ c=2C/p;\ s=2S/p;$
$u=2U/p$
$b=c=1-u$
$a=\sqrt{b^2-s^2/4}-\sqrt{\frac{3}{2}}s$
$\underline{|AB}=\underline{|CA}=\sin^{-1}s/2b$
$\underline{|BC}=2\sin^{-1}s/2b$

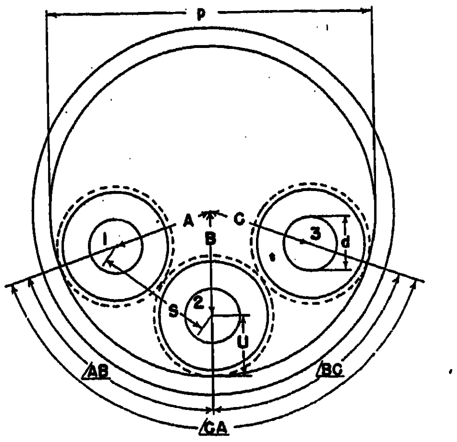


Figure 7. Cables in cradle configuration in magnetic pipe

$a=2A/p;\ b=2B/p;\ c=2C/p;\ s=2S/p;$
$u=2U/p$
$a=b=c=1-u$
$\underline{|AB}=\underline{|BC}=2\sin^{-1}s/2b$
$\underline{|CA}=4\sin^{-1}s/2b$

However, the presence of the magnetic material not only increases the flux but also tends to concentrate the flux in the material of the pipe, thus producing higher losses than calculated with the foregoing assumptions.

Calorimetric determinations of the loss in the two steel pipes used in the measurements reported in the paper (see Table VII) have indicated that the numerical factors in these expressions are about 2.6 times the values indicated, that is to say, 0.020 for the 6-inch pipe and 0.017 for the 8-inch pipe. For conservatism, and because the 6-inch pipe more nearly represents the type of pipe likely to be used in pipe installations, it is recommended that 0.020 be used. Thus,

$$Y_i\left(\begin{matrix}\text{magnetic}\\ \text{60 cycles}\end{matrix}\right)=0.020p(CI)Q\ \text{at 30 degrees centigrade} \tag{16}$$

Evaluation of Q for Practical Cases

The evaluation of Q for random distribution of the cables in the pipe is a time-consuming process. Generally, however, cables are considered to be arranged either in equilateral triangular configuration or in cradle configuration, as shown graphically in Figures 6 and 7. For these cases the various distance ratios and angles involved are subject to considerable simplification.

Thus, for equilateral triangular configuration

$$b=c=1-u$$

$$a=\sqrt{b^2-\frac{s^2}{4}}-\sqrt{\frac{3}{2}}s$$

$$|\underline{AB}=|\underline{CA}=\sin^{-1}\frac{s}{2b}$$

$$|\underline{BC}=2\ \sin^{-1}\frac{s}{2b} \qquad (25)$$

For cradle configuration

$$a=b=c=1-u$$

$$|\underline{AB}=|\underline{BC}=2\ \sin^{-1}\frac{s}{2b}$$

$$|\underline{CA}=4\ \sin^{-1}\frac{s}{2b} \qquad (26)$$

Note that $\cos\phi=\cos(-\phi)$.

Thus, in evaluating say $|\underline{AB}$ for the purposes of the equation for Q it is immaterial whether α is subtracted from β or β is subtracted from α. It will be most convenient to subtract in a manner which will produce a positive angle.

It should be noted further that equations 25 apply to any equilateral triangular arrangement, regardless of whether or not the cables touch the inner pipe surface; also that equations 26 apply to any arrangement wherein all conductors are equally distant from the center of the pipe and the two outside cables are symmetrical with respect to the center cable.

Where adjacent cables are touching, as they are normally considered to be, u for both equilateral and cradle configurations will be equal to the over-all radius of the cables and very nearly equal to $s/2$. Table XIII gives the values of Q for this condition. The values of Q for both the equilateral triangular and cradle configurations in the broad sense explained before are given graphically in Figure 2.

Appendix III. Effect of Accidental Short Circuits Between Segments

A series of tests was performed to determine the effect of accidental short circuits between segments. In order to accentuate the effect, the measurements were made with short circuits deliberately imposed at frequent intervals. The results of these tests are given in Table XX.

The short circuits were placed in the top conductor only. Copper wedges, driven between adjacent segments, served as the short circuits for these determinations. The effective resistance of these short circuits was 65±20 microhms. Short circuits between bare and opposite segments were obtained by removing the segment insulation from the segment opposite the bare segment. Copper wedges were driven between the bronze binder tape and the insulated opposite segment at the place where the insulation was removed. The effective resistance of these short circuits was 345±50 microhms.

From the results, it is evident that the short circuiting of the bare segment with either an adjacent or the opposite insulated segment by means of several low-resistance short circuits spaced one-half lay length apart has no measurable effect on the a-c resistance of a segmental cable system in air. Similar short circuits have measurable but slight effect on the a-c resistance of cables in steel pipe. It may be concluded that occasional accidental short circuits between segments have an insignificant effect on the losses.

Table XX. Effect of Short Circuits

Cable 1B with Shield Removed—Triangular Configuration (S=2.35 Inches)

	Three-Phase A-c to D-c Resistance Ratio
Short Circuits Between Bare and Adjacent Segments	
Cable in Air	
No short circuits	1.022*
Short circuits at each lay length	1.023
Short circuits at each one-half lay length	1.023
Short circuits at each one-half lay length, but top conductor rotated 90 degrees	1.023
Cable in 6.10-Inch Inside diameter Steel Pipe	
No short circuits	1.178
Short circuits at each one-half lay length	1.188
Short circuits at each one-half lay length, but top conductor rotated 90 degrees	1.194
Short Circuits Between Bare and Opposite Segments	
Cable in Air	
No short circuits	1.027*
Short circuits at each lay length	1.027
Short circuits at each one-half lay length	1.027
Cable in 6.10-Inch Inside Diameter Steel Pipe	
No short circuits	1.180
Short circuits at each one-half lay length	1.186

* Difference between these two values probably due to some difference in separation.

Appendix IV. Losses in Cable with "Compack" Round Conductors

A set of a-c resistance measurements was made on cable having Compack round conductors. The results are given here for purposes of comparison and as a matter of interest.

The cable tested had an 800,000-circular-mil Compack round conductor with a conductor diameter of 0.95 inch and an insulation wall of 0.540 inch. The protective

Symbols

A, B, C = distances from center of pipe to respective centers of conductors 1, 2 and 3, in inches

$a=A/r$; $b=B/r$; $c=C/r$ for nonmagnetic pipe. $a=2A/p$; $b=2B/p$; $c=2C/p$ for magnetic pipe

$\underline{AB}$ = angle formed between lines joining center of pipe with centers of conductors 1 and $2=\beta-\alpha$

$\underline{BC}$ = angle formed between lines joining center of pipe with centers of conductors 2 and $3=\gamma-\beta$

$\underline{CA}$ = angle formed between lines joining center of pipe with centers of conductors 3 and $1=\alpha-\gamma$

(CI) = conductor area in circular inches

e = rms volts per inch length induced in pipe element $rtd\theta$

f = frequency in cycles per second

$g_1=\log_\epsilon[1-a\epsilon^{j(\theta+\alpha)}]$ $g_2=\log_\epsilon[1-a\epsilon^{-j(\theta+\alpha)}]$

$h_1=\log_\epsilon[1-b\epsilon^{j(\theta+\beta)}]$ $h_2=\log_\epsilon[1-b\epsilon^{-j(\theta+\beta)}]$

$m_1=\log_\epsilon[1-c\epsilon^{j(\theta+\gamma)}]$ $m_2=\log_\epsilon[1-c\epsilon^{-j(\theta+\gamma)}]$

I = current per phase in rms amperes

p = internal pipe diameter in inches

p' = mean pipe diameter in inches

$$Q=\sum_{n=1}^{\infty}\frac{a^{2n}+b^{2n}+c^{2n}-a^nb^n\cos n|\underline{AB}-b^nc^n\cos n|\underline{BC}-c^na^n\cos n|\underline{CA}}{n^2}$$

r = mean pipe radius in inches

S = distance between centers of adjoining conductors in inches

$s=2S/p$

t = wall thickness of pipe in inches

U = distance from inner periphery of pipe to center of contiguous conductor in inches

$u=2U/p$

w = watts loss per inch length of pipe

w_c = d-c watts loss per inch length in the three conductors

X_1, X_2, X_3 = distances from center of pipe element $rtd\theta$ and respective centers of conductors 1, 2, and 3, in inches

$x_1=X_1/r$; $x_2=X_2/r$; $x_3=X_3/r$

Y_l = loss in pipe expressed as a decimal of w_c

α, β, γ = angles formed between reference line and lines joining center of pipe to respective centers of conductors 1, 2, and 3

ϵ = Naperian base of logarithms

θ = angle formed between reference line and line joining center of pipe to center of pipe element $rtd\theta$

ρ = resistivity of pipe material, in microhm-inches

ρ_c = resistivity of conductor material, in microhm-inches

ω = angular velocity $=2\pi f$

Table XXI. Losses of 800,000 Circular Mils Compack Round

Air or Pipe	Configuration	S, Inches	p, Inches	R_{ac}/R_{dc} Shields Insulated	R_{ac}/R_{dc} Shields Short Circuited
Air	Triangular	2.25		1.07	1.09
Air	Triangular	3.10		1.06	1.11
Air	Cradle	2.25		1.07	1.10
Pipe	Triangular	2.25	6.10		1.23
Pipe	Triangular	3.10	6.10		1.34
Pipe	Cradle	2.25	6.10		1.32

shield consisted of two 3-mil copper tapes, each intercalated with a 7-mil polyethylene tape, with two 0.100- by 0.200-inch skid wires, applied double start, with a spacing of $1^1/_4$ inches between successive convolutions.

Measurement of skin effect produced a value of 1.056. This compared with a value of 1.057 as calculated from accepted formulas for round homogeneous conductors.

Results of 3-phase measurements in air and in steel pipe are given in Table XXI.

References

1. A-C Resistance of Large Size Conductors in Steel Pipe or Conduit, R. J. Wiseman. *AIEE Transactions*, volume 67, part II, 1948, pages 1745–57.

2. Problems in the Measurement of A-C Resistance and Reactance of Large Conductors, E. H. Salter. *AIEE Transactions*, volume 67, part II, 1948, pages 1390–96.

3. Specifications for Varnished-Cambric Insulated Cables for the Transmission and Distribution of Electrical Energy. Insulated Power Cable Engineers Association, fifth edition, July 1946.

4. Eddy Current Losses in Multi-Core Paper-Insulated Lead-Covered Cables, Armoured and Unarmoured Carrying Balanced 3-Phase Currents. A. H. M. Arnold. *Journal*, Institution of Electrical Engineers (London, England), volume 88, part II, number 1, February 1941.

5. Proximity Effect in Solid and Hollow Round Conductors, A. H. M. Arnold. *Journal*, Institution of Electrical Engineers (London, England), volume 88, part II, number 4, August 1941, page 349.

6. A Critical Study of a Three-Phase System of Unarmored Single-Conductor Cables, from the Standpoint of the Power Losses, Line Constants and Interference with Communication Circuits, E. B. Wedmore, P. D. Morgan, S. Whitehead. *Journal*, Institution of Electrical Engineers (London, England), volume 67, 1929, page 359.

7. Losses, Induced Volts and Amperes in Armor and Lead Cover of Cables, H. W. Fisher. *AIEE Transactions*, volume XXVIII, part II, 1909, pages 747–65.

8. Calculation of the Electrical Problems of Transmission by Underground Cables, D. M. Simmons. *Electric Journal* (East Pittsburgh, Pa.), 1925, volume 22, page 366.

9. Reactance of Large Cables in Steel Pipe or Conduit, W. A. DelMar. *AIEE Transactions*, volume 67, part II, 1948, pages 1409–15.

10. Thermal Characteristics of a 120-Kv High-Pressure, Gas-Filled Cable Installation, W. D. Sanderson, J. Sticher, M. H. McGrath. *AIEE Transactions*, volume 67, part I, 1948, pages 487–502.

Discussion

C. T. Hatcher (Consolidated Edison Company of New York, Inc., New York, N. Y.): Proper selection of cable size to meet carrying capacity requirements has been a troublesome phase in the engineering of pipe cable lines, especially in the case of the heavier circuits installed during the past few years. Basic technical data were limited with respect to vital factors involved in the rating of these systems—namely, component a-c losses and thermal resistance of the soil.

Considerable research has been carried on during that time on both of these subjects and the situation today is considerably improved. Basic data are being obtained, and the underlying theory governing effect is being developed.

This paper is a major contribution to our knowledge of the behavior of a-c losses in pipe cable systems and a very necessary supplement to Mr. Wiseman's Insulated Power Cable Engineers Association[1] report. As a result of these two studies, the general theory has been more adequately developed and, with the more complete understanding now furnished of the component sources of a-c losses in pipe, the direction of attack towards reduction of these losses becomes apparent.

The authors have not claimed to offer a complete answer to the problem and, because of the number of variables involved, further tests are deemed desirable. The need for additional test data has been recognized generally by the industry. Some months ago the Consolidated Edison Company was confronted with the need for specific data in order to evaluate properly design features proposed by cable manufacturers for several projected 138-kv pipe cable lines. A research program, to be conducted by Electrical Testing Laboratories, was authorized for study of the component losses in cables of 1,500,000-circular-mil size. Because of the concurrent interest of other utilities, an Association of Edison Illuminating Companies—Edison Electric Institute Working Group was appointed to sponsor further research on this subject. In setting up the Consolidated Edison Company research program, the cable manufacturers and members of the AEIC—EEI Working Group were invited to participate in a series of discussions with a view to formulating a comprehensive program of research which might be correlated with possible further research under AEIC—EEI sponsorship.

The test program as set up at joint conferences was patterned very much after the lines pursued by Mr. Meyerhoff and Mr. Eager. Basic tests were to be made to determine: conductor loss; loss in shield and skid wires; and loss in pipe. Tests have been in progress for some months and a number of measurements of a-c/d-c resistance ratio have been made in air and in pipe on various samples of 1,500,000-circular-mil 138-kv cable from different manufacturers. All of these samples are of the "high resistance shield" type, corresponding approximately to sample 2*A* of the authors' tests. Three-phase measurements in air and in pipe were made on a number of samples in two configurations of the authors' tests and, in addition, in a "wide triangle" configuration, that is, with maximum separation of the cables as fixed by the inside diameter of the pipe. The addition of the latter configuration was dictated by an interest in the limits of resistance ratio, particularly as 300 feet vertical riser sections are to be installed on two tunnel crossings under the East River and in these riser sections the cables hang free.

To date no measurements of pipe loss have been made, and the data so far obtained have not been completely analyzed yet. However, the measured total a-c/d-c resistance ratios check reasonably well with the authors' computed values for the triangle configuration in air, but are higher for both configurations in pipe. The average of test data gave total loss ratios in pipe of 1.40 in the triangle configuration and 1.62 in the cradle configuration. These are dependent somewhat on details of construction.

The test results obtained in the "wide triangle" configuration indicate that values greater than those of the cradle formation may result with random lay in the field and this has been borne out by field test measurements made on two existing lines.

In their discussion of results, the authors have pointed out the line of attack in reducing the "shield" circulating current loss by increasing skid wire circuit resistance. Some tests have been made on samples with modified skid wire design which bear out this point, reductions of two-thirds or more in the "shield" circulating current loss having been obtained.

In the majority of tests made, measurements were obtained of the induced voltages in the "shield" circuits in air and in pipe. Measurements also were made, with tong ammeter, of the currents flowing under short circuit conditions in component parts of the "shield" assembly, insofar as these were separable. The indicated ratios varied considerably but on the average an increased loss in pipe over values in air of the order of 2.1 times for the close triangle configuration and 2.5 times for the cradle configuration was indicated. The over-all average ratio for all three configurations measured is 2.3.

Extra a-c losses are inherent in pipe-type construction. It is obvious however, that these losses can be reduced, some of them considerably so, by the use of proper techniques or choice of material. A number of steps in this direction are already underway and further improvement can be expected. The authors' paper illuminates the field for further research.

Losses in the conductors are considerably improved by the use of segmental construction in the larger sizes and it is possible that conductor loss can be still further reduced by insulating individual strands with some type of high resistance film which will suppress proximity effect.

Reductions of losses in shields and skid wire circuits were effected by the use of high resistance materials. As indicated by test data, increasing the circuit resistance by the use of low conductivity alloys and/or reducing the number and pitch of skid wires, can, for all practical purposes, eliminate the greater part of this seat of loss.

The authors and the General Cable Corporation are to be commended for carrying on this extremely valuable research which, when correlated with data now being obtained or planned by other manufacturers and utilities, will establish the necessary understanding for most economical design of the complete pipe cable system.

Reference

1. See reference 1 of the paper.

F. H. Buller (General Electric Company, Schenectady, N. Y.): For several years there has been a real need of data on the a-c/d-c ratio of cables in steel pipe and conduit. A good start was made on this problem in the paper on the Electrical Testing Laboratories tests on cables in steel pipe, presented by R. J. Wiseman before the AIEE in 1948. It was recognized at the time that additional information was badly needed especially on segmental conductors, and on the effect of losses in the shield and skid wire.

Mr. Meyerhoff and Mr. Eager appear to have made a substantial contribution along these lines. While the data they have presented are valuable and are badly needed, a few comments are in order.

Referring to Table III* and Table XI of the paper covering skin effect (Y_0), we have summarized the results given in those tables, in Table I of the discussion together with the calculated skin effect using multipliers of 2.0 and 2.3. It is evident from this table that the results given by the authors in their Table III and Table XI do not agree, and that on the 1,000,000- and 2,000,000-circular-mil sizes these results are appreciably lower than the test results; whereas on the 1,500,000-circular-mil size they are considerably higher than the tests. We are inclined to wonder if the test result on this size is not somewhat low. Furthermore, it is quite evident that the 2.3 multiplier is too high; the true multiplier should be around 2.1. This agrees with Mr. Wiseman's results presented in his paper.

Next with regard to Table IV of the paper, the computed results appear to agree with the test results within about 20 per cent (that is 20 per cent of the extra a-c loss, not 20 per cent of the a-c/d-c ratio) except in two cases. The two cases in question are designation 1*A* short-circuited shield where the computed result is 50 per cent high and designation 2*A* where the computed results are 50 per cent high on the insulated shield and 57 per cent high on the short-circuited shield.

It seems likely that the discrepancy in designation 1*A* and designation 2*A*, insulated shield, may be due largely to the difficulty in measuring small increments of loss, and possibly to the rounding off of the various figures involved. This is not true of designation 2*A* short-circuited shield where the extra a-c loss is 7 per cent measured and 11 per cent computed. A series of tests now in progress on a conductor of this size appears to give 11 per cent for the extra a-c loss which agrees well with the calculation given in the paper. It would seem likely therefore that the test result of 7 per cent is low in this instance.

Table II. Values of Y_{sp} and Y_{sp}' from Equations 12, 13, and Test

Conductor Size, Circular Mils	R_{c30}	R_s'	Cable in Air—Y_{sp}' Calc.	Test	Cable in Pipe—Y_{sp} Calc.	Test
1,000,000	10.75	240	0.033	0.020	0.081	0.030
1,500,000	7.31	242	0.047	0.080	0.118	0.080
2,000,000	5.32	217	0.073	0.030	0.182	0.150

Incidentally, while 20 per cent is reasonably good agreement for cables in magnetic pipe between calculation and test, it does seem like a rather large discrepancy for cables in air. Possibly it might be well to refine the method of calculation to bring this discrepancy down, if this can be done.

An estimate of proximity loss in the shield Y_{sp} and Y_{sp}' in pipes and in air can be obtained from Table IV and Table V of the paper by comparing a-c/d-c ratio as measured on high-resistance shields where it is negligible and low-resistance shield where it is appreciable. Table II of the discussion compares the results obtained in this way with the calculations and equations 12 and 13 of the paper. It is evident from this table not only that the test results are somewhat erratic (probably a matter of taking the difference between small quantities), but that the equations do not agree with the test results at all. In our opinion much better agreement is needed before equations 12 and 13 can be used with any confidence.

The over-all discrepancy in a-c/d-c ratio in Table V of the paper ranges from −4 per cent to +18 per cent. This is probably about as good as could be expected in a magnetic pipe. The agreement in Table VI is even better, from −9 per cent to +9 per cent. This indicates that an empirical correction factor of from 10 to 20 per cent should be added to the losses calculated by the authors' equations if they are to be applied for practical use with a reasonable factor of safety. Incidentally, tests now in progress give a figure of 1.39 rather than 1.33, as in Table V of the paper, designation 2*A*, shield short-circuited. It would seem therefore that Mr. Meyerhoff and Mr. Eager are running somewhat lower on their test results both in pipe and in air than other investigators. This is another reason which would justify the empirical correction factor just suggested.

In the paper there appears equation 6 for the quantity Y_{cp}' (conductor proximity effect) with segmental conductors. This equation and the subsidiary equation for X_1 with its coefficient $K=3.8$ for three insulated segments and 6.0 for two insulated segments, make it appear that proximity effect is very much less with three insulated segments than with two. The only possible cause of this difference is the short circuiting effect of the binder tape on the two uninsulated segments, and since this short circuiting effect is necessarily not very large it is very surprising that there should be such a difference in proximity effect between the two cases. We believe that the authors should support these *K* constants with extensive test data before they may be safely considered authoritative.

Appendix II involves a nice piece of mathematical work resulting in formulas for pipe loss in nonmagnetic pipes. As might be expected when it is attempted to apply these formulas to magnetic pipe, certain approximations as to depth of penetration are necessary, and rather large empirical multipliers are involved. Here again extensive test data are desirable to support these multipliers and this approximation before the results can be considered authoritative. We believe it would be of value to the industry if the authors saw fit to present these test data, and also the test data on which the *K* factor is based either before the AIEE or somewhere else in the technical press.

Finally, it is clear from the data presented in this paper that a nonintercalated shield is a bad business, and that much lower losses are obtained by intercalating the shielding tape.

Table I. Skin Effect Y_0

Conductor Size, Circular Mils	Calculated Skin Effect 2.0 Multiplier	2.3 Multiplier	Reported Skin Effect Calculated Table III	Table XI	Test
1,000,000	0.023	0.018	0.014	0.017	0.020
1,500,000	0.048	0.039	0.039	0.038	0.032
2,000,000	0.091	0.069	0.072	0.065	0.072

J. H. Neher (Philadelphia Electric Company, Philadelphia, Pa.): It is in large measure due to the efforts of Mr. Meyerhoff and Mr. Eager as embodied in their excellent paper, that we are rapidly approaching the point where we can stop guessing and commence to calculate the loss components in a pipe-type cable system with reasonable assurance of coming somewhere near the right answers. And this process will be materially simplified if we look carefully at the fundamentals involved and in so doing discard a few cumbersome practices which have been dragged along from the past.

I refer specifically to the continued use of the a-c/d-c ratio concept. Fundamentally the losses consist of d-c losses and extra a-c losses. The former depend upon the temperature of the conductor, but the extra a-c losses originating outside of the conductor in the shielding tapes and pipe depend upon their respective temperatures which are only indirectly related to the conductor temperature. Furthermore the extra a-c losses originating in the conductors have such a slight dependence on conductor temperature that they may be considered as being independent of this temperature. This was noted by E. H. Salter in the Electrical Testing Laboratories tests[1] and is confirmed by the Arnold Theory of Skin and Proximity Effects.[2]

From a practical standpoint, therefore, a given problem is more conveniently solved by calculating the extra a-c losses at representative temperatures and expressing them in terms of an equivalent extra conductor resistance, since it is a value of resistance which we ultimately use, and then adding this resistance to the d-c resistance of the conductor at its operating temperature. The a-c/d-c ratio which changes with conductor temperature need not enter the calculation, but the net result is the same as that obtained by use of the formulas given in the paper.

---

* Editor's Note: The figures in Table III referred to by Mr. Buller appeared in the preprint of the author's paper, but were revised as noted in the author's closure before publication.

The authors choose to calculate the skin and proximity effects in air separately, although they are combined when calculating the net effect within the pipe. Actually they may be calculated together just as simply by using the particular version of the Arnold Theory cited in Mr. Wiseman's paper.[2] The relationship between the additional conductor loss in pipe and the loss in triangular configuration in air is given as an empirical equation, 7, in the paper but its development is not indicated. I have examined this relationship for all of the tests given in the paper and also the tests of the Electrical Testing Laboratories[1,2] and find that within the limits of expected accuracy this ratio is 1.5 for triangular configuration in conduit, 2.0 for triangular configuration in steel pipe, 1.7 for cradled configuration in conduit, and 2.3 for cradled configuration in steel pipe. In all cases these ratios are based on a triangular configuration in air of the given adjacent conductor separation.

In the case of segmental conductors, and also theoretically in the case of stranded conductors, the Arnold Theory employs a modification factor in formulating the argument used in the Bessel Functions (equation 6 of the paper) to determine the skin and proximity effects. This factor may be introduced in several different ways, and the authors' K is not the same as Arnold's K. It seems more desirable to effect this modification by introducing a factor F as a multiplier of the d-c resistance. Thus

$$x_1 = \frac{6.80}{\sqrt{FR_{dc}}} \tag{1}$$

For a solid conductor $F=1$ and this also applies for the concentric stranded conductor in the Electrical Testing Laboratories' tests.[2] For the conductors covered in the paper, F is equal to 3.2, which is in effect the factor of merit of the conductor construction, since the higher the value of F, the lower the additional conductor losses for a given size of conductor.

The authors are to be commended on their mathematical persistence in solving for Q in equation 16 which represents the pipe loss component, but Q also can be obtained somewhat more simply by graphical integration. It would seem, however, that equation 16 may be improved by using R_c in preference to (CI). Thus

$$Y_t\left(\begin{matrix}\text{magnetic at}\\ \text{60 cycles}\end{matrix}\right) = 0.21\frac{pQ}{R_c} \tag{2}$$

The numerical constant in this equation applies to steel pipe. Based on the Electrical Testing Laboratories' tests,[1] the corresponding constant is 0.18 for conduit.

REFERENCES

1. See reference 2 of the paper.
2. See reference 1 of the paper.

A. H. Kidder (Philadelphia Electric Company, Philadelphia, Pa.): The authors are to be commended for presenting so thorough a description of their recent tests on certain segmental cable constructions, together with approximate quantitative illustrations of relationships which become increasingly important in the design of heavy-duty pipe-type cable circuits. Their observations of total losses are thought to have sufficient accuracy to permit an approximate appraisal of the basic problem, which is to identify and control the factors which determine the efficiency of heavy-duty pipe-type cable designs.

The authors present over-all ac-dc ratios which bring us near but not quite to a complete qualitative understanding of the problem.

Designers of pipe-type circuits are confronted with the need to provide paths for transmitting loads of 1,000 amperes or more, through environments having heat dissipating characteristics which determine the maximum permissible watts losses, and in turn the maximum permissible resistance, or required conductance, of each such path. The cable designer then must select the particular cable construction and the conductor size which will provide the required conductance. The ratios which the authors have presented permit ready analysis from which it will become apparent that the conductance of a pipe-type circuit, however, may have little or no relation to the conductor size.

For example, consider Figure 1 of the discussion which has been constructed to illustrate the effects of steel pipe and cable configuration on 69-kv conductor size, using the information given in Tables X, XV, and XVI of the paper. If a resistance of 0.0125 ohm per 1,000 conductor feet will satisfy the given conditions, the corresponding conductance would be 80 mhos per 1,000 conductor feet. From the illustration it will be seen that the minimum conductor size would be 1,060,000 circular mils for use in nonmetallic pipe. For installations in steel pipe, the conductor size would be 1,300,000 circular mils if the three cables were to be triplexed, or 1,560,000 circular mils if the cables were to be cradled as in present conventional practice. Conversely, it is apparent that adoption of a triplexed construction would save 260,000 circular mils of copper cross-section in this particular case and that the use of a nonmetallic pipe would save an additional 240,000 circular mils. The cost of cables is very sensitive to conductor size. The opportunities for copper economy are substantially greater under conditions which require conductances in the order of 90 to 100 mhos, and could be converted into substantial cash savings by proper design, if there were some degree of consistency between identical tests on cables of apparently identical construction. A practical substitute for magnetic steel pipe apparently would solve the whole problem, or at least reduce considerably the need for improved cable designs. For the present, however, steel pipe must be included in the analysis.

The large uncertain effects of apparently trivial variations in cable design and inconsistencies between successive tests on cables of identical manufacture, suggest that a good bit of thought can be given profitably to consideration of methods for increasing the conductor efficiency in steel pipe installations. In Figure 2 of the discussion, for instance, I have plotted conductor size against 75-degree-centigrade conductance in triangular formation for several 69-kv pipe-type cable sizes. The heavy curve represents the construction for which the authors have estimated R_{ac}/R_{dc} in Table XV. Also shown and identified for

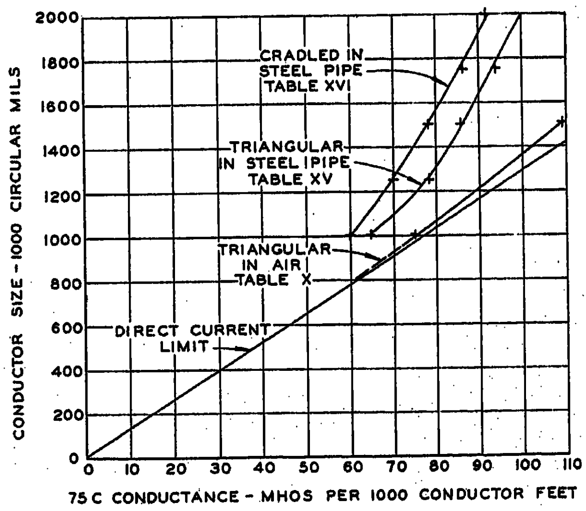


Figure 1. (left) Effects of steel pipe and cable configuration on 69-kv conductor size

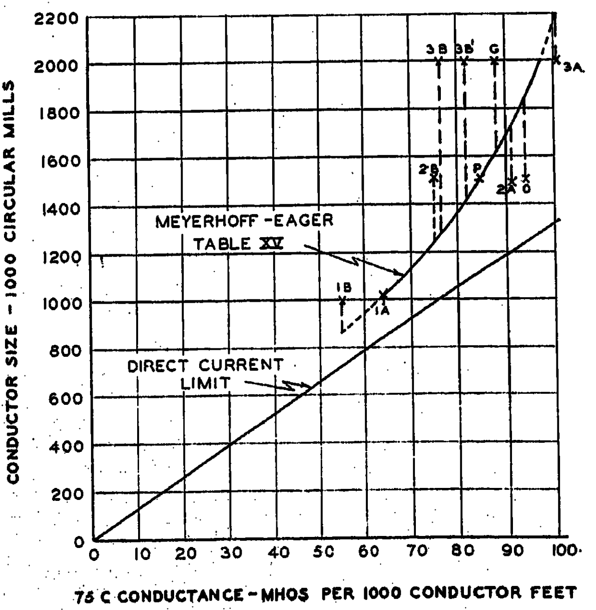


Figure 2. (right) Relation between 75 degree centigrade conductance and 69-kv pipe-type cable size for triangular configuration in steel pipe

comparison are tests reported by the authors in Table X for samples 1*A*, 2*A*, 3*A*, 1*B*, 2*B*, and 3*B*, together with corresponding data from four manufacturers' tests, 3*B'*, *G*, *O*, and *P*, made in conformity with certain purchase specifications with which I am familiar. Several significant observations become apparent from a study of these data:

1. The four, 2,000,000-circular-mil constructions have conductances the same as analysis of Table XV would indicate to be expected of conductors whose sizes range from 1,250,000 to 2,200,000 circular mils.
2. Similarly, the four 1,500,000-circular-mil constructions have conductances respectively the same as would be expected from Table XV for conductors whose sizes range from 1,200,000 to 1,850,000 circular mils.
3. The extra copper required to offset the higher losses in the authors' "low resistance shield" type of construction appears far out of proportion to any possible advantages that might be gained by its possible use as a path for carrying currents.
4. The fact that constructions 3*B* and 3*B'* may be considered to be identical and were tested in the same laboratory, suggests that slight variations in manufacture and perhaps even in handling the cables may have substantial effect upon their efficiency as conductors. The extra polyethylene-intercalated 3-mil copper tape of sample 3*B* hardly would account for its 5 per cent lower conductance.

The 1,500,000 circular mil sample *O* was probably the first on which a full-scale a-c loss test was made. The results were far better than had been expected, giving a conductance in the order of 94 mhos per 1,000 conductor feet. Test accuracy may have been less dependable than in later tests. It is therefore possible that this considerably older cable design had no higher conductance than the authors' sample 2*A* at 91 mhos.

The wide dispersion of test results suggests, however, that the respective constructions probably include some important indeterminate which should be brought under control before additional tests or increased test accuracy can have much value.

The inefficiency of the authors' novel "low resistance shield" construction can be avoided very conveniently by returning to the conventional use of intercalated insulation or the equivalent. The losses in the skid wires are identifiable and may be eliminated perhaps by eventual return to the canvas-type of abrasion protective covering, such as was used in the experimental Oilostatic cable installation at Plymouth Meeting in 1933.

By the process of elimination, it appears that the most probable cause of confusing test results is the uncertain mutual inductance effects of nearby segments and cables upon the current distribution between the individual strands which make up each of the conductor segments. These effects are amplified uncertainly by the magnetic properties of the steel pipe, also by apparently variable and uncontrolled contact resistance between adjacent strands. I have suggested[1] that the controlled application of an insulating or high-resistance film on each of the copper strands should permit making use of the inherent transposition of strands within each segment, in such a way as to equalize such mutual inductance effects and reduce proximity as a cause of conductor inefficiency. The authors' suggested possible use of enameled wires should not be nearly so effective with untransposed strands in concentric conductors, as with the transposition inherent in segmental construction, and I am not convinced that enamel is the answer. The authors' reference to the high contact resistance for aluminum suggests that aluminum strands might be more efficient than copper for large cables, despite the lower specific conductivity. A practical alternative may be the application of a very thin (possibly oxidized) aluminum film on the copper strands before the segments are stranded and compacted. Still another possibility is use of the conventional oxidized lead-coated copper strand, although it appears likely that the compacting pressures might make it less effective than aluminum. The use of a copper oxide or copper sulphide film might have similar advantages, but such films are thought to be more active catalysts than lead, aluminum, or their oxides. The method of joining conductors would, of course, have to be correlated with such a modification of the conductor strand.

REFERENCE

1. 69-KV PIPE-TYPE CABLE CIRCUITS IN PHILADELPHIA, A. H. Kidder, G. S. Van Antwerp. *AIEE Transactions*, volume 68, part I, 1949, pages 550–59.

W. A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): This paper is of great value to the industry both for the wealth of experimental data and for the mathematical analysis which has required not only a knowledge of the physics involved, but even more, a well-balanced judgment in evaluating the many necessary assumptions and approximations.

That the authors have used these qualifications and have made the best use of Arnold's work is clear to me because I have been able to check values of a-c/d-c resistance ratio calculated by their method against laboratory test data and, using reasonable assumptions, find a very close agreement.

The practical use of the method would be helped if Figure 5 of the paper were supplemented either by a table from which large scale curves could be made, or by reference to Arnold's Table I[1] from which Figure 5 appears to have been plotted.

In our segmental compression cables we, like the authors, have avoided short circuiting segments by having only one bare segment and were gratified to find the low resistance obtained. The 3-segment construction has the further advantage of mechanical stability as the segments cannot slide upon one another as in a 4-segment cable, when subjected to severe longitudinally compressive stresses.

REFERENCE

1. See reference 4 of the paper.

Robert J. Wiseman (The Okonite Company, Passaic, N. J.): This paper is a very fine contribution to the data which is being collected on the electrical properties of large size conductors in steel pipes. Two major factors are reported in this paper, segmental conductors and shielding of the insulation. In my paper which the authors refer to we, that is, the Insulated Power Cable Engineer's Association reported on concentric conductors and only one segmental conductor and also on nonshielded cables. As the authors state our results are more applicable to low-voltage rubber or varnished cambric cables and not high-voltage cables, yet they can be used for heavy-wall nonshielded cables in pipes.

I like very much the detailed information which the authors report and the development of the theoretical aspects of the various factors. It is good that they have given curves and tables for some of the terms used in the formulas. Otherwise some of us would not want to go through the calculations each time we had to determine a conductor size to carry a given current. It is desirable, if possible, to use as simple formulas as possible. Therefore, as a matter of interest I took the pipe loss as given in Table XV of the paper and computed it by the formula given in my paper, namely, $PL = 0.00150\, S^2/p$ ohm per 1,000 feet, where S is the spacing, center to center, for triangular spacing and p is inside diameter of pipe. Table III of the discussion compares the values given by the authors, column 3, with the above calculations, column 4. Likewise for the cradle configuration, I took the geometric mean spacing, that is $S' = \sqrt{S_1 S_2 S_3}$ and $PL = 0.00180\; S'^2/p$ ohm per 1,000 feet obtained the values shown in column 6 as compared to column 5 for the authors. The agreement is good considering that we are making assumptions which involve approximation.

I hope that time will be available later to see what can be done with proximity effect.

There is considerable information given in the paper which affords us a means for studying what is the order of magnitude of each of the factors which contribute to the extra a-c losses and how can they be reduced. For example, the authors use a 4-segment conductor and insulate three of the four segments. It is customary to insulate the two opposite segments. If we assume, as the authors do, that the skin effect of a 4-segment conductor is the same as that for a round conductor having 44 per cent of the area of the segmental conductor, then nothing appears to be gained by insulating three segments over two segments, as the Insulated Power Cable Engineer's Association tests made on a 2,500,000-circular-mil conductor gave the same result and two of these segments were insulated. Perhaps this could be investigated further.

The results on shielding losses, both high and low resistance, show the superiority of high resistance in giving lower losses. Why not go further and use only one shielding tape and one *D* wire, as my company did on our first Oilostatic cables with a *D* wire? Why not go even further and let us look into the possibility of dropping the *D* wire and use only one shielding tape? Can it be done? We must study what it means to us from such angles as damage to the shielding tape when installing long cable lengths, good grounding of the shielding continuously along the pipe instead of at joints, and separation of the conductors to allow the oil to flow freely between the cables to aid in conducting the heat away from the cables to the pipe. If the latter is affected, then it means an increase in conductor size. For the pipe, where the highest extra loss exists, we must study the factors which influence the loss,

Table III. Pipe Losses

Conductor Size, Circular Mils	Insulation Thickness, Inches	Table XV Meyerhoff and Eager	Table XV Computed A*	Table XVI Meyerhoff and Eager	Table XVI Computed B**
1,000,000	0.315	0.09	0.09	0.13	0.15
	0.480	0.09	0.10	0.15	0.17
	0.560	0.11	0.12	0.15	0.18
	0.560	0.09	0.09	0.18	0.16
1,250,000	0.315	0.09	0.11	0.19	0.19
	0.480	0.15	0.15	0.19	0.23
	0.480	0.11	0.11	0.23	0.20
	0.560	0.12	0.13	0.23	0.21
1,500,000	0.315	0.14	0.14	0.23	0.24
	0.480	0.16	0.15	0.29	0.26
	0.560	0.17	0.16	0.30	0.29
1,750,000	0.315	0.19	0.19	0.28	0.30
	0.480	0.21	0.19	0.34	0.32
	0.560	0.22	0.21	0.36	0.35
2,000,000	0.315	0.25	0.23	0.32	0.36
	0.315	0.20	0.17	0.38	0.32
	0.480	0.24	0.23	0.42	0.39
	0.560	0.27	0.26	0.43	0.43

* $PL = 0.00150\, S^2/p$ $\quad A = 1 - PL/R_{dc}$

** $PL' = 0.00180\, S'^2/p$ $\quad B = 1 - PL'/R_{dc}$

namely, spacing of the conductors, pipe diameter, and pipe material. Increase in the spacing between the conductors increases the loss for a given pipe size. Increasing the pipe size decreases the loss for both triangular and cradle configuration. As cradle configuration gives a higher loss than triangular formation, we should endeavor to keep the cables in triangular configuration. The nature of the pipe material is the major factor. Perhaps a nonmagnetic pipe of high resistance would be best, but its cost may be prohibitive. A magnetic pipe of low permeability may be helpful, but Dr. Hutchings of the British Electrical and Allied Industries Research Association has made calculations to show that using a steel of permeability of $\mu=300$ and $\mu=1{,}700$ gives about the same loss. Therefore, we would have to go under $\mu=300$.

Table IV of the discussion gives total a-c/d-c ratios for some cables that we have tested.

Table IV

Cable	Ratio R_{ac}/R_{dc}	Temperature, Degrees Centigrade
3 single conductor, 1,500,000 circular mil, segmental $t=0.315$ inch; $6^5/_8$ inch OD pipe	1.38 1.27	27.4 65.8
3 single conductor, 500,000 circular mil, compact $t=0.500$ inch, 6 inch OD pipe	1.19 1.13	32.3 78.0
3 single conductor, 350,000 circular mil compact $t=0.465$ inch; $5^9/_{16}$ inch OD pipe	1.08	68.2
3 single conductor, 500,000 circular mil $t=0.560$ inch; number 10 American wire gauge	1.47	38.2
copper armor over insulation; $6^5/_8$ inch OD pipe	1.41 1.45	107.0 46.0

The values for the 350,000-circular-mil circuit and the 500,000-circular-mil circuit (nonarmor) are the same as the Insulated Power Cable Engineer's Association[1] have set up for 3-conductor cables.

The results reported in this paper and the investigation now being made by the Electrical Testing Laboratories for the Association of Edison Illuminating Companies will supply us with plenty of data to analyze carefully and draw conclusions from, with the hope that we will arrive eventually at a construction that will give us an optimum value of extra a-c losses at a reasonable cost.

REFERENCE

1. CURRENT CARRYING CAPACITY OF IMPREGNATED PAPER, RUBBER AND VARNISHED CAMBRIC INSULATED CABLE FOR THE TRANSMISSION AND DISTRIBUTION OF ELECTRICAL ENERGY. *Publication P29-226*, Insulated Power Cable Engineers Association (New York, N. Y.), December 1943, Table A, page 9.

L. Meyerhoff and G. S. Eager, Jr.: The reception of the paper, as evidenced by the various discussions submitted, is highly gratifying. In general, there is agreement that the methods of computation given in the paper give a reasonably close approximation to losses measured by other observers. One of the discussors points out discrepancies between observed and calculated values and suggests that modifications of certain equations are in order for greater reliability. Another intimates that since there is so wide a range of losses with apparently identical constructions, too close correlation should not be expected.

All of the foregoing is in line with the statement in the early part of the paper that the data presented are not claimed or believed by us to constitute a complete answer to the problem. We concede that there are discrepancies among the data and between the data and calculations and that some of the equations may be subject to modification in the light of later information. We believe, however, that we have presented a logical mathematical approach which should be very helpful in planning future work and in analyzing other data.

A concrete accomplishment of the paper is already evident in the apparently complete acceptance that shield circulating current losses vary substantially according to equations 9 and 10 of the paper. Various lines of approach which were mentioned and analyzed in the paper have received attention, such as using shielding materials of higher resistance and using one *D*-wire instead of two, and the shield losses, within experimental error, have been found to vary inversely as the shield resistance, as the equations indicate. Mr. Hatcher's data indicate, however, that the ratio between shield loss in pipe and that in air may be greater than indicated by the paper.

Mr. Hatcher, Mr. Kidder, and Mr. Wiseman discuss further the matter of reduction of conductor losses by use of various devices which would increase contact resistance between the individual wires making up the strand. Enamel, copper oxide, copper sulphide, or aluminum coatings on the wires, if the coatings are not broken up too much in compacting, may all be very effective in reducing the proximity effect and probably also, to a lesser extent, the skin effect of segmental conductors. The scheme mentioned in the paper, where the outer layer of wires in each segment is separated from the other layers by an insulating tape, also might be effective to a somewhat smaller extent but would probably be less expensive and easier to splice. The use of enamelled concentric strand was suggested in the paper as a possible way of obtaining the same conductor losses as with ordinary segmental conductors, but with a possible cost saving. We agree with Mr. Kidder that concentric strand made with enamelled wires would have higher losses than segmental strand made with enamelled wires, provided, of course, that in the latter the integrity of the enamel coating is not seriously impaired by compacting.

Mr. Kidder's suggestion that aluminum conductors might be used in place of copper is interesting. Aside from possible reduction in conductor losses, there may, in fact, be conditions where the use of aluminum conductors will result in a lower cost installation, as, for example, where the diameter of the pipe used with copper conductors is sufficiently large to accommodate aluminum conductors having a conductance about equal to that of the copper conductors.

The matter of reduction of pipe losses is touched on by Mr. Kidder and Mr. Wiseman. Of course, a suitable nonmetallic pipe, if such were available, would be the most satisfactory answer. A metallic, nonmagnetic pipe would be a satisfactory answer only if the resistivity of the pipe material is extremely high. It is evident from equation 14 that, within the assumptions used, the loss in nonmagnetic pipe is proportional to the thickness of the pipe and inversely proportional to its resistivity. For magnetic pipe actual thickness is replaced by the "depth of penetration," which is of the order of 5 to 10 per cent of the thickness. Taking into account the multiplying factor of 2.6 for magnetic pipe, the resistivity of nonmagnetic pipe must be of the order of 4 to 8 times that of steel, that is, 30 to 60 times that of copper, to produce the same loss as steel pipe, assuming the same wall thickness. To obtain lower losses than with steel pipe, the resistivity of the nonmagnetic pipe would have to be still higher. It may be expected, however, that the use of nonmagnetic pipe will reduce the conductor and shield losses somewhat.

Regarding the losses in magnetic pipe, it should be pointed out that the losses are high primarily because the conductance is high and not because the pipe is magnetic. The common impression, reflected in Mr. Wiseman's comment, is that the way to reduce losses in magnetic pipe is to reduce the permeability. On the contrary, the evidence is that reducing the permeability will increase the losses. Thus, the two lines of attack are to increase the resistivity and to increase the permeability. From the data in Table II of the paper it appears that, with the currents used in the tests, the permeability of the 8-inch pipe is about 50 per cent higher than that of the 6-inch pipe, yet the pipe loss factor is about 15 per cent lower for the 8-inch pipe than for the 6-inch pipe, in spite of the fact that the resistivity of the 8-inch pipe is 10 per cent lower. Similarly, the data in Mr. Wiseman's Table XIII indicate lower permeability and higher resistivity for the steel pipe than for the various conduits, but the loss data indicate higher losses for the steel pipe. Deduction from equation 14 leads to the same conclusion, bearing in mind that, for magnetic pipe, t must be replaced by "depth of penetration," which is inversely proportional to the square root of the permeability.

The excellent agreement between pipe losses as calculated by the simple formulas used by Mr. Wiseman and as calculated by equation 15 is interesting and suggests that for purposes of rough approximation, simple approximations of some of the other equations in the paper also may be found useful.

In Table IV of the discussion Mr. Wiseman gives loss data on four additional cables. It is assumed that these were tested with close triangular configuration. It is to be regretted that construction details have not been given, as these would add greatly to the value of the data.

In Figure 2 of the discussion, Mr. Kidder points out the large spread of effective conductance with conductors of the same size. The reduced effective conductance was produced in most cases by low resistance shields but in one case perhaps by low contact resistance in the strand. It should be noted that Mr. Kidder's comparison of different constructions on the basis of conductance alone, as derived from over-all a-c/d-c ratio, is an oversimplification of the actual design problem and one that could readily lead to erroneous conclusions concerning carrying capacity. In addition to the over-all a-c/d-c ratio one must of necessity give proper weight to the point in the thermal circuit at which the various losses are generated. It is obvious, for example, that a given degree of extra loss developed in the conductors will produce more rise in temperature than an equal amount of extra loss originating in the shielding assembly or in the pipe.

Again referring to Mr. Kidder's Figure 2, it will be noted that our samples 2A and 2B fall below the line drawn from the approximate calculated losses as given in Table XV of the paper. The reason for this is that the actual conductor diameters, and hence the conductor separations, were somewhat smaller than for conventional concentric strand, which were used in calculating Tables XV and XVI, resulting in lower actual than calculated losses. The loss values as given in Tables XV and XVI were intended to be conservative.

Table V. The Functions $G(x)$ and $H(x)$

x	$G(x)$	$H(x)$	x	$G(x)$	$H(x)$
1.1	0.022	0.345	2.6	0.318	0.630
1.2	0.031	0.350	2.7	0.341	0.667
1.3	0.041	0.356	2.8	0.363	0.705
1.4	0.054	0.364	2.9	0.384	0.745
1.5	0.069	0.373	3.0	0.405	0.785
1.6	0.086	0.385	3.1	0.425	0.826
1.7	0.106	0.399	3.2	0.444	0.867
1.8	0.126	0.414	3.3	0.463	0.908
1.9	0.149	0.433	3.4	0.481	0.948
2.0	0.172	0.454	3.5	0.499	0.987
2.1	0.197	0.477	3.6	0.516	1.025
2.2	0.221	0.503	3.7	0.533	1.061
2.3	0.246	0.531	3.8	0.550	1.096
2.4	0.271	0.562	3.9	0.567	1.129
2.5	0.295	0.595	4.0	0.584	1.160

Mr. Buller points out various discrepancies between measured and computed values. The measured skin effect values as given in the original Table III of the preprint of this paper are indeed somewhat higher than the corresponding values in Table XI. However, the skin effect values given in Table III are based on measured resistances at temperatures somewhat lower than 30 degrees centigrade, whereas in Table XI they are based on calculated resistances at a specific temperature. Since there is no particular point in showing the values of skin effect to the third decimal, as was originally done, the values in the revised Table III are shown only to the second decimal.

The discrepancies in shield proximity losses pointed out by Mr. Buller must be attributed to sample variability. These losses constitute a minor part of the total losses and are entirely negligible for the more common cable constructions. Moreover, the results obtained from the equations, as given, appear to be conservative for most cases. Until further data show it to be desirable, there does not appear to be much need for revision of these equations or any of the others given in the paper.

Regarding the value of 6.0 for K, which was indicated for one case of strand with two insulated segments, this was obtained on one specific sample which had a low resistance path between the two bare segments, produced by a lapped bronze binder, and in which the contact resistance between individual wires might be expected to be low by reason of the fact that it received no drying treatment at all. As also pointed out in the paper, the data for a segmental cable reported by Mr. Wiseman show a K value of 5.4. However, it was not intended to convey the impression that all segmental strand with two insulated segments have so high a K factor. Indeed, tests performed since the presentation of the paper have shown that a K factor of 3.8 applies not only to strand having three insulated segments, but also to strand having two insulated segments, provided that the cable with two insulated segments is dried with access of air for part of the time and the conductor binder consists of a bronze tape, intercalated with a paper tape, or is of some other construction having sufficiently high resistance.

We agree with Mr. Buller on the desirability of supporting the K_1 factor for loss in magnetic pipe with extensive data. However, we believe the data given in Table VII are sufficient for a good start in that direction. Perhaps other investigators will supply additional data.

Mr. Neher suggests that a better method of approach toward the evaluation of extra a-c losses is on the basis of apparent resistance increment rather than ratio of a-c to d-c resistance. From the operating man's standpoint his suggestion has much merit. However, the use of the ratio lends itself more readily to the accumulation and evaluation of laboratory data. We believe that this is largely a matter of personal preference, as is also the choice between the K factor as used in the paper and the F factor suggested by Mr. Neher, and the use of conductor resistance instead of conductor size in the pipe loss formula.

Regarding Mr. Neher's suggestion of evaluating skin and proximity effect in air together instead of separately, we believe that separate evaluation has certain advantages, especially because in segmental conductors different K factors apply for the two, K being about 4.5 for skin effect and 3.8 for proximity effect.

It is interesting to note that Mr. Neher has found from available data more or less definite multiplying factors between excess a-c conductor loss in pipe and in air.

Empirical equation 7 given in the paper was an attempt to correlate all available data. Perhaps there is not adequate justification to go beyond establishing constants for the various broad conditions as Mr. Neher has done.

In compliance with Mr. Del Mar's suggestion, there are given in Table V of the discussion, $G(x)$ and $H(x)$ values for the range of x values required in the present connection for equations 5 and 6. These were taken from Table I of reference 4 of the paper.

Self-Saturation in Magnetic Amplifiers

W. J. DORNHOEFER
ASSOCIATE AIEE

THE PURPOSE of this paper is to describe self-saturation in magnetic amplifier circuits and to propose a simplified but approximate method of calculating the performance of this type of circuit. To illustrate the simplified method, the calculated performance for a particular case is compared with the measured performance.

Saturable Reactors

The literature is replete with references to the elementary magnetic amplifiers known as saturable reactors.[1] A saturable reactor, as is known, is a device having an a-c impedance which can be adjusted by means of d-c premagnetization. Simple saturable reactors, in general, are of slightly larger size than 2-winding transformers of the same kilovolt-ampere rating. They are commonly made in one of two styles: 3-legged or 4-legged. In the 3-legged reactor, the a-c windings embrace the outer legs of a shell-type transformer structure, while the control, or d-c winding, embraces the inner leg. The a-c windings are so connected in series or in parallel that the fundamental magnetic flux path is through the outer legs and yokes, giving no fundamental component of power-frequency voltage in the control winding. In the 4-legged reactor, a-c windings are wound on each of the two inner legs, while the d-c winding embraces both. In this case also, no fundamental component of power-frequency voltage is induced in the control winding. With respect to static performance, the two constructions yield reactors with similar characteristics. While differences exist between them with respect to leakage flux conditions and even-harmonic pick-up in the control coil, the 3- and 4-legged reactors operate in much the same way.

The reactance of the a-c windings is adjusted by varying the value of d-c premagnetization in the a-c magnetic path, by passing direct current through the control winding. The reactance is an inverse function of the control current, so that it is maximum at zero direct current and falls to relatively low values at high values of premagnetization. Obtaining optimum utilization of the reactor structure involves supplying a premagnetization with the d-c winding at least equal to the peak magnetomotive force developed by the a-c coils plus the amount necessary to saturate the magnetic circuit of the reactor. Generally, the d-c increment is from 25 to 100 ampere-turns per inch of mean magnetic path. As the result of the excess of d-c over a-c ampere-turns, saturable reactors usually are made with more of the window space devoted to control-winding copper than to power-winding copper. For this reason, a reactor of a given rating is generally slightly larger in size than its analogous 2-winding transformer.

However, assuming that the same window space will be used by both the a-c and d-c windings gives a simplified and not inaccurate idea of saturable reactor sizes and amplifications. For example, if the copper losses of a moderate sized transformer constitute approximately 2 per cent of the rating of the transformer, the copper losses of each winding are the equivalent of 1 per cent of the rating. The a-c winding of a reactor resembles the secondary winding of the transformer in that the current and voltage ratings are equivalent if each has the same amount of iron and secondary copper. For equal d-c and a-c window space and ampere-turns, copper losses in the d-c and a-c reactor windings are equal, both being 1 per cent of the reactor rating for this example. Thus the effective power gain realized through decoupling the d-c winding from the a-c winding is of the order of 100. In practice, because of the use of more than 50 per cent of the window by the d-c coil, gains of approximately 250 can be realized in 50-kva saturable reactors, while gains of approximately 50 are obtained in 2-kva reactors. Of course, the gains mentioned are realized partly at the expense of apparatus efficiency; that is, a larger amount of iron and copper must be provided in the reactor than in a transformer of equivalent rating.

Saturable Reactor Limitations

The simple saturable reactors described have several limitations. The control windings require a great many turns of small wire, in which the induced voltages become extremely high if one of the two a-c coils becomes inoperative. Because of the many turns used in the d-c coils, all of which link the entire magnetic circuit, the time delay (or effective time constant) of the d-c coil is generally long. The delay is reduced if the d-c winding is controlled from a high-resistance source or if resistance is inserted in series with the control coil. This method of reducing the time constant of the control circuit is not economical of control power, and the gain in dynamic response is accomplished only at the expense of the power amplification of the saturable-reactor circuit.

Reactors with Feedback

In simple saturable reactors, a considerable portion of the control power is required to equilibrate the peak a-c magnetomotive force. By making use of feedback or compensation, this portion may be reduced. The use of feedback permits the same range of control of the output as is available in simple reactors. The d-c coil supplies only the magneto-

---

Paper 49-140, recommended by the AIEE Electronics and Basic Sciences Committees, and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20-24, 1949. Manuscript submitted December 1, 1948; made available for printing May 16, 1949.

W. J. Dornhoefer is with the Vickers Electric Division of Vickers, Incorporated, St. Louis, Missouri.

motive force required to saturate the magnetic circuit of the reactor, while the feed-back means supplies sufficient magnetomotive force to equilibrate that of the a-c coils. Several investigators have reported on a method of feedback known as self-excitation or external feedback.[2,3,4] External feed-back circuits are shown in Figures 1 and 2. In these circuits, the alternating current flowing in the main windings is rectified and caused to flow in additional windings on the reactors in such a way that in one reactor the d-c magnetomotive force assists the a-c magnetomotive force at a given instant; while in the other, the d-c ampere-turns oppose the a-c ampere-turns. Of course, on the succeeding alternation of current, the assistance and opposition alter in sense, so that, at any instant, either one or the other of the a-c windings exerts no net magnetomotive force on the core, providing the feedback winding turns are made approximately equal to the a-c winding turns. As a result, saturation of the magnetic core material by means of the d-c control winding can be accomplished by supplying only sufficient magnetomotive force to saturate the magnetic circuit.

External feed-back circuits offer considerable improvement in sensitivity over the simple saturable-reactor arrangement. It is possible, by adjusting the turns in the feed-back winding, to compensate exactly, to over-compensate, or to under-compensate for the power-winding magnetomotive force. Overcompensation corresponds to feedback reactors having feedback turn ratios of greater than unity. It has the effect of reducing the control ampere-turns required by the reactor and accentuating nonlinearities in the output versus d-c premagnetization, or transfer, curve. Under-compensation gives opposite effects.

Several important considerations which enter the design and evaluation of such circuits involve the precision of the equilibrium which can be obtained between the magnetomotive forces of the a-c and feed-back windings. In general, it is not only desirable but absolutely necessary to reduce the leakage flux between the a-c power winding and the feed-back winding to a minimum. This is often done by winding both at the same time, that is, two-in-hand. Moreover, it is necessary to be certain that the current conversion ratio of the rectifier does not change. This, of course, means that back-current leakage in the rectifier must be minimized. The considerations involved in equilibrating the feed-back and a-c winding magnetomotive forces are particularly important with reactors using high quality magnetic materials in gapless magnetic circuits.

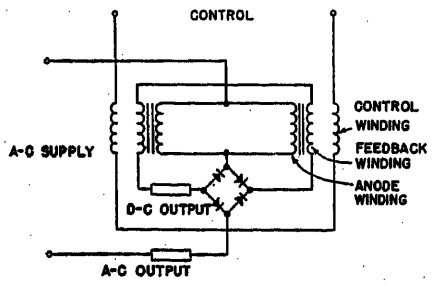


Figure 1. Circuit diagram of a d-c premagnetized reactor having external feedback and parallel connection of the a-c windings

Symbols

N = anode winding turns

ϕ = flux linking anode winding, maxwells

i = anode circuit current, amperes

R = anode circuit resistance, ohms

E_m = maximum value of sinusoidal supply voltage, volts

e_R = forward voltage drop on rectifier, volts

e_L = instantaneous value of load voltage, volts

$e_{L(Avg)}$ or $\bar{e}_L$ = average value of load voltage, volts

E_f = a defined critical voltage for firing $= B_f \omega NA \times 10^{-8}$ volts

A = effective area of magnetic core, square centimeters

l = effective mean length of magnetic path in core, centimeters

f = supply frequency, cycles per second

ω = angular supply frequency, radians per second

θ = angular argument of supply frequency, radians

B = instantaneous magnetic flux density in core, gausses

B_0 = initial magnetic flux density, gausses

B_m = maximum value of alternating magnetic flux density associated with supply voltage $= E_m \times 10^8/\omega NA$, gausses

B_f = critical value of magnetic flux density for firing, gausses

$H(B)$ = instantaneous magnetizing force indicated as a function of B, oersteds

H_i = magnetizing force due to anode current, oersteds

H_0 = initial magnetizing force, oersteds

H_f = critical value of magnetizing force, oersteds

t_f = instant at which firing occurs, seconds

θ_f = angular argument at which firing occurs, radians

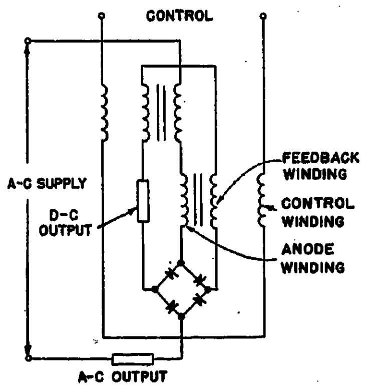


Figure 2. Circuit diagram of a d-c premagnetized reactor having external feedback and series connection of a-c windings

One investigator[5] has reported a 4 to 1 change in the output versus input characteristics of a magnetic amplifier using external feedback when the turns in the feed-back winding were changed from 100 per cent of the power winding turns to 100.1 per cent of power-winding turns.

These results, as might be expected, were found using high quality core material with a gapless structure. Variations of magnetic amplifier characteristics to be expected with reactor core structures built up of conventional *E-I*, *E-E*, or *F*-type laminations, and the adverse effect of slight unbalances in feed-back turns, would not be as great.

Self-Saturation

Another method of balancing the power-winding magnetomotive force involves the use of self-saturation of the type found in the circuit given in schematic form in Figure 3.[6,7]

The schematic circuit drawing of Figure 3, while not that of a practical self-saturating magnetic amplifier, will serve to illustrate the form of feedback which is the principal topic of this discussion. In this circuit an electric valve is connected in series with the power winding of a reactor. Ideally, the current flowing in the power winding is pulsating and unidirectional, and causes pulsating, unidirectional magnetic flux in the core of the reactor, which flux never falls below a minimum value. This value depends on the maximum flux density and magnetizing force achieved during the forward alternation of supply voltage and positive pulsation of anode-winding current. It depends also on the magnetomotive force of the control winding.

In order that the effect upon the output of premagnetization supplied by the control winding may be appreciated, a

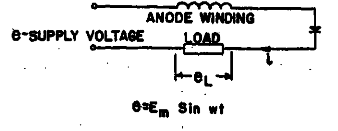


Figure 3. Circuit diagram of the basic half-wave magnetic amplifier with self-saturation

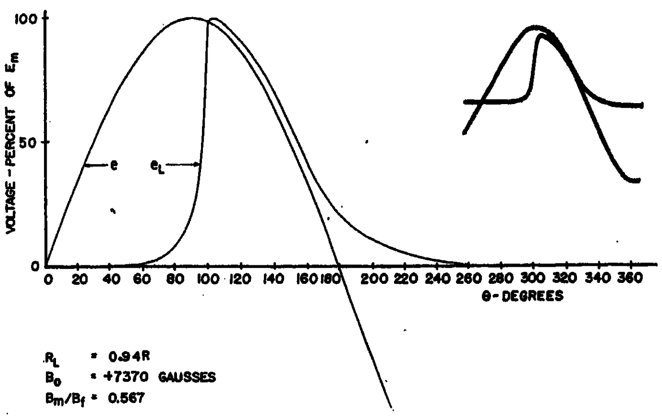


Figure 4. Calculated wave form, see Appendix I, of output voltage of a half-wave magnetic amplifier circuit and oscillogram for corresponding conditions

Calculated	Oscillogram
$B_m/B_f=0.57$	$B_m/B_f=0.57$
$R_L/R=1.0$	$R_L/R=0.94$

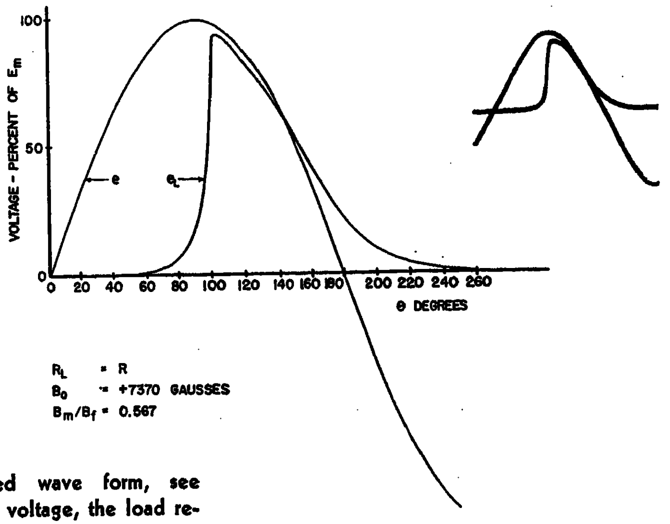


Figure 5. Recalculated wave form, see Appendix I, of output voltage, the load resistance comprising only 94 per cent of the total circuit resistance

Calculated	Oscillogram
$B_m/B_f=0.57$	$B_m/B_f=0.57$
$R_L/R=0.94$	$R_L/R=0.94$

typical reactor with self-saturation is analyzed in Appendix I. Briefly, this appendix treats a half-wave magnetic amplifier, with a sufficiently high impedance connected in control circuit so that the voltage induced in the control winding causes no fundamental-frequency current. (In the case of the practical magnetic amplifier circuits, the induced voltage of fundamental frequency which appears in each control coil is balanced by an equal voltage in another coil; these are so connected that no fundamental voltage appears at the control-circuit terminals.) It is shown in Appendix I that the calculated output-voltage wave resembles the output of a gas-filled grid-controlled electronic tube. See Figure 4. The output-voltage wave form contains a portion with very great slope, which is associated with "firing" of the magnetic amplifier. The time at which firing occurs depends on the magnetomotive force H_0 of the control winding, and in particular, upon the initial magnetic flux density B_0 associated with this magnetomotive force. For purposes of comparison, an oscillogram of the measured output-voltage wave form for the same conditions and values used for the calculation is shown also in Figure 4. A recalculated output-voltage wave form, which is more nearly in accordance with the test conditions of the oscillogram, is shown in Figure 5.

The labor involved in the step-by-step calculations of Appendix I makes it an inconvenient method either for gaining a general understanding of the circuit or for predicting performance.

Effect of Control Premagnetization

The effect of control premagnetization upon the output can be seen in the oscillograms of Figures 6 and 7. As the values of H_0 and B_0 are adjusted, the point of firing varies along the angular time base from 180 degrees towards 0 degrees. Before firing, virtually all the supply voltage is absorbed as a voltage-time integral (flux-linkage change) by the coil. After firing the core is saturated, and almost all of the supply voltage appears on the load resistor. This is particularly evident in the oscillograms of Figure 6. Corresponding oscillograms showing magnetic conditions in the core are given in Figure 7.

The operation of the circuit can be considered in a simplified way as being of this nature:

> The anode winding is capable of developing a large electromotive force due to magnetic flux changes with small changes and small values of anode current, since the reactor for which the oscillograms are shown has a high-permeability gapless core structure. Consequently, from $t=0$, when the supply voltage becomes positive with positive slope, until some later time, all of the supply voltage appears across the anode winding of the reactor, which may be thought of as absorbing a voltage-time integral. The absorption of a voltage-time integral by the reactor must be associated with a change of magnetic flux in the reactor core. When the change of flux from the initial value is such that the magnetic flux density reaches a certain critical value B_f, firing occurs. At firing, the reactor no longer can readily change its flux linkages, and the current increases abruptly from a small value to a value sufficient so that the iR drop is approximately equal to the supply voltage. Figure 4 shows that the iR drop is actually in excess of the supply voltage by the small amount associated with the low rate of discharge of the reactor flux linkages as the magnetic flux is dropping below its maximum value.

The B versus $H(B)$ oscillograms of Figure 7 will serve to illustrate the operation of the self-saturating magnetic amplifier circuit somewhat more clearly. They represent the operating minor hysteresis loops existing in the circuit for which the supply-voltage and load-voltage oscillograms of Figure 6 were taken. The oscillograms are shown in the same order as in Figure 6 from minimum output to a maximum output. The outputs are given in a convenient form developed in Appendix II.

Effect of Hysteresis

The oscillograms of Figure 7 give an insight into the principal factor involved in control of the output. When the minimum flux density, which is the flux density in the core at the time of the axis-crossing with positive slope of the supply voltage, is adjusted to a minimum value ($B_0=-5{,}300$ gausses in Figure 7), the excursion of flux density associated with the supply voltage, the anode turns, and the core area, is not sufficient to cause saturation of the reactor, so that the anode current remains at a small value corresponding to a normal alternating exciting current of the reactor. As the initial flux density B_0 is adjusted upward, firing occurs. As B_0 is increased, firing occurs earlier in time phase and results in larger peak and average values of a-c magnetizing force H_t and output voltage. From these oscillograms, it is evident that the fundamental consideration involved in controlling a self-saturating reactor output is the matter of adjusting the initial magnetic flux density at the time of the axis-crossing with positive slope of the supply voltage. This also is evident from the analysis of Appendix I.

Also shown there is the manner in which the magnetomotive force of the control winding establishes the value of B_0. The relation giving initial magnetic flux density in terms of H_0 is determined as the upper branch of the hysteresis loop.

It is shown that the peak magnetizing forces reached are greatly in excess of the coercive force of the material. As a matter of fact, the peak magnetizing forces in the particular circuit for which the oscillograms of Figure 7 are shown, amount to more than 1,000 times the coercive force of the material. Consequently, operation corresponding to the decay of flux is on the upper branch of the first, second, and third quadrant hysteresis loop, with maximum induction at saturation.

As a result of the saturation caused by the anode current on each half cycle, during the next half cycle when no anode current flows, the magnetic core is in a condition somewhere along the upper branch of the hysteresis loop. Thus, control is obtained along this portion of the magnetization curve, rather than on the normal magnetization curve.

Output, Firing Angle

A method of calculation giving the approximate firing angle and output voltage is developed in Appendix II. It is shown there that, in a typical case, the prefiring skirt of the voltage wave represents about 3 per cent of the voltage-time integral causing flux-linkage changes in the reactor. Consequently, an assumption that the voltage-time integral absorbed by the reactor is given by the integral of the supply voltage from zero to the firing instant is a relatively good one. This assumption simplifies the analysis greatly. While the height of the prefiring skirt may be neglected in determining the instant at which firing occurs, it may not be negligible with respect to the output. Furthermore, it is assumed that all of the circuit resistance is in the load, and that no extraneous effects, such as eddy-current shielding occur. What the assumptions amount to then is this: Before firing all of the supply voltage appears across the coil, while after firing all of the supply voltage appears across the load. This is obviously a rather naive assumption, but it will be seen that the results are in fair agreement with the approximations developed on this basis. Following Appendix II, the firing angle is given by

$$\theta_f=\cos^{-1}\left(\frac{B_m+B_0-B_f}{B_m}\right) \quad 0\leq\theta_f\leq\pi \qquad (11)$$

This relation giving the firing angle is plotted in Figure 8. Several features of the relation are of interest. In the first place, if the working magnetic flux density B_m exceeds the critical magnetic flux density at which firing occurs, then regardless of the value of B_0, firing cannot be prevented. Consequently, one of the elementary principles of self-saturating magnetic amplifiers is operation of the reactor core material at magnetic flux densities below the critical value. It also is obvious in Figure 8 that as the ratio of the working magnetic flux density excursion to critical magnetic flux density is reduced, the changes of initial magnetic flux density required to change the firing angle from 0 to 180° diminish.

In Appendix II the most convenient way of expressing the postfiring output is shown to be as an average value—or more precisely, as the ratio of the average value of the output voltage to a defined constant. The output is expressed as follows

$$\frac{e_{L(avg)}\Big|_{\theta_f}^{\pi}}{E_f/2\pi}=\frac{2B_m}{B}+\frac{B_0}{B_f}-1$$

$$-1\leq\frac{B_0}{B_f}\leq+1 \qquad (14)$$

A chart of this relation is shown in Figure 9, which is the so-called transfer or static characteristic of an ideal half-wave magnetic amplifier circuit. The postfiring output ratio is plotted as the ordinate while the ratio of initial to critical magnetic flux density is shown as abscissa. The transfer curves in these co-ordinate systems are straight lines. The effect of changing the ratio of working magnetic flux density excursion to the critical magnetic flux density value is shown for several values of the ratio.

Experimental Results

In order to indicate the nature and magnitude of the errors introduced by the

Figure 6. Oscillograms of supply voltage and load voltage of the half-wave magnetic amplifier shown in Figure 3. For increased values of B_0, "firing"—the abrupt rise in output voltage—occurs at earlier instants; that is, at smaller firing angles

$B_m/B_f=0.57$ $R_L/R=0.94$

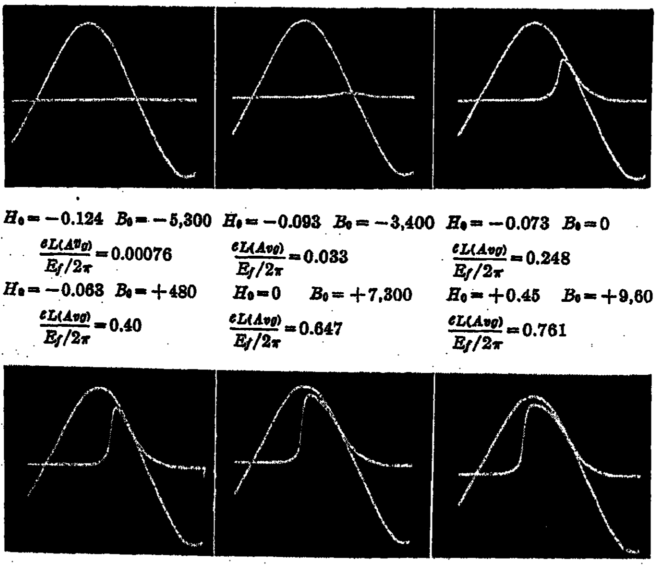
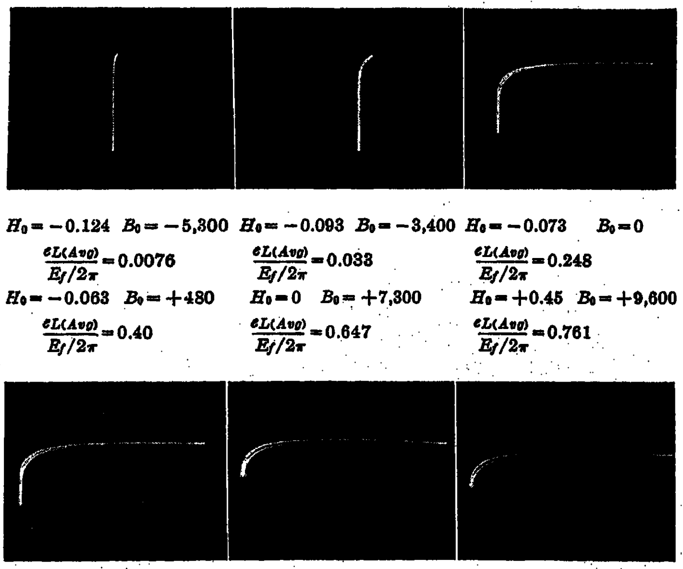


Figure 7. Oscillograms of the minor hysteresis loops associated with operation of the half-wave magnetic amplifier circuit of Figure 3. Circuit conditions correspond with those in Figure 6

$B_m/B_f=0.57$ $R_L/R=0.94$



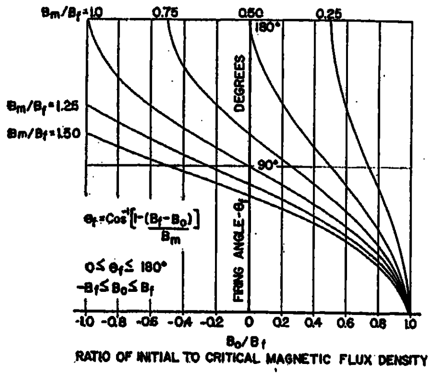


Figure 8. Calculated firing angle of an ideal half-wave magnetic amplifier as a function of the ratio of initial magnetic flux density of a defined critical magnetic flux density. The firing relation is shown for several values of B_m/B_f as parameter

idealizing assumptions of Appendixes I and II, a magnetic amplifier circuit using a core as shown in Figure 10 was constructed and tested. Oscillograms of supply voltage, load voltage, and B versus $H(B)$ have been shown for stated conditions in Figures 6 and 7. Evaluation of oscillograms, several of which are given in Figure 6, serve to determine the firing angle, yielding the experimental points shown in Figure 12. Data and the calculated firing angle curve are for $B_m/B_f=0.57$. Correspondence between the measured values of firing angle and the theoretical values is no better than fair, because the oscillograms of Figure 6 were taken on small 35-millimeter film, making the firing angles rather difficult to measure accurately.

A calculated transfer characteristic, with the initial premagnetization in terms of a magnetizing force rather than a ratio of initial to critical magnetic flux density, is shown in Figure 13. The abscissas are given in oersteds, while the ordinate is shown as the output factor of equation 14. As a result of the shape of the hysteresis loop, the straight lines of Figure 9 are reflected in Figure 13 as shown, with large slope in the vicinity of the coercive force value of the magnetizing force and with small slope in the vicinity of the flat portions of the hysteresis loop of Figure 11.

A set of measured transfer characteristics for a half-wave magnetic amplifier circuit of the sort which has been under discussion is shown in Figure 14. The ordinate has been reduced to the normalized units of equation 14, and the abscissas have been calculated on the basis of the known control magnetomotive force and the arithmetic mean length of magnetic path in the core.

While the general shape of the measured characteristic of Figure 14 corresponds approximately with the calculated shape, Figure 13, a noticeable discrepancy exists between the slopes of the transfer characteristics in the region of the coercive force value of the magnetic intensity. The measured curves show a great deal less sensitivity to changes of control magnetizing force than do the calculated curves. In addition, if the maximum slope portion of the measured curves is projected upon the axis of abscissas, cut-off is seen to occur in the vicinity of -0.12 oersted, rather than the -0.08 oersted value predicted by the calculations.

Several explanations of this departure from expected behavior are possible. Two are considered most likely. They are: eddy-current shielding of the laminated core structure and consequent broadening of the hysteresis loop; and the effect of the distributed parameters of the magnetic circuit of the test core. It is shown in Appendix II that the reason for the discrepancy between the calculated and measured results probably is concerned with the radial depth of the toroidal magnetic core. Careful consideration of the conditions in the core during the time when control is obtained, that is, during the negative alternation of the supply voltage, indicates that for complete cut-off, the initial flux density in the extreme magnetic path must be reduced to the cut-off value. However, the cut-off value of initial flux density for the inner portion of the lamination is reached considerably earlier because of the shorter magnetic path. If the measured transfer curve for $B_m/B_f=0.57$ is compared with the calculated transfer curve based on the mean length of magnetic path, 21.9 centimeters, curves A and C of Figure 15 result. If, now, the calculation is extended to include the effect of the extreme length of magnetic path, curve B of Figure 15 results. It will be seen that curve B is more nearly in agreement with the measured curve in the vicinity of the linear portion of the characteristic than is the curve calculated on the basis of mean length of magnetic path, curve A. While no further work has been done to determine the exact effect of the radial depth of the magnetic core and to confirm what may be inferred from Figure 15, it is felt that the radial depth consideration offers a reasonable explanation of the departure of the calculation, using only the mean length of magnetic path, from the measured performance.

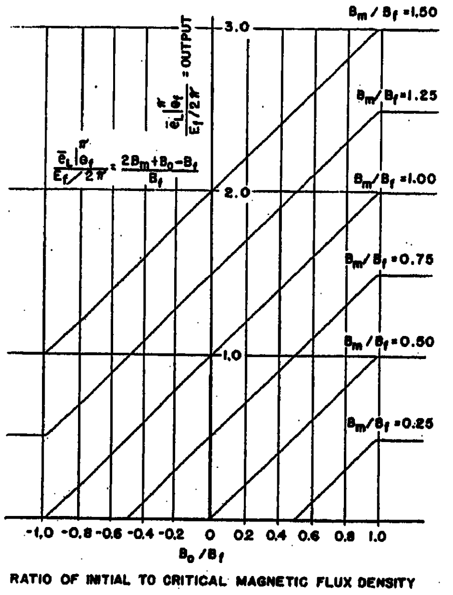


Figure 9. Integrated average output voltage of an ideal half-wave magnetic amplifier plotted as a function of the ratio of initial magnetic flux density to the defined critical magnetic flux density

Initial Induction, B_0

The value of magnetic flux density B_0 in the core at the start of the forward alternation of supply voltage plays a major role in determining the subsequent values of maximum, rms, and average output current and voltage. The changes in value of B_0 which result in changes of

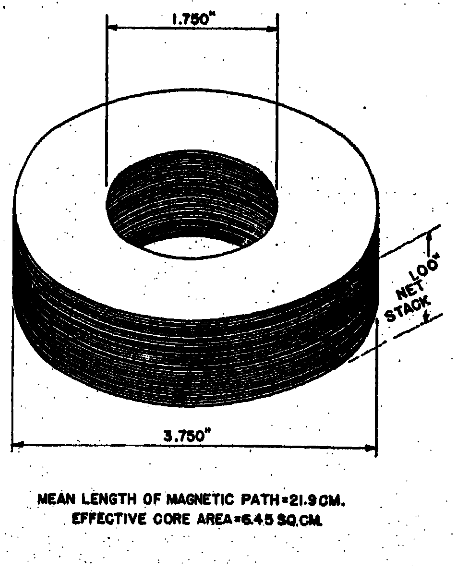

1.750"
1.00" NET STACK
3.750"
MEAN LENGTH OF MAGNETIC PATH=21.9CM.
EFFECTIVE CORE AREA=6.45 SQ.CM.

Figure 10. Reactor core used for purpose of demonstrating the sample calculations. Core is a composite structure using stacked laminations of 0.0066 inch (0.0167 centimeter) thickness

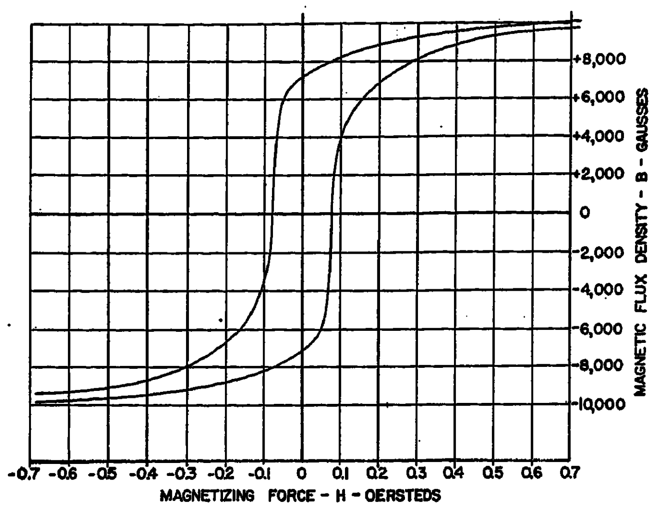


Figure 11 (above). Hysteresis loop of core material employed in reactor for which the oscillograms in Figures 6 and 7 and the measured transfer characteristic of Figure 14 are shown

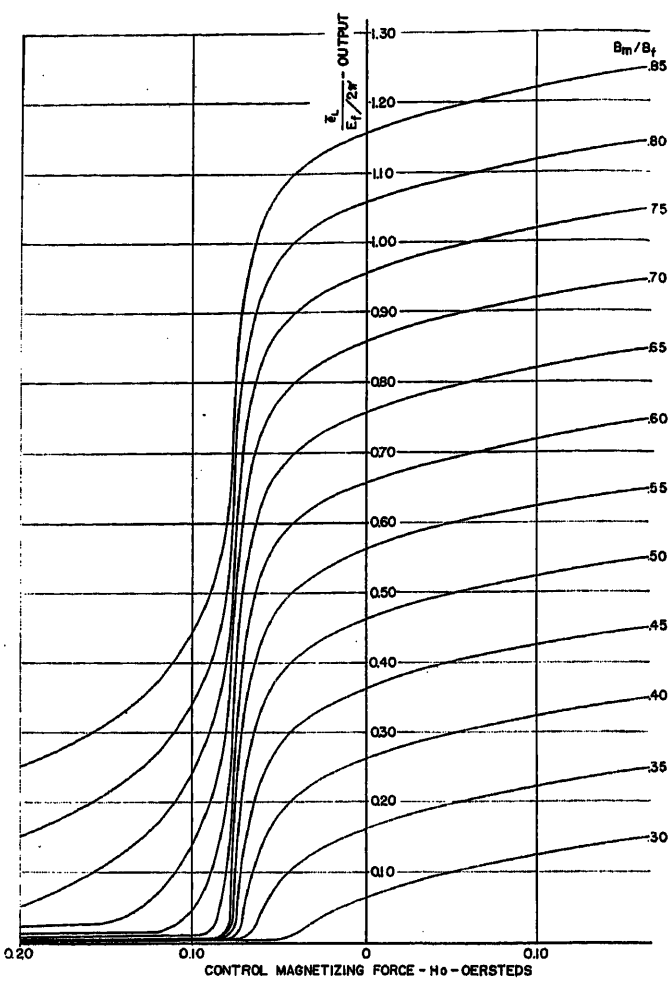


Figure 13 (right). Calculated average load voltage as a function of the control premagnetizing force for reactors with core material as in Figure 11. Characteristics shown with several values of B_m/B_f as parameter

output may occur as a result of the operation of several factors, of which the control current, anode current, operating hysteresis loop, and magnetic circuit configuration are most obvious. It is desired, of course, that changes in output result only from changes of the control current.

The control characteristics of a magnetic amplifier employing self-saturation are dependent to a great degree upon the characteristic of the rectifier which is employed, particularly the reverse characteristic. As an example, in the circuit under discussion here, the peak anode magnetizing force amounted to approximately 112 oersteds. Reverse current leakage permitted by the rectifier of only 1 per cent would cause an anode demagnetizing force during the negative alternation of the supply voltage of 1.12 oersteds, which is almost ten times the value needed in the control winding for full control. With leaky rectifiers, the control winding would have to provide the control shown in Figures 13 and 14, plus sufficient magnetizing force to compensate for leakage of the rectifiers. Consequently, rectifiers for use in magnetic amplifier circuits must have high values of reverse resistance, particularly where high-permeability cores with gapless construction are employed.

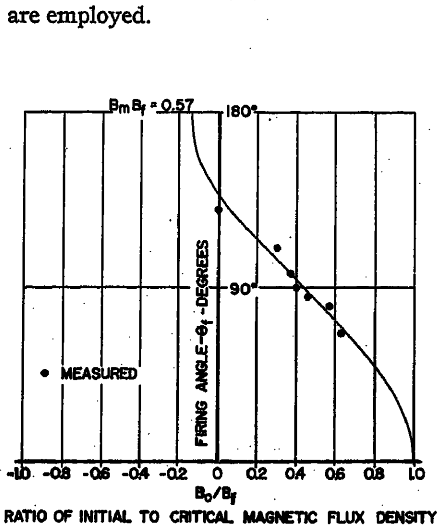


Figure 12. Measured and calculated firing angles of half-wave magnetic amplifier circuit consisting of a reactor with core as shown in Figure 10 and magnetization curve as shown in Figure 11. The measured values of firing angle are from oscillograms, several of which are shown in Figure 6

$B_m/B_f = 0.57$

The rectifiers used most commonly in the magnetic amplifier practice are of the metallic type. Metallic rectifiers, of course, have limited values of inverse resistance, which means simply that the reverse current associated with rectifier leakage causes an increase of control ampere-turns required for a given change of magnetic amplifier output. The effect of leakage currents in magnetic amplifiers is an upward shift of the control characteristics of the amplifier, by an amount which depends on the leakage. If the inverse resistance of the rectifiers were constant, the amount of shift would, of course, be proportional to the supply voltage. This general effect is seen in the family of magnetic amplifier transfer curves shown in Figure 16. These transfer curves were taken with a magnetic core material of even higher permeability than the medium-nickel-iron alloy for which the curves thus far have been shown, and with particularly unsuitable rectifiers, in order to bring out the effect of reverse current. For small applied voltages, leakage in the rectifier causes little shift of the characteristic. As the ratio to working flux density to critical flux density is increased, that is, as the supply voltage is increased, the reverse current increases, causing the upper curves of the family to shift into the positive control region. The amount of the shift in control is proportional to the magnetomotive forces developed by the reverse current in the anode winding. In general, the desired characteristics of metallic rectifiers for magnetic amplifier circuits are both high inverse resistance to minimize the leakage, and low forward drop. In order to minimize the number of series rectifier cells which must be employed in the stack, it also is desirable that the allowable working voltage per cell be as high as possible. Good practice dictates, for instance, that, with commercial selenium rectifier plates normally rated at 18 volts rms per cell, working voltages for magnetic amplifier circuits be of the order of 8 to 12 volts per cell, which means about 10 series plates per leg for

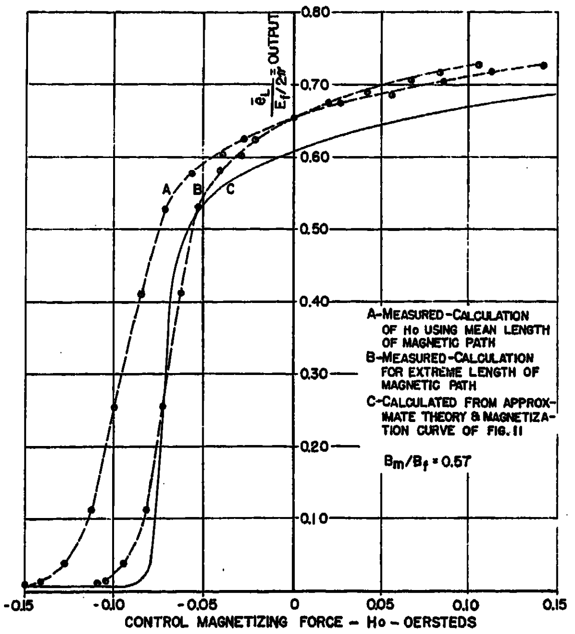


Figure 15. Calculated and measured average output voltage of a half-wave magnetic amplifier circuit as a function of the control magnetizing force, with the radial depth of the core of Figure 10 taken into consideration

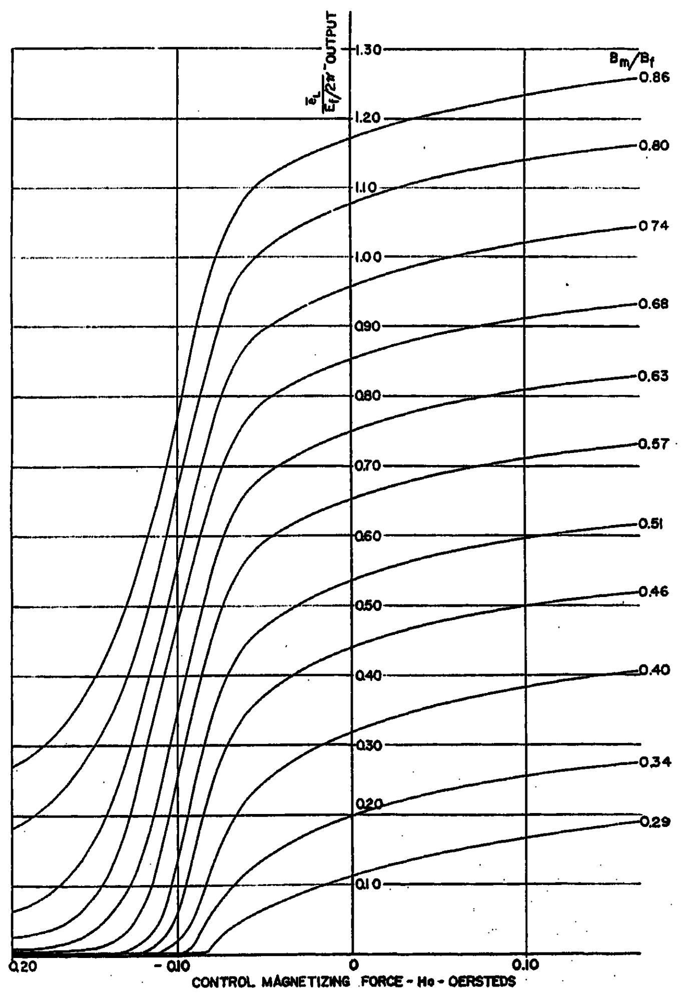


Figure 14. Measured average output voltage of half-wave magnetic amplifier circuit with cores as in Figure 10 and magnetization characteristics as in Figure 11, with several values of the ratio of working magnetic flux density to critical magnetic flux density

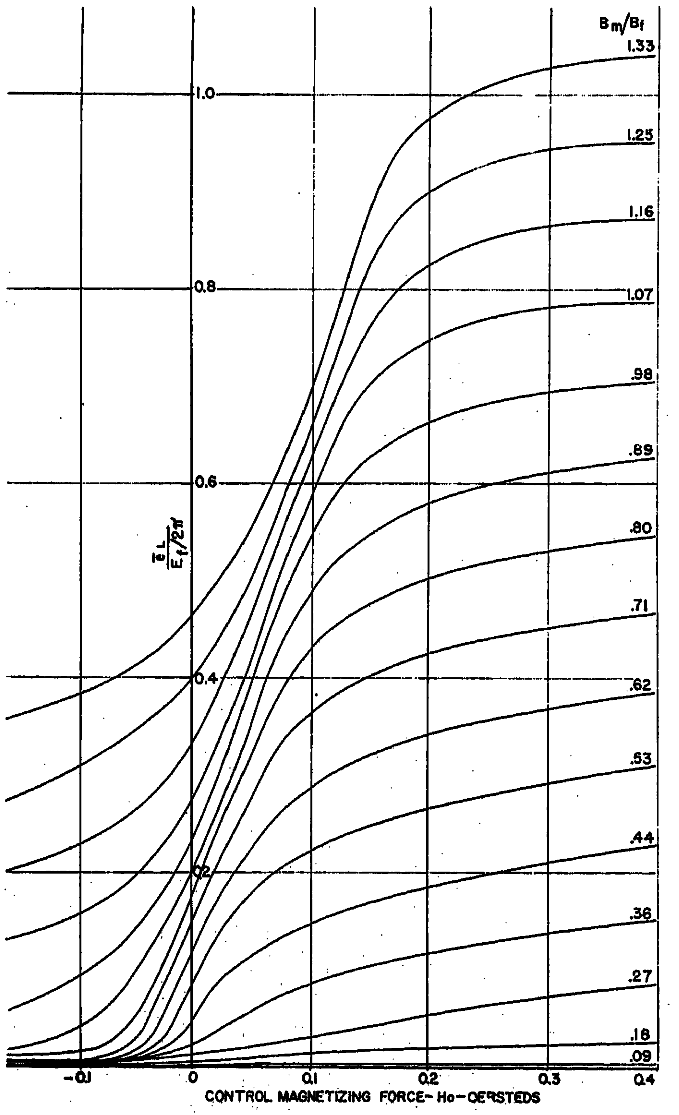


Figure 16 (lower right). Family of transfer characteristics of a magnetic amplifier with 80 per cent nickel-iron reactor cores and metallic rectifiers. The shift of similar portions of the characteristics toward the right is due to reverse current leakage of the rectifiers as the working magnetic flux density (B_m/B_f) and applied voltage increase

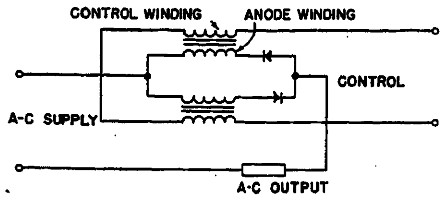


Figure 17. Schematic diagram of a "doubler" self-saturating magnetic amplifier. The output contains an alternating component of fundamental frequency. The second harmonic of the supply voltage is the lowest which is present at the control terminals

operation at 100 volts. Manufacture of selenium rectifiers giving excellent performance in certain magnetic amplifier circuits at ratings of 30 to 40 volts rms per cell is entirely feasible, however.

Control Means

The half-wave self-saturating magnetic amplifier circuit which has been discussed thus far is susceptible to control by any of several means. The basic means is the adjustment of the value of initial flux density during the nonconducting portion of the cycle, that is, during the reverse alternation of supply voltage. Various means may be employed to adjust this value of initial flux density. The most obvious, of course, is premagnetization by means of continuous direct current. However, other means are equally feasible. Control of the initial flux density by means of synchronous alternating current, adjustable either in amplitude or in phase, may be employed. Control by means of amplitude variation of a synchronous alternating current also is feasible in many cases. In addition, control may be achieved by changing the reverse current in the anode winding during the nonconducting portion of the cycle by shunting adjustable impedance elements across the anode winding or the rectifier.

Magnetic Amplifier Circuits

The half-wave magnetic amplifier circuit which has been employed for analysis and study thus far is of little practical value, principally because a component of electromotive force of fundamental frequency appears at the control-circuit terminals. In practical magnetic amplifier circuits this control-circuit component of fundamental electromotive force must be minimized or eliminated. This is done in single-phase circuits by connecting a second half-wave magnetic amplifier circuit to the first, as shown in Figure 17 or 18. The circuit of Figure 17 delivers an output which contains an alternating component of fundamental frequency, while that of Figure 18 delivers an output which has an average value, that is, a direct component. The former is called the "doubler" circuit, while the latter is called the "full-wave" circuit. It will be noted in these figures that the control coils are so connected that the fundamental components of induced voltage in the coils are opposed, making the lowest harmonic of the induced voltage that appears at the control-circuit terminals the second.

It is possible to connect the basic half-wave circuit in polyphase arrangements such as the 3-phase wye circuit shown in Figure 19, the 6-phase star circuit shown in Figure 20, or the 3-phase bridge circuit shown in Figure 21. In general, the polyphase circuits are used where particularly fast response is required and where output ripple requirements are difficult to meet with the single-phase circuits.

The magnetic amplifier circuits of Figure 22 are basically self-saturating magnetic amplifier circuits, in which added external feedback has been employed for the purpose of reducing the control change required for a prescribed output change. The object generally is overcoming the effect of leakage in the rectifiers. The turns associated with the added feed-back winding are few, being of the order of 1 or 2 per cent of the main-winding turns.

External Feedback and Self-Saturation

From the discussions thus far it is apparent that under ideal conditions the static performance of the circuits using external feedback, Figures 1 and 2, and of the self-saturating circuits are virtually identical, since the purpose of the feedback in both cases is to balance the magnetomotive force of the anode windings and thus permit control of the magnetic amplifier output with an amount of signal power equal to that required for saturation of the magnetic core material. A recent analysis has indicated excellent correspondence both for calculated and for measured static performance of the two circuits for the case of built-up laminated cores with air gaps.[12]

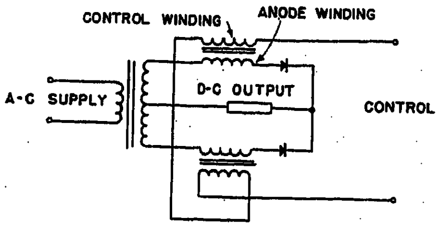


Figure 18. Schematic diagram of a "full-wave" self-saturating magnetic amplifier. The output contains a direct component. The second harmonic of the a-c supply is the lowest which is present at the control terminals

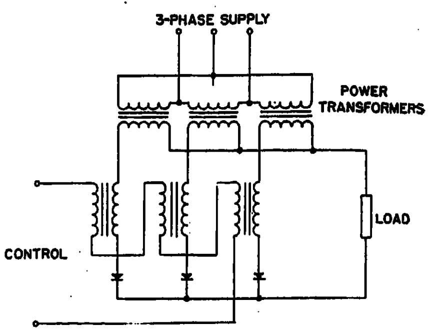


Figure 19. Schematic diagram of a "3-phase half-wave" self-saturating magnetic amplifier. The output contains a direct component. The third harmonic of the supply voltage is the lowest which is present at the control terminals

In both circuits, the characteristics of the rectifiers are quite important, if not critical. In the self-saturating circuit, limited reverse resistance of the rectifiers causes back-current demagnetization of the core during the nonconducting half cycle, with consequent loss of amplification. In the Buchold circuit, Figure 1, a change of current conversion ratio of the rectifiers, resulting from back-current leakage of the nonoperating rectifiers, has much the same effect. In the magnetic amplifier circuit using external feedback, leakage flux between the feed-back winding and the anode winding plays a major role in determining the effectiveness of the feedback. No such consideration exists in the self-saturating circuit.

A further relation between the two circuits is found from consideration of the apparatus utilization. Considering two magnetic amplifiers, one with the Buchold circuit of Figure 2 and one with the self-saturating circuit of Figure 17, in which the magnetic core structures are identical and in which the applied voltages are the same, only 1/2 the a-c winding turns are required for the former as compared with the latter. Since, in the Buchold circuit, the feed-back turns equal the a-c turns the same total turns and wire size are employed. In the self-saturating circuit, the full load current flows in the full reactor turns 1/2 the time. In the Buchold circuit, the full load current flows in all

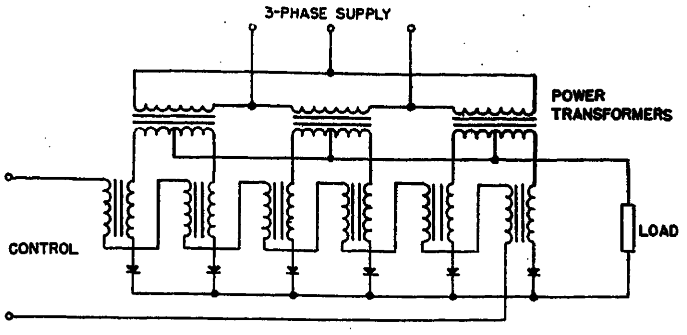


Figure 20 (above). Schematic diagram of a "6-phase diametrical" self-saturating magnetic amplifier. The output contains a direct component. The sixth harmonic of the supply voltage is the lowest which is present at the control terminals

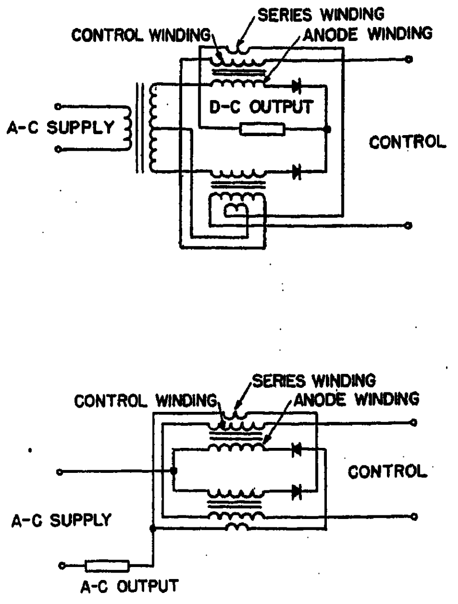


Figure 22 (right). Schematic diagrams of doubler and full-wave self-saturating magnetic amplifiers with added external feedback to compensate for rectifier leakage current

windings during the entire cycle, and the power dissipation in the circuit with external feedback is twice that in the self-saturating circuit. Conversely, for the same temperature rise, the output of the Buchold circuit must be limited to 50 per cent of that allowable in the self-saturating circuit using the same weight of copper and iron. To reach the same output with the Buchold circuit with operation on the same voltage, the copper section must be approximately doubled. Thus, twice the window area will be required for the circuit using external feedback. If a square window and the same stack is assumed, doubling the window means an increase of approximately 41 per cent in the mean length of the magnetic path and weight of core material, as well as the 100 per cent increase of copper weight.

Appendix I

Circuit Relations, Assumptions

The half-wave magnetic amplifier circuit shown in Figure 3 is analyzed here, and an example is calculated for the core shape shown in Figure 10, with core material characteristics as given in Figure 11. The differential equation applying to the circuit is solved by step-by-step numerical integration for a particular set of operating conditions and circuit parameters, in order to find the waveform of the voltage appearing across the load resistor e_L.

The differential equation describing the voltage relations in the circuit of Figure 3 is

$$e = N\frac{d\phi}{dt}\times 10^{-8}+iR+e_R \qquad (1)$$

If the applied voltage is sinusoidal

$$E_m \sin \omega t = N\frac{d\phi}{dt}\times 10^{-8}+iR+e_R$$

$$E_m \sin \omega t - e_R = N\frac{d\phi}{dt}\times 10^{-8}+iR \qquad (2)$$

If $E_m >> e_r$, it is approximately true that

$$E_m \sin \omega t = N\frac{d\phi}{dt}\times 10^{-8}+iR$$

$$d\phi = \frac{(E_m \sin \omega t - iR)}{N}dt\times 10^{8} \qquad (3)$$

For the present, it is assumed that the distributed parameters of the magnetic circuit may be lumped to an effective area A and an arithmetical mean magnetic path length l.

Further, it is assumed that the impedance of the control circuit is of large enough value to suppress harmonic currents in the control circuit. This assumption is permissible and, in fact, required because in practice, with high-permeability cores and circuit connections as in Figures 17 through 21, harmonic currents flowing in the control circuit have limited effect on the static performance. In the circuits mentioned, no fundamental component of supply-frequency voltage is found at the control-circuit terminals. In addition, resistance usually is added in the control circuit to reduce the amplifier time constant. As a result, the only alternating current superimposed on the control current is a higher harmonic of small amplitude. To make the calculated response of the half-wave circuit resemble conditions actually found in the conventional self-saturating amplifier, it will be assumed that the control circuit impedance is sufficient to suppress all but the signal current. As a result, the magnetizing forces of the anode winding and of the control winding are sufficient to give the total magnetic intensity

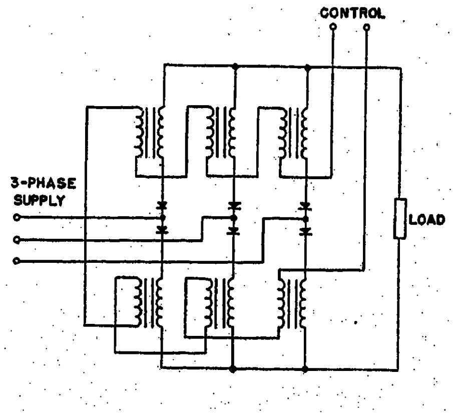


Figure 21. Schematic diagram of a 3-phase bridge self-saturating magnetic amplifier. The output contains a direct component. The sixth harmonic of the supply voltage is the lowest which is present at the control terminals

$$H(B) = H_0 + H_t$$

$$H_t = \frac{0.4\pi Ni}{l}$$

$$d\phi = AdB$$

Then from equation 3

$$dB = \left(\frac{E_m \sin \omega t}{NA} - \frac{H_t lR}{0.4\pi N^2 A}\right)dt\times 10^{8} \qquad (4)$$

Since $H(B)$ enters the relation giving the differential flux change, analytical solution of equation 4 is possible only if $H(B)$ can be expressed as an explicit function of B. While an approach involving use of an analytical expression of $H(B)$ in terms of B is feasible, it becomes burdensome when the relation is radically nonlinear, causing the higher-order terms of the series expansion to have substantial coefficients. In particular, in this case the response of the circuit to a sinusoidal input voltage includes a waveform which is extremely nonsinusoidal, containing at one point a slope which approaches a finite discontinuity. Consequently, the higher-order terms of an analytical expression for $H(B)$ would have to be considered, should an analytical solution be required. It is more convenient for the purpose of this appendix to solve the differential equation, which has numerical coefficients if the circuit parameters and operating conditions are given, by direct numerical integration, employing the magnetization curve of Figure 11 for data. As in any numerical integration process, the increments of angular argument must be small, particularly in regions of sharp curvature of the characteristic curve, if the requirements implicit in the numerical method are to be satisfied.

Initial Conditions

In order to establish the correct initial conditions so that the equilibrium response may be described by the first integration, attention must be paid to the working conditions, as indicated in the oscillograms of Figures 6 and 7. The initial conditions to be determined include the current, the magnetizing forces due to the main winding and control winding, and the initial magnetic flux density, found on the proper portion of the magnetization curve, due to the net initial magnetizing force.

In the first place, because of the presence of the rectifier in the anode circuit, no current flows in the reverse direction, providing that the reverse resistance of the rectifier is unlimited in the working range of applied voltage. Before $t=0$, the current in the anode winding is zero, as is the magnetomotive force exerted by the main winding on the core. Thus the initial value of magnetizing force is only that caused by the control winding.

Secondly, in order to determine the initial magnetic flux density, it is necessary to note in the oscillograms that the forward current during the positive alternation of the supply voltage causes magnetic intensities greatly in excess of the coercivity of the material. This may be verified by observing in the B versus $H(B)$ oscillograms of Figure 7 that the right-hand portion of the magnetization curve is essentially flat, indicating that the core material becomes thoroughly saturated during the forward alternation of the supply voltage. Following saturation, the decaying value of anode-winding magnetizing force approaches zero along the upper branch of the hysteresis loop, indicating that during the succeeding negative alternation, the magnetic condition of the core is defined by that branch of the loop. The value of initial magnetic flux density B_0 then may be found, for a given value of initial premagnetization H_0 exerted by the control winding, by referring to the upper branch of the hysteresis loop.

Sample Wave Form Calculation

Following determination of the initial conditions, numerical integration according to equation 4 is carried out. A particular set of values has been chosen for illustration; a calculated load voltage wave form is shown in Figure 4. The illustrative values are:

$E_m=141.4$ volts
$l=21.9$ centimeters
$R=50.3$ ohms
$N=650$ turns
$\omega=377$ radians per second
$A=6.45$ square centimeters
$H_0=0$ oersteds
$B_0=7{,}300$ gausses
Magnetization data as given in Figure 11

The working equation is

$$dB=\left(E_m \sin\theta-\frac{H_i lR}{0.4\pi N}\right)\frac{d\theta}{\omega NA}\times 10^8 \qquad (5)$$

The calculated wave form of the total iR drop in the anode circuit is shown in Figure 4. Also shown is an oscillogram of e_L taken in a half-wave circuit with values as enumerated, core as in Figure 10, and magnetization curve as in Figure 11. When this is compared with the calculated wave form, a general resemblance is noted, but the calculated curve has ordinates that are greater than the measured values. The reason for the discrepancy is obvious, when it is considered that the anode winding of the reactor has resistance, so that the load voltage oscillogram of Figure 4 does not represent the entire iR drop in the circuit but only a fraction of it. The oscillogram was taken in a circuit in which the load resistance represents approximately 94 per cent of the total circuit resistance. A calculated wave form of load voltage with this factor considered is shown in Figure 5, together with another oscillogram of the measured wave form with the same conditions.

Appendix II

While the method of numerical integration illustrated in Appendix I serves to predict approximately the wave form of the output voltage, from which the effective and average values may be found, it is not convenient to use in predicting the static performance characteristic of the amplifier, principally because of the labor involved. It is the purpose of this appendix to point out how the performance curves may be simply approximated, and to indicate the magnitude of the errors introduced by the simplifying assumptions made in the course of the approximation.

Firing Angle

Assumption 1. Firing of a half-wave self-saturating magnetic amplifier circuit employing a high-permeability gapless core occurs abruptly enough so that the iR drop before firing may be neglected in determining the angular argument at which firing occurs.

In evaluating this assumption, it is revealing to consider the calculated response of the circuit given in Appendix I. In Figure 5, firing occurs at approximately 90 degrees. The voltage-time integral (flux-linkage change) absorbed by the anode winding during the prefiring interval is 0.375 volt-second, less the value of voltage-time integral associated with the voltage drop in the circuit resistance during that interval—approximately 0.011 volt-second. The error involved in assuming that the entire supply voltage-time integral appears across the coil before firing would be approximately 3 per cent for the case shown in Appendix I. While this does not represent an inconsiderable order of magnitude of error, it is considered tolerable here in view of the simplification which results from the assumption.

It should be mentioned, of course, that as the circuit resistance is increased, as the supply voltage is reduced, and as the permeance of the magnetic circuit is reduced, the error involved in Assumption 1 is increased.

Assumption 2. Firing occurs in the mode shown in Appendix I. This limitation rejects one of the two possible modes of firing. The rejected mode obtains when the core is premagnetized beyond negative saturation in a negative direction ($B_0<-B_f$). If this condition exists, then, following $t=0$, the entire supply voltage appears initially as a drop on the circuit resistance. Firing in a reverse sense from that illustrated thur far occurs when the magnetic flux density in the core is increased to $-B_f$. After firing, virtually the entire supply voltage appears at the coil terminals. Because of the large values of H_0 which are required and because of the limited range of control possible, this mode of operation is at present of only secondary interest in magnetic amplifier circuits. This basic mode of operation is employed, however, in d-c metering circuits, generally known as d-c converters or "transformers."[13]

The second and more important mode of firing, for magnetic amplifiers at any rate, is the type illustrated in Appendix I. The restriction $-B_f \leq B_0 \leq +B_f$ is sufficient to include this mode and exclude the d-c "transformer" mode.

On the basis of these assumptions and those of Appendix I, a simple relation for the firing angle may be set down, following equation 3. Since the prefiring skirt is neglected, and $d\phi=A\,dB$, equation 3 becomes

$$dB=\frac{E_m \sin\omega t}{NA}\,dt\times 10^8 \qquad (6)$$

$$B=B_0+\int\frac{E_m \sin\omega t}{NA}\,dt\times 10^8 \qquad (7)$$

Firing occurs when the instantaneous magnetic flux density reaches the critical value B_f

$$B_f=B_0+\int_0^{t_f}\frac{E_m \sin\omega t}{NA}\,dt\times 10^8 \qquad (8)$$

$$B_f-B_0=\frac{E_m\times 10^8}{\omega NA}(1-\cos\omega t_f)$$

$$\theta_f=\cos^{-1}\left(1-\frac{(B_f-B_0)\omega NA}{E_m\times 10^8}\right) \qquad (9)$$

where

$$\theta_f=\omega\times t_f$$

But the maximum value of flux density associated with the supply voltage is

$$B_m=\frac{E_m\times 10^8}{\omega NA}$$

so that

$$\theta_f=\cos^{-1}\left(1-\frac{B_f-B_0}{B_m}\right) \qquad (10)$$

The values of the angular argument at which firing occurs are given by equation 10. Restrictions of the value of θ_f exist and should be recognized. Because of the rectifier

$$\theta_f\leq\pi$$

Because of the limitation of Assumption 2 that is, $-B_f\leq B_0\leq +B_f$

$$\theta_f\geq 0$$

Thus a complete statement giving the firing angle in convenient form is

$$\theta_f=\cos^{-1}\left(\frac{B_m+B_0-B_f}{B_m}\right);\quad 0\leq\theta_f\leq\pi \qquad (11)$$

It is most convenient to plot equation 11 by showing θ_f as a function of B_0/B_f with B_m/B_f as parameter. This is done in Figure 8. As an experimental check, a set of load-voltage oscillograms, several of which are shown in Figure 6, were taken with the circuit parameters and conditions as in Appendix I and with the core of Figure 10. The calculated firing-angle curve, together with the experimental points, are shown in Figure 12.

Output

Assumption 3. While ignoring the prefiring skirt of the output-voltage wave form introduces small error in the approximate calculation of the firing angle, it may introduce serious error in the approximation of output.

As is obvious in the first two oscillograms of Figure 6, for values of B_0 near cut-off there may be measurable output, even though sharp firing never occurs. As an approximation to be evaluated later by comparison of calculated and measured static characteristic or transfer curves, let the alternating exciting current corresponding to the value of working flux density B_m be added to the postfiring output to represent the contribution of the prefiring skirt. Comparison of transfer curves will indicate whether this approximation is justified.

To find the output current and voltage after firing, it is convenient to express the output voltage as an integrated average value. To be consistent with the assumptions made thus far, if all the circuit resistance is in the load

$$e_L = E_m \sin\theta;\quad \theta_f \leq \theta \leq \pi$$

Expressing the postfiring output voltage as an integrated average value

$$e_{L(Avg)}\Big|_{\theta_f}^{\pi} = \frac{1}{2\pi}\int_{\theta_f}^{\pi} E_m \sin\theta d\theta \qquad (12)$$

$$e_{L(Avg)}\Big|_{\theta_f}^{\pi} = \frac{E_m}{2\pi}(1+\cos\theta_f) \qquad (13)$$

From equation 11

$$e_{L(Avg)}\Big|_{\theta_f}^{\pi} = \frac{E_m}{2\pi}\left(1+\frac{B_m+B_0-B_f}{B_m}\right)$$

A more convenient form of output relation results if a critical maximum voltage is defined as

$$E_f = \omega NAB_f \times 10^{-8}$$

Thus

$$e_{L(Avg)}\Big|_{\theta_f}^{\pi} = \frac{\omega NA}{2\pi}(2B_m+B_0-B_f)\times 10^{-8}$$

And finally

$$\frac{e_{L(Avg)}\Big|_{\theta_f}^{\pi}}{E_f/2\pi} = \frac{2B_m}{B_f}+\frac{B_0}{B_f}-1 \qquad (14)$$

Since

$$0 \leq \theta_f \leq \pi$$

The output is restricted

$$0 \leq \frac{e_{L(Avg)}\Big|_{\theta_f}^{\pi}}{E_f/2\pi} \leq \frac{2E_m}{E_f}$$

The ideal postfiring average output of the half-wave magnetic amplifier circuit is shown in Figure 9 as a function of B_0/B_f, with B_m/B_f as parameter.

Transfer Characteristic

The ideal transfer characteristic of Figure 9 shows several features of magnetic amplifier operation. For values of maximum working magnetic flux density in excess of the critical value $B_m/B_f > 1.0$, the output can never be reduced to zero, regardless of the premagnetization. For lower values of B_m/B_f, the characteristic shows both cut-off and saturation.

The transfer curves of Figure 9 are more useful when put in the form of an output versus control magnetic intensity chart with B_m/B_f as parameter, as in Figure 13.

The calculated transfer curves are shown with the H_0 magnetic intensity value found from the upper branch of the hysteresis loop of Figure 11, and with several values of B_m/B_f as parameter. It should be noted that the calculation gives $e_{L(Avg)}/(E_f/2\pi)$ as a function of H_0 without regard for the particular values of circuit parameters. Further, the alternating exciting currents are added to the postfiring output to account for the effect of the prefiring skirt, which is particularly evident near cut-off and below.

A set of measured transfer curves for magnetic and electric circuit parameters as in Appendix I and Figure 10, is shown in Figure 14. In order to simplify comparison, the output has been reduced to the general form given in equation 14. Values of control H_0 given in Figure 14 were calculated from the data by making use of the known control winding turns and current and an arithmetic mean value of magnetic path length (21.9 centimeters).

The general shapes of the calculated and measured curves show certain resemblances. The calculated effects of saturation for positive values of H_0 are seen to occur in the measured curves, and the nature of the effect of the working value of flux density (B_m/B_f) on the value of H_0 for cut-off is seen to be predicted correctly by the calculation. There is one notable difference between the calculated and measured curves—the slope and location of the linear, sensitive portion of the transfer curve.

In order that this discrepancy between the calculated approximate performance may be seen more clearly, the measured transfer curve for $B_m/B_f = 0.57$ and a transfer curve for that value calculated by making use of the approximations are shown in Figure 15. The measured transfer curve *A* is plotted as before, with H_0 found by the calculation from the known magnetomotive force and the arithmetic mean magnetic path length. It is obvious that the linear portion corresponds neither in slope nor in location with the calculated curve.

Possible explanations of the discrepancy involve the effect of distributed magnetic circuit of test core and eddy-current shielding of the core laminations.

Magnetic Circuit

The ratio of extreme to mean magnetic path length of the core structure of Figure 10 is 1.36, while the ratio of longest to shortest path length is 2.14. In view of these values, it might be questioned whether calculations of the sort thus far outlined, which are based on a lumped magnetic circuit with a fixed path length, properly apply to such a core.

It is of interest to note that if the experimental data are reduced to fundamental units on the basis of the extreme length of magnetic path, rather than the mean value, a better correspondence between calculated and measured performance is found. (Compare transfer curves *B* and *C* with *A* of Figure 15.)

Eddy-Current Shielding

In order to evaluate the order of magnitude of hysteresis loop broadening due to operation with time-varying flux, the techniques given in the literature may be employed.[8–11] It is intended that only the order of magnitude of the worst possible effect be calculated, to determine whether eddy-current demagnetization might be sufficient to cause the shift of the linear portion of the transfer curve noted in Figure 15. As a matter of fact, the techniques which are available for calculating the effects of eddy-current shielding are, in general, based on sinusoidal conditions and low or moderate values of magnetic flux density. In spite of this, it is reasonable to expect that at least the order of magnitude of the effect can be predicted in this case.

In order to determine the maximum broadening to be expected, for the 47.5 per cent nickel-iron alloy used, take:

$\rho = 51\times 10^{-6}$ ohm-centimeters
$\mu = 50{,}000$ centimeter-gram-second electromagnetic units
$f = 60$ cycles per second
$a = 0.0084$ centimeter–half lamination thickness

Then, following the procedure of reference 10:

$$\frac{L}{L_0} = \frac{s}{2a}\,\frac{\sinh\left(\frac{2a}{s}\right)+\sin\left(\frac{2a}{s}\right)}{\cosh\left(\frac{2a}{s}\right)+\cos\left(\frac{2a}{s}\right)}$$

where s is the skin depth and is given by

$$s = \frac{5{,}030}{\sqrt{\frac{\mu f}{\rho}}} = 0.0208 \text{ centimeter}$$

The ratio of lamination thickness to skin depth is

$$\frac{2a}{s} = 0.804$$

Solving

$$\frac{L}{L_0} = 0.96$$

At this frequency, the greatest broadening of the hysteresis loop which might be expected is of the order of 4 per cent, a value insufficient to resolve the discrepancy between curves *A* and *C* of Figure 15. Consequently, it is inferred that the effect is probably due principally to the radial depth of the test core.

References

1. Bibliography on Transductors, Magnetic Amplifiers, H. B. Rex. *Instruments* (Pittsburgh, Pa.), volume 21, number 4, 1948, page 332.

2. Uber gleichstrom vormagnetisierte Wechselstrom drosselspulen und deren Ruckkoppling, Th. Buchold. *Archiv für Elektrotechnik* (Berlin, Germany), volume 36, 1942, pages 221–37.

3. UNITED STATES PATENT 1,730,254, Phillips Thomas. October 1, 1929.

4. UNITED STATES PATENT 1,328,797, Ernest F. Alexanderson. January 20, 1920.

5. INFLUENCE OF CORE MATERIAL ON MAGNETIC AMPLIFIER DESIGN, A. O. Black. *Proceedings*, National Electronics Conference, (Chicago, Ill.) volume 4, 1948, pages 427–35.

6. UNITED STATES PATENT 1,997,179, Frank G. Logan. Issued April 9, 1935.

7. THE TRANSDUCTOR, D. C. PRE-SATURATED REACTOR, WITH SPECIAL REFERENCE TO TRANSDUCTOR CONTROL OF RECTIFIERS, U. Lamm. *Esselte Aktiebolag* (Stockholm, Sweden), 1943.

8. WIRKSAME PERMEABILITAT UND EISENVERLUSTE IN BLECHEN UND DRAHTEN BEI SCHWACHEN MAGNETISCHEN FELDERN, W. Cauer. *Archiv für Elektrotechnik* (Berlin, Germany), volume 15, number 4, 1925, page 308.

9. A BRAUN TUBE HYSTERESIGRAPH, J. B. Johnson. *Bell System Technical Journal* (New York, N. Y.), volume 8, 1929, pages 286–308.

10. VARIATION OF THE INDUCTANCE OF COILS DUE TO THE MAGNETIC SHIELDING EFFECT OF EDDY CURRENTS IN THE CORES, K. L. Scott. *Proceedings*, Institute of Radio Engineers (New York, N. Y.), volume 18, 1930, pages 1750–64.

11. EDDY CURRENTS IN COMPOSITE LAMINATIONS, E. Peterson, L. R. Wrathall. *Proceedings*, Institute of Radio Engineers (New York, N. Y.), volume 24, 1936, pages 275–86.

12. AN ANALYSIS OF MAGNETIC AMPLIFIERS WITH FEEDBACK, D. W. VerPlanck. *Proceedings*, National Electronics Conference, (Chicago Ill.) volume 4, 1948, page 426.

13. EIN EINFACHER GLEICHSTROMMESSWANDLER MIT ECHTEN STROMWANDLEREIGENSCHAFTEN, W. Kramer. *Elektrotechnische Zeitschrift* (Berlin, Germany), volume 58, 1937, pages 1309–13.

14. EIN NEUER GLEICHSPANNUNGSMESSWANDLER ZUR MESSUNG HOHER GLEICHSPANNUNGEN, W. Kramer. *Elektrotechnische Zeitschrift*, volume 59, 1938, pages 1295–8.

Discussion

L. A. Finzi, D. W. VerPlanck, D. C. Beaumariage (Carnegie Institute of Technology, Pittsburgh, Pa.):

Mr. Dornhoefer is to be congratulated for an analysis of a half-wave magnetic amplifier circuit which is very interesting and thought provoking, and which appears to throw further light on the behavior of practically useful circuits having two or more elements. It would be of interest to know more about how the author uses his results as obtained for the single-element case in predicting the performance of actual amplifiers.

Comparing Mr. Dornhoefer's analysis with that given by the writers in a paper presented at the same session,[1] it is seen that there are two basic differences in the underlying assumptions:

1. Mr. Dornhoefer assumes that his control circuit has infinite impedance to harmonic currents, while the writers' analysis[1] takes the opposite extreme and assumes that harmonic currents may circulate freely there.

In other terms the writers have dealt with a case which has been called unconstrained or natural magnetization[2] while Mr. Dornhoefer assumes totally constrained conditions.

2. Mr. Dornhoefer treats a case where the magnetization curve has a relatively sharp knee and then becomes essentially horizontal permitting no further increase in flux. The writers, on the other hand, treat the case of a core which never reaches complete saturation.

It appears thus that by other combinations of the alternatives under each of the preceding statements, one may have two additional extreme types of treatment, making a total of four. It is conceivable that practical conditions might arise making anyone of these four the best approach.

REFERENCES

1. ANALYTICAL DETERMINATION OF CHARACTERISTICS OF MAGNETIC AMPLIFIERS WITH FEEDBACK, D. W. VerPlanck, L. A. Finzi, D. C. Beaumariage. *AIEE Transactions*, volume 68, part I, 1949, pages 565–70.

2. THE TRANSDUCTOR, H. B. Rex. *Instruments*, (Pittsburgh, Pa.) volume 20, 1947, page 1104.

S. B. Cohen (Sperry Gyroscope Company, Great Neck, N. Y.): The assumptions used for the determination of θ_f are valid for most of the magnetic materials used, including cores made up of EI^-, EE^-, or F-type laminations. We have been able to reproduce Figure 12 of the paper with about the same degree of correlation using EI and EE cores.

The determination of the output (voltage or current) is, however, a more difficult problem and depends upon the assumptions used and the type of core construction and material.

In Appendix I, the step-by-step solution of the differential equation 5 of the paper yields a current or voltage output across the load as is shown in Figures 4 and 5 of the paper. The angle at which conduction ceases can be called the conduction angle θ_t. In Figure 4 of the paper the conduction angle is about 260 degrees. This conduction angle is a function of the material and core structure if the core is made up of EI- or EE-type laminations. However, in Appendix II, the angle of conduction was assumed to be 180 degrees. This assumption is valid if:

1. Most of the resistance of the circuit is assumed to be in the load.

2. The material and core used present an extremely low effective impedance when saturated, that is, the B-H characteristic is almost a horizontal line after saturation.

These assumptions are essentially valid for the particular examples in Appendix II. However, in those cases where core structure (that is, EI-type), and material do not have the characteristic as mentioned previously in assumption 2 (for example, silicon iron for higher power work), and where the load resistance is comparable to the rectifier impedance, which may be the case when matching for maximum power into the load, the assumptions are not quite valid.

If equation 2 of the paper were solved for the current

$$E_m \sin \omega t = N\frac{d\phi}{dt}10^{-8}+iR_T+e_R$$

or

$$E_m \sin \omega t = L\frac{di}{dt}+iR_T+e_R$$

where

$L(di/dt)$ is the equivalent of $N(d\phi/dt)10^{-8}$

The value of L is the equivalent value of inductance of the core when saturated, because we are interested in the current during the conduction period. If assumption 2 were valid, then L would be zero.

For some value of θ_f; say $\theta_f = 0$ degrees, the solution of equation 2

$$i(t)=I_m\left[\sin(\theta-\omega t)+\frac{\sigma}{\cos\theta}-\left(\sin\theta+\frac{\sigma}{\cos\theta}\right)\epsilon^{\frac{-R_T}{\omega L}\omega t}\right]$$

where

$$I_m=\frac{E_m}{\sqrt{R_T^2+(\omega L)^2}}$$

$$\theta=\tan^{-1}\frac{\omega L}{R_T}$$

$$\sigma=\frac{e_R}{E_m}$$

to solve for θ_t, set $i(t)=0$

$$\sin(\theta-\theta_t)+\frac{\sigma}{\cos\theta}=\left(\sin\theta+\frac{\sigma}{\cos\theta}\right)\epsilon^{\frac{-R_T}{\omega L}\theta}$$

This equation is solved empirically for θ_t.

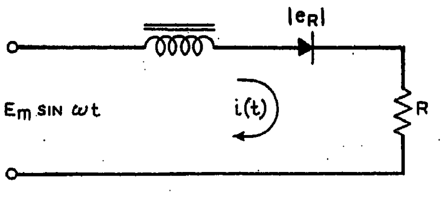


Figure 1. Self-saturable magnetic amplifier circuit

The solution for θ_t as a function of $\omega L/R_T$ is plotted in Figure 2 of this discussion with σ as a parameter. From this plot, we can see that for $\sigma = 0$ assuming $e_R = 0$ and rectifier is constant resistance lumped in R_T, the conduction angle is 180 degrees as $\omega L/R_T$ approaches zero. It is possible to obtain large values of θ_t if $\omega L/R_T$ increases as is shown. It also is interesting to note that for finite values of σ, the conduction angle can be less than 180 degrees.

The use of material in cores other than those mentioned in assumption 2 introduces finite values of $\omega L/R_T$ which can be large enough to yield values of θ_t, which are appreciably higher than 180 degrees. The errors introduced in the calculation of power output by using 180 degrees instead of the θ_t from Figure 2 of the discussion may be as high as 25 per cent in some cases.

Since in many applications power is the primary interest, a plot of power as a function of $\omega L/R_T$ is shown in Figure 3 of the discussion for various values of σ. If E_m were 37 volts (26 volts rms) and the drop across a typical rectifier were 2 volts, then $\sigma = 2/37 = 0.054$. This would mean a loss

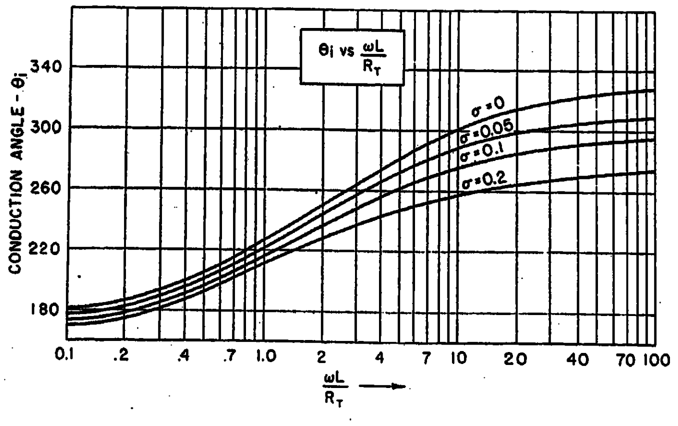


Figure 2. Conduction angle (θ_l) as a function of $\omega L/R_T$

of about 20 per cent in power at optimum conditions.

It is interesting to note that the optimum value of $\omega L/R_T$ for maximum power is about 2.4 for $\sigma = 0$. This means that $R_T = \omega L/2.4$ for maximum transfer of power to R_T.

Another set of curves, Figure 4 of the discussion, indicates the effect of the rectifier resistance in the power transferred to the load. These curves were calculated for $\sigma = 0$, but assuming the rectifier to be a resistance equal to R_T. This means that e_R actually exists and varies, but according to the method used in the setting up of the equation, we have $\sigma = 0$. As can be seen from these curves, the point of maximum power varies with variations in λ.

The type of calculation and curves presented in this discussion can be used to obtain a step-by-step determination of the transfer curves in the self-saturable amplifier. We have used the curves drawn for various values of θ_f, σ, and θ_l to obtain end design figures for various types of amplifier applications.

The experimental results yield fairly close correlations using the design values so obtained. The use of *EI*-type laminations yield inferior results with respect to gain per unit time constant as compared to the solid stamping or ribbon-wound type cores. However, the maximum power obtained is almost the same for a given size core, and the stability obtained as far as reproducibility is concerned is quite good.

In many applications, the lower performance can be tolerated, but the reproducibility is very desirable.

It may be of interest, therefore, to include in the theory of calculating procedure methods for handling cores which do not exhibit the ideal characteristics, and also conditions in which the forward characteristics of the rectifiers used are not negligible in comparison to the load and anode winding.

H. F. Storm (General Electric Company, Schenectady, New York): In his very interesting paper Mr. Dornhoefer presents several transfer characteristics of magnetic amplifiers, such as those shown in Figures 13–16 of the paper. These characteristics demonstrate the performance of magnetic amplifiers in the region of high gain; that is, between the lower and the upper right knee, but they do not indicate the trend beyond the two knees. In Appendix II, Mr. Dornhoefer mentions correctly that the operation beyond the lower knee is related to the mode of operation of d-c metering circuits, and then lists references 13 and 14. While Mr. Dornhoefer's paper deals with regenerative circuits, the aforementioned reference does not include such circuits; hence the reader may remain in doubt as to the performance of magnetic amplifiers beyond the lower knee. Inspecting Figures 13–16 of Mr. Dornhoefer's paper, the reader may conclude that for large negative control currents the transfer characteristics approach zero output current. This, however, is not so; on application of large negative control currents the load current again increases as shown in Figures 5 and 6 of the discussion. It is the object of the following discussion to describe the transfer characteristic beyond the region shown in Mr. Dornhoefer's paper.

Figure 5 of the discussion demonstrates a transfer characteristic of a magnetic amplifier which Mr. Dornhoefer calls "full-wave" self-saturating connection, Figure 18 of the paper; more specifically, it relates to a circuit where the impedance of the source of control current is small in comparison with the resistance of the control winding. In that region of the transfer characteristic which is located to the right of the lower knee, a change of control current I_c in a negative direction will cause a reduction of the load current I_L, both currents being measured in average values. However, if the control current is made more negative than the value corresponding to the lower knee, that is, in this case, more negative than —5 milliamperes, the load current again increases. In order to demonstrate the full range of negative control currents, Figure 6 of the discussion utilizes a much larger scale for the control-current scale. It can be seen from this figure that the load current I_L first increases almost linearly with negatively increasing control current I_c, and finally approaches a horizontal asymptote at the same level as for very large positive control current (Figure 5 of the discussion). It is an important conclusion that increasing the negative control current causes a reduction of load current when operating to the right of the lower knee, but an increase of load current when operating to the left of the lower knee.

In automatic-control and regulator engineering an error signal often is used to excite the control winding of a magnetic amplifier. This error signal normally operates on the steep section of the transfer characteristic, that is, to the right of the lower knee. The magnetic amplifier usually is connected into the regulated system in such manner as to reduce the error signal and thus tends to keep the regulated quantity constant. If now a large disturbance occurs in the regulated system, the error signal suddenly may become so large as to operate on that part of the transfer characteristic which is to the left of the lower knee. Hence, the load current instead of becoming smaller actually may increase instead and thus produce a still larger error signal. As a result

Figure 3. Power coefficient (ξ) as a function of $\omega L/R_T$ with σ as a parameter

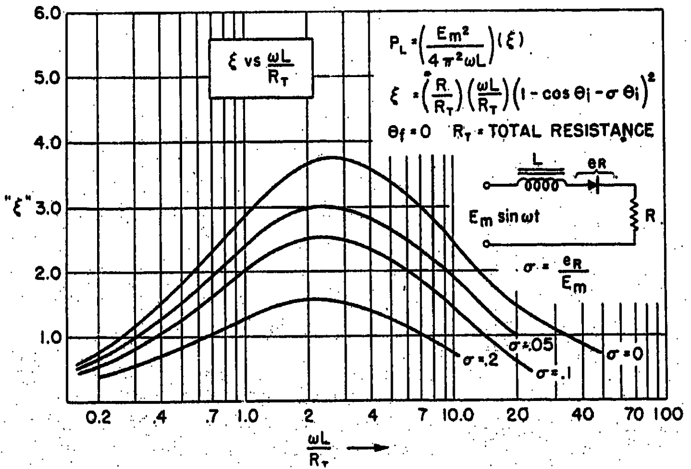
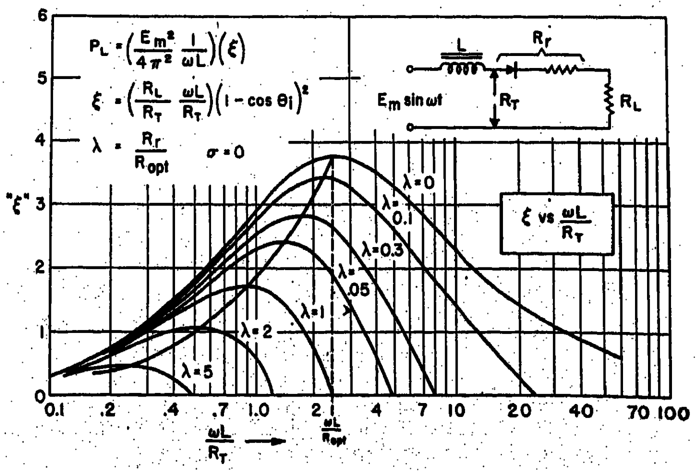


Figure 4. Power coefficient (ξ) as a function of $\omega L/R_T$ with λ as a parameter



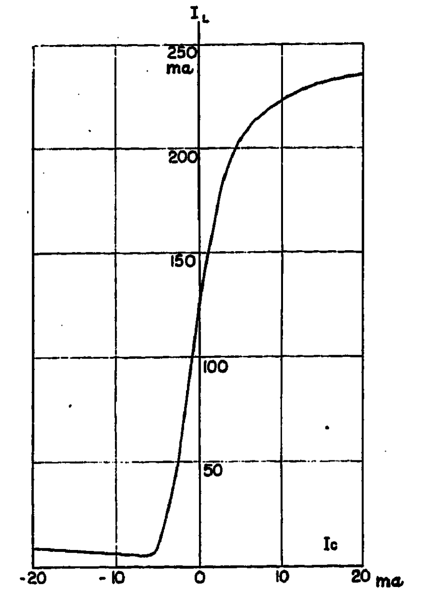


Figure 5. Transfer characteristic of magnetic amplifier in the region of highest gain

the system will no longer operate as a regulator but may drive itself into a ceiling condition. Such occurrence obviously should be avoided, and in order to evaluate the possibility of loss of control, it is necessary to know the transfer characteristic to the left of the lower knee.

There are theoretical reasons why this transfer characteristic should approach a straight line throughout a considerable range of negative control currents as demonstrated by Figure 6 of the discussion. While space does not permit dwelling on these reasons in this discussion, the resulting equation for the straight line approximation can be stated simply.

$$I_L = -I_c\frac{N_c}{N_L}$$

where I_L is the average current in the load resistor (see Figure 18 of the paper), I_c the average current in the control winding, N_c the number of turns of the control winding, and N_L the number of turns of the anode winding.

This equation gives a good approximation when the reactor material is operated at magnetic flux densities at or below the critical value, as defined in Mr. Dornhoefer's paper.

Figure 7 of the discussion shows an oscillogram taken in the region to the left of the lower knee. The upper trace shows the load current, the lower trace the control current.

W. J. Dornhoefer: The writer concurs precisely with the accurate and concise summary given by L. A. Finzi, D. W. VerPlanck, and D. C. Beaumariage of the differences between the basic assumptions made in this paper and in a paper presented by them.[1]

In the writer's case of totally constrained magnetization, the experimental results for a case approaching the assumed conditions showed moderately good correlation with the theory. In the case of unconstrained magnetization, the discussors' results indicated excellent correlation with the theory. It may be taken for granted then, that both approaches lead to correct results for the assumed conditions.

The constrained magnetization condition was assumed in this paper in order that results ultimately applicable to the dynamic performance might be obtained. Since the time delay found in magnetic amplifier circuits resides principally in the control circuit, with a small residual delay in the anode circuit, the assumption of a control circuit lacking resistance could not be made, as this would involve theoretically infinite delay. While the resistance values required in the control circuit for quick (1-cycle) response are generally not sufficient to suppress harmonic currents entirely, they are sufficient to make the assumption quite reasonable.

The dependence of the transient response of saturable reactors upon the control circuit resistance was given in an unpublished paper by Mr. Finzi, Mr. VerPlanck and Mr. Beaumariage. In this paper, the assumption of zero control circuit impedance would have led to trivial results and could therefore not be made as it was before. Rather, the significant time-delay factor was shown to be of the nature

$$\tau_s = \frac{2N_1A}{\mu R_1 I_{1\infty}}$$

where

N_1 = control turns
A = core area
R_1 = proportional to control circuit resistance
μ = magnetization curve-fitting argument
$I_{1\infty}$ = ultimate value of control circuit current

A similar relation can be shown for the self-saturating case with small changes of output for which an effective permeability can be defined. Following equation 14 of the paper

$$\frac{d\left(e_{L(avg)}\Big|_{\theta_f}^{\pi}\right)}{dt} = \frac{d}{dt}\left\{\frac{\omega NA}{2\pi}(2B_m+B_0-B_f)\times 10^{-8}\right\} \quad (15)$$

$$\frac{d\left(e_{L(avg)}\Big|_{\theta_f}^{\pi}\right)}{dt} = fNA\frac{dB_0}{dt}\times 10^{-8} \quad (16)$$

$$i_0 = i_{00} + \frac{E_{dc}}{R_1}\left(1-\epsilon^{-\frac{t}{\tau_1}}\right) \quad (17)$$

$$B_0 = \mu\frac{0.4\pi N_1}{l}i_0 \quad (18)$$

$$B_0 = \mu\frac{0.4\pi N_1}{l}\left\{i_{00} + \frac{dc}{R_1}\left(1-\epsilon^{-\frac{t}{\tau_1}}\right)\right\} \quad (19)$$

where

$$\tau_1 = \frac{0.4\pi N_1^2\mu A}{lR_1}\times 10^{-8} \quad (20)$$

$$\frac{dB_0}{dt} = \frac{E_{dc}}{N_1A}\epsilon^{-\frac{t}{\tau_1}}\times 10^8 \quad (21)$$

$$\frac{d\left(e_{L(avg)}\Big|_{\theta_f}^{\pi}\right)}{dt} = \frac{N}{N_1}fE_{dc}\epsilon^{-\frac{t}{\tau_1}} \quad (22)$$

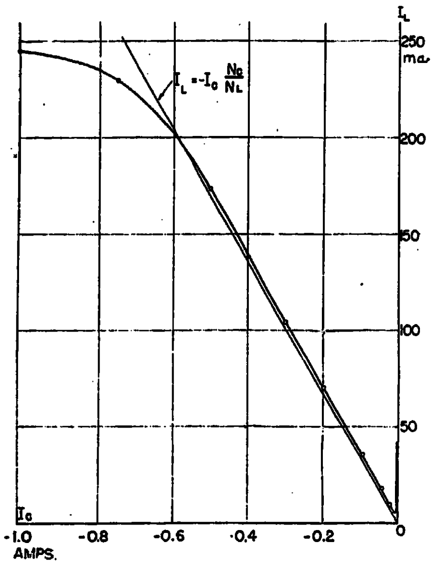


Figure 6. Transfer characteristic of magnetic amplifier for negative control currents

Anode winding: 592 turns each, control winding: 200 turns

where

i_{00} = initial control current
E_{dc} = amplitude of step-function input voltage change
R_1 = control circuit resistance
τ_1 = control circuit time constant (approximate)
N_1 = control winding turns

and the remainder of the nomenclature is as given in the paper.

Again, as in the results cited previously, the control circuit resistance is an important quantity insofar as transient response is concerned. The values of R_1 for fast response were felt to be such that the case of zero harmonic currents in the control circuit would be the more generally useful one, particularly where transient response might be an important consideration.

Mr. Cohen's discussion, giving the calculation of self-saturating magnetic amplifier output for the completely saturated condition, is a useful contribution to the calculation methods for this type of circuit. The assumption of a constant post-firing inductance adds to the difficulty of literal solution but leads to extremely interesting results.

Essentially the method given by Mr.

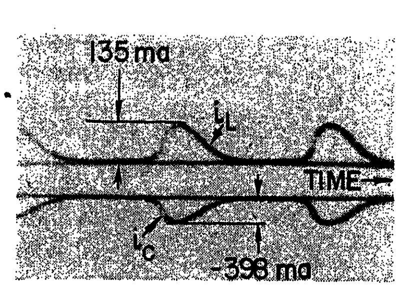


Figure 7. Oscillogram of load current I_L and control current I_c

I_L: upper trace, 15.3 milliamperes per millimeter, peak current 135 milliamperes
I_c: lower trace, 75 milliamperes per millimeter, peak current 398 milliamperes

Cohen involves an assumption resembling Mr. Boyajian's "2-zone" magnetization curve,[2] as does the method proposed in the paper. In Mr. Cohen's case, particular attention was paid to the second, or saturated, zone.

Adapting equations 21–24 of Mr. Boyajian's paper to this case, and using the nomenclature of the discussion, one finds an expression for the current

$$i_{(t)}=I_m\left\{\sin(\omega t-\theta)-\frac{\sigma}{\cos\theta}+\left(\sin\theta+\frac{\sigma}{\cos\theta}\right)\epsilon^{-\frac{R_T}{L}}\right\} \quad (23)$$

As Mr. Boyajian points out, and is evident in his Figures 5 and 6, the exponential decay of current after the axis-crossing with negative slope of the supply voltage is principally a zone *1*, or unsaturated, variation. Examination of a numerical integration schedule, or of Figures 6 and 7 of the paper, seems to confirm this. Of course, by his assumption $\theta_f=0$, Mr. Cohen has excluded all unsaturated operation of the reactor and has given a useful calculation method for the saturated output. It would be interesting to see a general "2-zone" treatment in which θ_f was not so restricted.

The treatment of the effect of nonideal rectifiers on the saturated output also is of considerable interest, particularly in view of the indication given in Figure 2 of the discussion that assumption 3 of the paper would be very poor for large values of $\omega L/R_T$ and small values of σ.

The discussion of the shape of the transfer characteristic of a self-saturating magnetic amplifier in the range of extreme negative premagnetization beyond the cutoff point, or lower knee, which Mr. Storm has prepared, represents an important addition to the material contained in the paper. The mode of firing involved in operation in this region has been described[3,4] and is presented briefly: Due to saturation, the reactor cannot initially change its flux linkages and thus can introduce no voltage drop in the anode circuit. As a result, the anode current, following the axis-crossing with positive slope of the supply voltage, increases sinusoidally, as in a resistive circuit, until it has reached a value sufficient so that the anode magnetomotive force equilibrates the premagnetizing magnetomotive force. When this condition prevails, the reactor core becomes unsaturated and is capable of developing an inductive voltage in the anode winding. This voltage assumes a value so that the load current is constant and gives rise to an anode-winding magnetomotive force which remains equal to the premagnetizing magnetomotive force during the remainder of the alternation. Similar events occur in the other anode circuit during the following alternation, causing the load current to resemble a drastically clipped and rectified sinusoid with amplitude directly related to the premagnetization through the equality of magnetomotive forces.

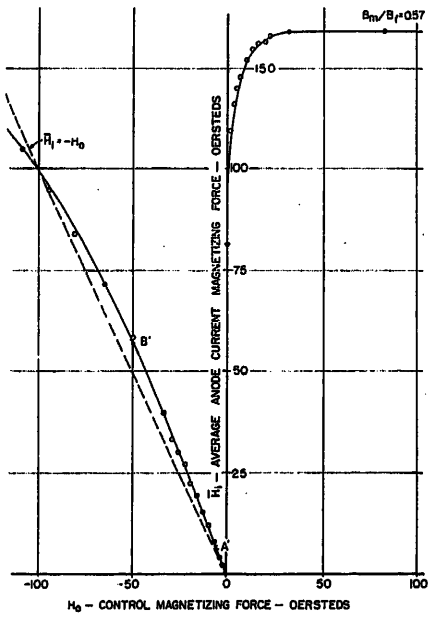


Figure 8. Transfer characteristic of a full-wave magnetic amplifier over extreme control range

$E_m=141.4$ volts
$l=20.1$ centimeters
$R=40.5$ ohms
$N=1,200$ turns
$\omega=377$ radians per second
$A=3.5$ square centimeters

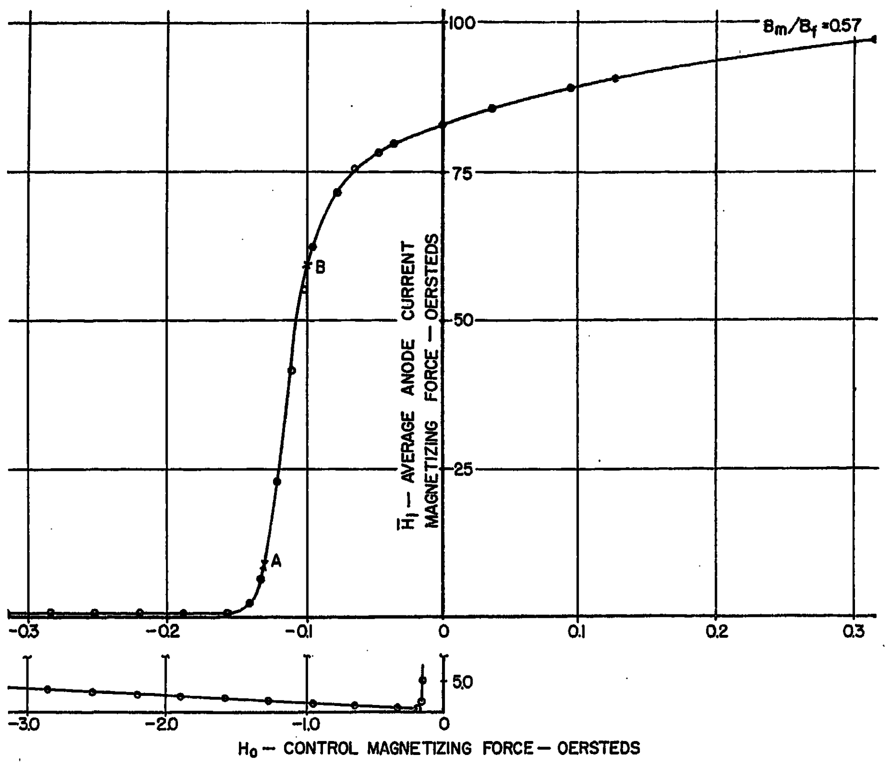


Figure 9. Transfer characteristics of a full-wave magnetic amplifier over normal control range

As demonstrated in Mr. Storm's discussion, the fundamental relation for this mode of operation is

$$H_t+H_o=0 \quad (15)$$

Since H_t may be taken approximately as a square wave, the average value, when rectified, is equal to the amplitude, and

$$H_{t(avg)}=-H_o \quad (16)$$

Figures 8 and 9 of the discussion are given in order to show the extreme anisotropy displayed by a self-saturating magnetic amplifier with respect to the direction of control premagnetization on opposite sides of cut-off. They are quite similar to Figures 5 and 6 of Mr. Storm's discussion, referring as they do to the same type of circuit, and are presented to illustrate the effect of extreme negative premagnetization for conditions similar to those of the illustrative example given in the paper. While the saturable-core reactors employed were not identical with those described in Appendix I, they were gapless and consisted of the same material, and had magnetization data similar to Figure 11.

The increase of output on either side of the cut-off point, which has been pointed out by Mr. Storm with particular reference to automatic control and regulator applications, is evident in Figure 8 of the discussion, and in the inset of Figure 9 of the discussion. In order to determine the importance of the loss of signal polarity sense in automatic control work, the orders of magnitude of the effects must be considered. As an example, in regulator applications, it is generally desirable to limit the range of operation of the amplifier to that portion of the characteristic between points A and B of Figure 9, at which the sensitivity (slope) has been reduced to half the maximum value. In order to permit operation in this region with a reversible polarity signal, it is necessary to provide a bias of approximately −0.115 oersted; while a single polarity signal would require a negative bias of at least 0.130 oersted.

Generally, the use of extreme negative bias on a single-ended amplifier, together with signal values of commensurate magnitude, is to be avoided, since the result is usually reduced power gain, slow dynamic response, and inordinate sensitivity of the amplifier to supply voltage changes. In the case of a push-pull amplifier, bias is used only to establish the balanced quiescent outputs of each channel, and is quite small in value. Push-pull amplifier stages are not subject to complete loss of signal polarity sense under extreme signal conditions, and are thus excluded from this discussion. Actually, the only amplifiers subject to this possibility are single-ended units operating with reversible polarity signals or with extreme negative bias. In addition, good practice dictates the matching of impedances of intermediate stages of multistage amplifiers, thus precluding the possibility of loss of polarity sense due to overdrive in any but the lowest-level stage.

In automatic control work, then, the cases where the possibility of loss of polarity sense is a significant consideration are:

1. Input stage, single-ended, reversible polarity signal.

2. Input stage, single-ended, extreme single-polarity signal and bias values.

Examination of the co-ordinates of the points A and B mentioned previously, and of corresponding points (A′ and B′) on the left portion of the transfer characteristic indicates the signal values required to cause "transformer" mode operation, shown in Table I.

The sensitivity ratio is seen to be (49.4−5.5)/(0.130−0.0098) = 1,370, while the bias ratio, or permissible signal overdrive ratio is 5.5/0.13 = 42.3.

As indicated above and in Figures 8 and 9 of the discussion, a difference of sensitivity of more than three orders of magnitude is seen to exist on opposite sides of cut-off. A signal overdrive of at least 40 times is evidently permissible as well. As the quality of the magnetic core material is lowered, and as the reverse resistance of the rectifiers is reduced, these ratios become smaller, and may ultimately reach values that would be troublesome.

Table I

Point	$H_{i(avg)}$ Oersteds	H_0 Oersteds
A	6.75	− 0.130
A′	6.75	− 5.5
B	57.8	− 0.098
B′	57.8	−49.4

Similar ratios for Mr. Storm's Figures 1 and 2, locating the operating limits as before shown in Table II.

The sensitivity ratio equals (550−25)/(2.5+3.9) = 82, and the permissible signal overdrive ratio equals 75/3.9 = 19.2.

Providing the overdrive on the input stage due to the extreme value of a push-pull signal, or of the bias alone with zero value of extreme single-polarity signal, is maintained within the foregoing limits (20 to 40), little difficulty is encountered with the "transformer" mode of operation. It also should be mentioned that optimum gain and speed of response considerations in high-performance regulators and servomechanisms generally limit the operating levels to maximum overdrive ratios of approximately three to five.

Table II

Point	I_1 Milliamperes	I_0 Milliamperes
A	25	− 3.9
A′	25	− 75
B	180	+ 2.5
B′	180	−550

REFERENCES

1. ANALYTICAL DETERMINATION OF CHARACTERISTICS OF MAGNETIC AMPLIFIERS WITH FEEDBACK, D. W. VerPlanck, L. A. Finzi, D. C. Beaumariage. *AIEE Transactions*, volume 68, part I, 1949, pages 565–70.

2. MATHEMATICAL ANALYSIS OF NON-LINEAR CIRCUITS, A. Boyajian. *General Electric Review* (Schenectady, N. Y.) volume 34, September 1931, pages 531–37.

3. UNITED STATES PATENTS 2,137,878, 2,153,377, and 2,153,378. W. Kramer. November 22, 1938 and April 4, 1939.

4. See references 13 and 14 of the paper.

Automatic Grounding and Air-Break Switches for Protection of Transformer Stations

E. A. RICKER
MEMBER AIEE

A COMBINATION of an automatic-grounding switch with an automatic-opening air-break switch can be used as a substitute for a 110-kv circuit breaker. Although customers, who are supplied from a small transformer station tapped to a 110-kv line, would have an occasional interruption in service, they are receiving electric energy at a lower rate per kilowatt-hour because of the lower capital cost of the transformer station. Since each 110-kv circuit breaker requires two isolating disconnecting switches, the capital reduction for each combination of automatic switches is almost equal to the installed cost of one circuit breaker. With proper installation and maintenance, the switches will give reliable protection for small transformer stations.

The following paragraphs give a discussion of the preferred station diagrams, ways of mounting these switches, and reviews of experience since 1930 with several types of automatic switches operated by spring and motor-driven mechanisms amounting to a total of 260 switch years. The favorable operating experience with automatic disconnecting switches has resulted in their adaptation for use in other parts of transformer stations.

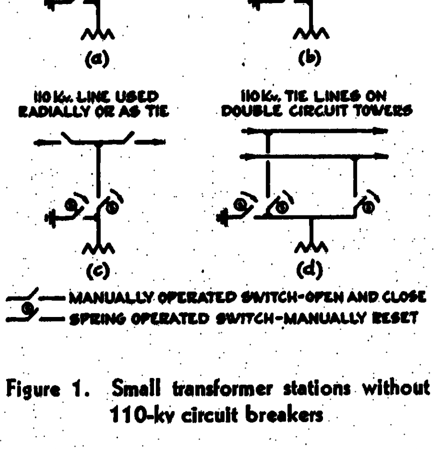

RADIAL 110 Kv. LINE
110 Kv. TIE LINE
(a)
(b)
110 Kv. LINE USED RADIALLY OR AS TIE
110 Kv. TIE LINES ON DOUBLE CIRCUIT TOWERS
(c)
(d)
MANUALLY OPERATED SWITCH-OPEN AND CLOSE
SPRING OPERATED SWITCH-MANUALLY RESET

Figure 1. Small transformer stations without 110-kv circuit breakers

Sequence of Operation

When automatic grounding and air-break switches are used at transformer stations, the circuit breakers at distant stations are given the double duty of protecting both the transmission line and the distant transformer. An automatic single-phase grounding switch, located on the 110-kv switching structure of a transformer station, is used to apply a solid line-to-ground fault on a transmission line whenever the protective relays of a transformer have detected a fault. The artificial line-to-ground fault is detected by ground relays at distant stations, causing circuit breakers to operate and disconnect the sources of energy from the faulted line. After the line is de-energized, an automatic 3-pole air-break switch is opened on the 110-kv side of the defective transformer to disconnect from the line both the transformer bank and the single-pole grounding switch. This combination of automatic switches allows a small transformer station to be supplied from a high-voltage transmission line without using an expensive 110-kv circuit breaker.

Since the trip circuit of the air-break switch is controlled by a pallet switch on the automatic grounding switch, a timer is usually put into the circuit to delay the tripping of the 3-pole automatic air-break switch by a time interval ranging from 0.5 to 3.0 seconds. During this interval, the distant circuit breakers will have time to interrupt the flow of fault current, and the automatic air-break switch can be opened with safety. With the faulted transformer removed from the system, automatic reclosing relays can close the circuit breakers after a time delay and restore the line to service.

General System Conditions Permitting the Use of Automatic Switches

Automatic grounding switches are not recommended for stations which supply part of a load along with local generation, because the artificial fault will cause a disturbance to both the system and local generation. For the same reason, they should not be used with transformers which are a tie between two parts of a large system where power might be transferred in either direction. This objection is reduced if the low-voltage circuit breakers are made to operate first, and the artificial ground fault is applied after a time delay. However, the distant circuit breakers are tripped after a longer delay, thereby giving slower clearance of the faulted transformer. In Table I, there are at least five installations which are exceptions to the above preferred system conditions. When the local station is connected to a source of generation on the low-voltage side, the 110-kv windings of the transformer should be star-connected with the neutral solidly grounded, to prevent damaging the insulation on the high-voltage winding.

Paper 49-143, recommended by the AIEE Substations Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20–24, 1949. Manuscript submitted March 22, 1949; made available for printing May 4, 1949.

E. A. Ricker is an Assistant Relay Engineer with The Hydro-Electric Power Commission of Ontario, Toronto, Ontario, Canada.

Another factor which must be considered before deciding whether the high-voltage circuit breaker can be eliminated by the substitution of automatic switches, is the class of service that is required by the customers supplied from the station. Whenever any grounding switch operates at a station, the power supply for all customers is interrupted. If a station has two or more transformer banks with as many high-voltage circuit breakers, customers are not affected by faults in one transformer. If there is only one transformer bank at the station, service is not improved by using a circuit breaker in place of automatic switches. If the station has two banks of transformers, both equipped with automatic switches, and if the distant circuit breakers have automatic reclosing equipment, the local station has only a short interruption for a fault in one transformer, because the supply of energy to the load is restored as soon as the distant circuit breaker has been reclosed automatically.

Mounting of Automatic Switches

The single-pole automatic grounding switch usually is mounted near the automatic air-break switch. The steel structure might be used to support both. The grounding switch often is used to produce a circuit to ground across the insulator stack at the hinge end of the air-break switch, an extra contact jaw for the grounding switch being attached to the top casting on the stack of insulators.

The spring operated mechanism for the single-pole grounding switch usually is mounted near the hinge of the grounding switch; linkages extend the manual control to the ground level. Energy is stored in the springs by moving a lever through an angle which ranges from 90 to 180 degrees.

The box containing the spring-operated mechanism for the automatic 3-pole air-break switch usually is mounted nearer the base of the steel structure. The box is heavier and contains stronger springs which must be capable of breaking three contacts rated at 600 amperes each, and of moving the linkages and arms of three switches to the open position. Springs of different strength are combined to give extra force at the beginning of the travel and to overcome excess friction caused by corrosion of the contacts or obstructions such as ice formation. Energy is stored in the springs by operating a lever manually. The second movement of the lever returns the arms of the air-break switch to the closed position. Motor-operated mechanisms are available for winding up the springs. When the mechanism has been latched in the loaded condition, the lever can be used for opening or closing the switch manually to energize or isolate

Table I. Installations of Automatic Grounding Switches

	Grounding Switch			Air Break Switch			Transformer Bank Data				Generation	Number of Troubles Reported		
Station	Date in Service	Years	Months	Number	Mechanism	Battery Voltage	Kva Capacity	Voltage (kv)	Frequency	Connection	Kva on Low Voltage Side	Ground Switch	Air Break	Control
Hanover	Oct. '30	13	3		None	125	6,750 8,000	110/6.6 110/14.2	25	*	10,000	2		4
St. Clair	Oct. '32	16	5		None	125	15,000	110/26.4	25	*	None	2		2
Ontario Paper (S)	Feb. '33	10	8	3	Motor	24	67,500	121/6.6	25	*	None	0	0	2
Matachewan	Apr. '34	14	11	1	Spring	24	4,500	121/27.7	25	*	None	0	0	0
Kirkland Lake	July '34 Apr. '39	12 7	3 11	2	Motor	125	28,500 28,500	117.5/12	25	**	4,250	1	1	1
Smooth Rock Falls	Aug. '34	14	8	2	Motor	24	39,000	121/6.6	25	*	None	0	0	3
Cornwall (H.S-S)	Aug. '34	11	7	1	Spring	24	20,000	105/6.6	60	*	None	0	0	0
Falconbridge	Nov. '35 (Oct. '45)	3	8	1	Spring	24	4,500	121/27.7	25	†	None	0	3	2
Larder Lake	Dec. '35 May '40	13 8	4 10	2	Spring	24	4,500 4,500	121/27.7	25	†	None	0	1	2
Timmins	Nov. '35 Oct. '37	4 2	2 3	2	Spring	125	4,500 4,500	121/27.7	25	†	None	0	0	0
Pamour	Dec. '36 July '40	12 8	3 8	2	Spring	(24) 110	4,500 4,500	121/27.7	25	†	None	0	0	0
Long Lac	June '38	10	9	1	Spring	24	9,000	110/44	60	†	None	0	0	0
Crow River	Sept. '39	8	5	1	Hand	48	3,750	66/23	60	**	3,500	0		0
Allanburg	Dec. '39	8	3	3	Motor	125	67,500	121/12	25	*	None	0	0	0
Norfolk	Jan. '40	9	2	1	Spring	(110) 125	8,000	110/28.4	25	*	None	0	0	1
Crystal Falls	Mar. '40	9	0	1	Spring	125	8,000	110/22	60	**	8,500	0	0	0
Coniston	Mar. '40 Feb. '49	9	0	1	Spring	125	8,000 8,000	110/22	60	**	11,400	1	0	0
Ramore	Apr. '40	8	11	1	Spring	24	4,500	121/27.7	25	†	None	0	0	0
Shiningtree	Apr. '41	7	11	1	Spring	24	4,500	121/27.7	25	†	None	0	1	0
Palmerston	Dec. '41	8	3	1	Spring	(110) 125	8,000	110/26.4	25	*	None	5	5	3
Oshawa	Feb. '42	4	4	1	Spring	125	15,000	110/44/4	60	‡	System	0	1	0
St. Catharines	May '42	6	11	1	Spring	24	8,000 8,000	110/28.4	25	*	None	0	0	0
Thorold	Oct. '42	6	6	4	Spring (then motor-'42)	(24) 125	15,000 ('29) 15,000 ('36) 15,000 ('37)	121/13.2	25	*	None	0	0	1
Fort William	Apr. '45	4	2	1	Hand	125	15,000	110/22	60	*	None	0	0	0
Barrie	June '46	1	11	1	Spring	125	15,000	110/44/4	60	‡	System	0	2	0
Schumacher	Feb. '47	2	0	1	Spring	125	9,000	110/12	25	*	System	0	0	0
Cornwall (CIL)	Feb. '47	2	1	1	Spring	125	15,000	110/13.2	60	*	None	0	0	0
Caledonia	Apr. '47	1	11	2	Spring	125	8,000	110/28.4	25	*	None	0	0	0
Kerr-Addison	June '48	0	9	2	Spring	125	15,000	121/27.7	25	*	None	0	0	0
Kingsville	July '48	0	8	1	Spring	125	8,000	110/28.4	25	*	None	0	0	0
Owen Sound	July '48	0	8	1	Spring	125	15,000	110/44/4	60	‡	System	2	0	1
Wallaceburg	Aug. '48	0	7	1	Spring	125	8,000	115/28.4	25	*	None	0	0	0
Stewartsville	Sept. '48 Sept. '48 Oct. '48	0 0 0	6 6 5	3	Motor	125	27,000 27,000 27,000	115/13.2	60	**	72,000	0	0	0
											Total	13	14	22

Note: The following symbols are used to denote connections of windings in transformer banks: * Star/delta, † Delta/delta, ** Grounded-star/delta. ‡ Star/grounded-star/delta.

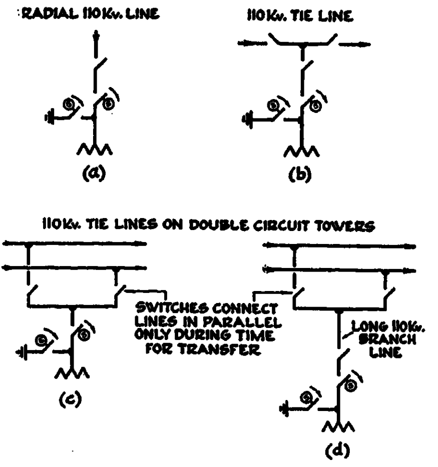

RADIAL 110 Kv. LINE
110 Kv. TIE LINE
(a)
(b)
110 Kv. TIE LINES ON DOUBLE CIRCUIT TOWERS
SWITCHES CONNECT LINES IN PARALLEL ONLY DURING TIME FOR TRANSFER
LONG 110 Kv. BRANCH LINE
(c)
(d)

Figure 2. Modifications which permit easy testing of automatic switches

a transformer while it is off load. Arcing horns are added to the switch to prevent burning the main contacts.

Both kinds of automatic switches have solenoids which are energized from the station battery for moving the trip-latch or moving a linkage through a neutral position. Both types of mechanism boxes contain pallet switches for opening circuits to trip coils and controlling other circuits to relays and lamps.

Since automatic switches are used at stations during the earlier stages of their growth, the supporting structure should be designed for the addition of a circuit breaker with the least modification. A 3-pole automatic air-break switch and a 3-pole disconnecting switch might be mounted on a steel structure above the place where the circuit breaker could be installed. The function of the extra disconnecting switch will be discussed later with the procedure for tests. The foundations for the steel structure should be well made, so that wind and frost will not force the structure out of line and cause excessive friction in the bearings of the switches. Wood poles are not recommended as supports since they are more likely to be heaved by the frost, causing misalignment.

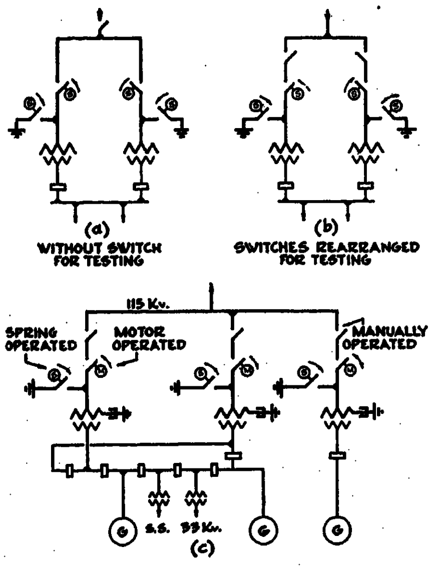


Figure 3. Stations with two and three banks of transformers

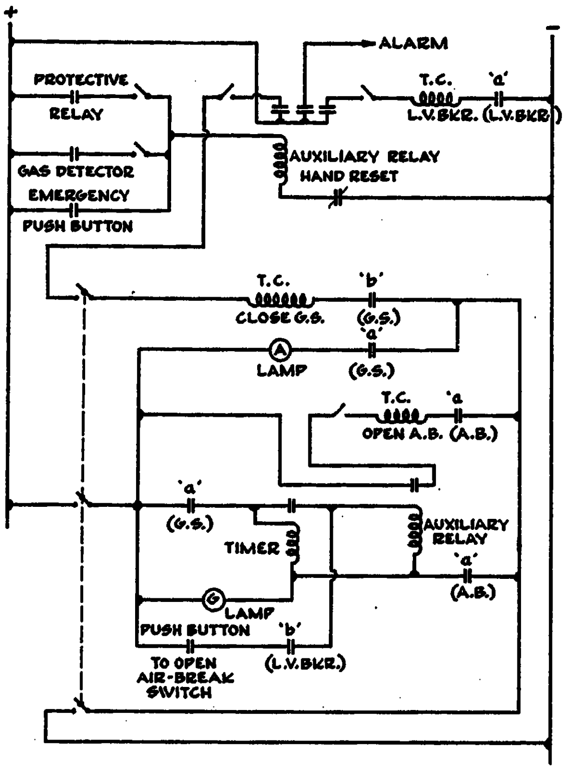


Figure 4. Diagram of control circuits for automatic switches

Arrangement of Equipment on Diagrams of Stations

In the simplest station having only one transformer, the low-voltage breaker might be omitted and fuses used. When the load includes synchronous motors or local generation, a circuit breaker is required to remove the source of fault current and to reduce interruptions to the high-voltage line by the operation of the overload protection on the transformer. When two or more banks are in parallel, low-voltage breakers are required to isolate the faulted bank. The breaker usually is tripped a short time before the artificial line-to-ground fault is applied to the high-voltage side of the faulted bank.

When two transformers are installed for reducing the duration of interruptions, the overload relays for each transformer should have settings which are greater than the combined loads of both banks, or else the second bank would be lost due to overload whenever the first bank develops an internal fault.

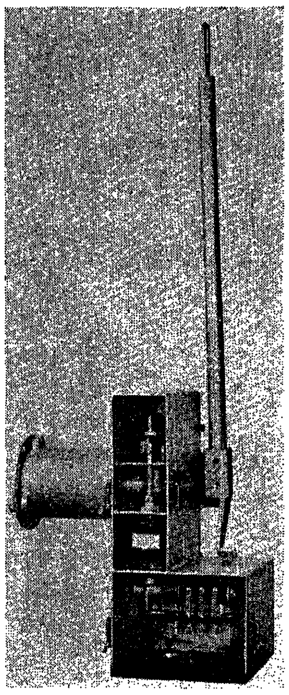


Figure 5. Automatic grounding switch with cover removed from mechanism box

Resistance of Station Ground

For successful operation of a scheme which includes automatic grounding switches, the protective equipment with the circuit breakers at the distant ends of the transmission line must include relays which are capable of detecting ground faults caused by automatic grounding switches. At some stations the resistance through ground might reduce the current to a value that cannot be detected by the relays at the distant station, as well as causing dangerous voltages in the local station. Considerable expense might be involved in installing numerous ground rods, in burying long bare-copper cables as counterpoise, or in extending a solid connection from the station to the nearest wet location. The alternatives are installation of high-voltage fuses or circuit breakers.

Testing Routine

Experience has shown that some installations of automatic switches are unreliable and require frequent testing. Since many of the troubles have developed in control circuits rather than in the switches themselves, the tests should be a complete check of the whole clearing process, from the closing of a contact at the panel to the final opening of the air-break switch. A push button usually is installed as a means of initiating the process. When

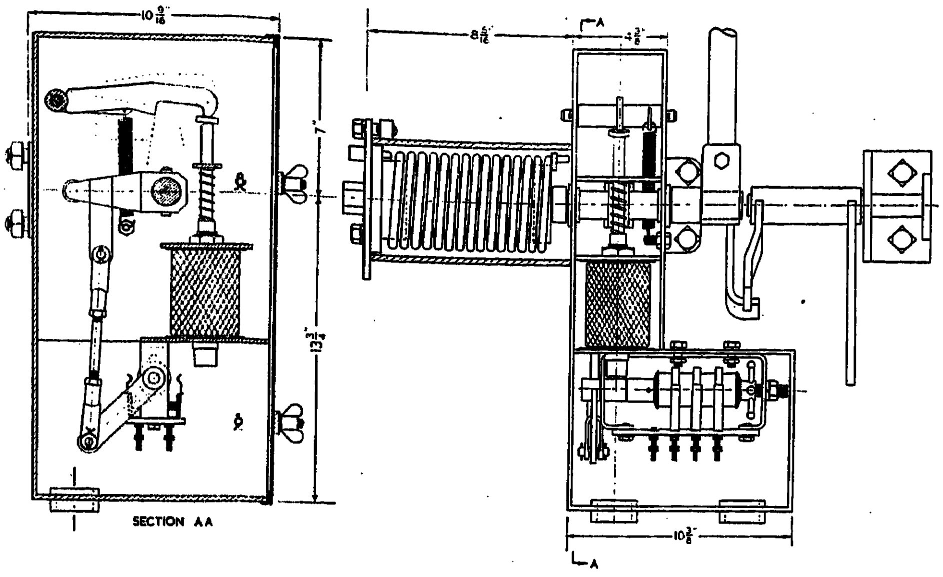


Figure 6. Assembly of mechanism for automatic grounding switch

low-voltage lines and switches are available for transferring the load of the small transformer station to another station, the test can be made during light load periods. When the station is in an isolated location, the tests are made during prearranged interruptions.

Tests on a station having a single transformer involve a complete interruption of the station. Load is removed from the transformer by tripping the low-voltage breaker. The exciting current of the transformer is broken by the air-break switch. A 3-pole disconnecting switch should be installed on the line side of the automatic air-break switch to permit the test being made without operating the breakers at the distant ends of the line. When the automatic switches have been isolated, the air-break switch should be reset to its normal operating position. Then the emergency push button on the control panel should be operated to trip the grounding switch and the air-break switch in their normal sequence. This test should be repeated once or twice to clean the contacts. The present practice is to make these tests monthly wherever possible. Whenever an automatic operation from one of the protective relays has occurred during a month, the routine monthly test can be omitted.

Gravity-Type Automatic Grounding Switch

The first installation of an automatic grounding switch was placed in service in 1930. A weight was added to one phase of an ordinary manually-operated line-grounding switch. The arm of the switch remained horizontal while in the normally open position. When the coil was energized, the latch was tripped, and the arm moved to the vertical position to ground the line. In its earliest form, the arm of the switch and the overbalancing counterweight were in a straight line. Experience showed that the angle of 180 degrees should be reduced by 30 or 45 degrees to produce a force which would keep the contact closed. Rebounding of the arm was reduced by using a resilient material in the jaw. Guides for the jaw and arcing horns were added.

There were only two installations of gravity-type switches, and no automatic air-break switch was used with them.

The mechanical design of the switch was the cause of failure to operate in four cases; troubles in control circuits prevented operation in six other cases. All failures except two occurred during routine tests.

Spring-Type Automatic Grounding Switch

This type of switch with its method of mounting already has been described. The stored energy is limited by the adjustments of the spring mechanism. Some looseness in the mechanism will allow a mechanical impact to break a coating of ice. Excessive spring pressures might cause damage at the jaw of the switch.

Troubles were recorded on 10 installations out of a total of 35, the auxiliary equipment being the cause of many of them. Nine cases of failure were caused by the mechanical parts not functioning properly; in 11 cases the operations of control circuits were incomplete or faulty. Only three of all these failures occurred during service; the others occurred during routine tests.

Spring-Type Automatic Air-Break Switches

At first, no automatic air-break switches were used. The list of troubles with these switches contains almost as many items as the list for the automatic grounding switches. For reliable operation the bearings should be noncorrodible, antifriction, and greaseless. Since the stored energy in the spring is large, there is greater pressure on the latch. The force from the tripping solenoid should be large enough to move the latch. The station should have at least a 48 volt battery and preferably a 125 volt battery. The higher voltage will avoid troubles caused by resistance in contacts and control wiring. The design should be chosen so that ice formation does not prevent operation to either the open or closed positions.

Among 32 installations, seven switches were the subject of 13 reports on troubles. Eight failures were caused by mechanical troubles and three, by control circuits. The cause of five other failures could not be determined. One failure occurred while the switch was in service; the others occurred during routine tests.

Motor-Operated Air-Break Switches

Due to slower operating speed of air-break switches with motor-operated mechanisms, the timer controlling the trip coil is often omitted. The motor usually is rated at one-quarter horsepower, and the full capacity of the station

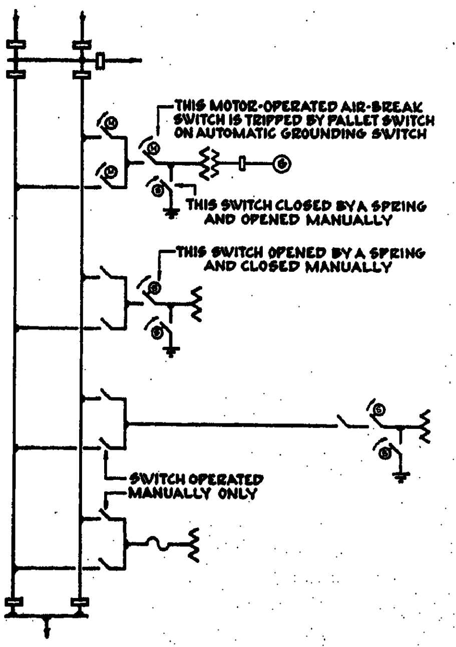

THIS MOTOR-OPERATED AIR-BREAK SWITCH IS TRIPPED BY PALLET SWITCH ON AUTOMATIC GROUNDING SWITCH
M
M
M
G
S
THIS SWITCH CLOSED BY A SPRING AND OPENED MANUALLY
THIS SWITCH OPENED BY A SPRING AND CLOSED MANUALLY
S
S
S
S
SWITCH OPERATED MANUALLY ONLY

Figure 7. Typical diagram of 110-kv system showing manual disconnecting switches which permit testing of automatic air-break switches

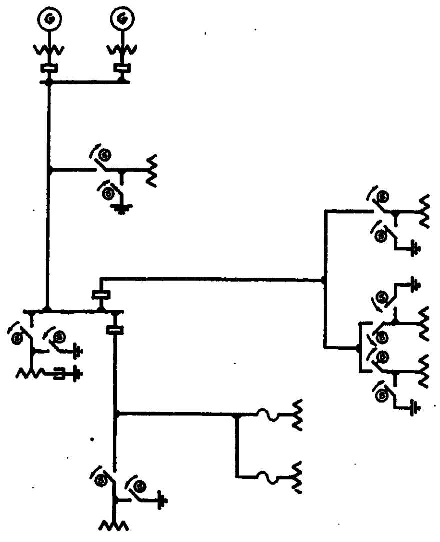


Figure 8. Growth by extension of branch circuits

battery is the stored energy available to operate it. In a total of over 16 installations, three of them were the subject of trouble reports.

One long interruption in service was caused by failure of lamps to indicate that a switch at a distant station had remained in the closed position. Other failures were found during routine tests. In one case the bevel gears had been broken by a person moving the lever for hand operation in the wrong direction. Another motor-driven switch had been operated by the controller to the open position, but it failed to close when the controller was turned to the close position. This failure might have been caused by ice or snow, as the temperature was −50 degrees Fahrenheit.

Experience with 39 Installations of Spring-Operated Grounding Switches

In Table I, 33 stations are listed in which automatic switches are used. The combined experience on the automatic grounding switches since the first one was installed in 1930, is a total of 260 switch years. The combined capacities of the transformers in these stations is 643,500 kva. In five of these stations with a total transformer capacity of 150,000 kva, circuit breakers were installed replacing the automatic grounding switches when fewer interruptions were required or when interconnections in the 110-kv network were changed. Construction is now proceeding on nine other stations with a total transformer capacity of 96,000 kva, including 11 automatic grounding switches and 13 air-break switches.

The range of transformer capacities, which are protected by a single grounding switch, extends from a minimum of 3,750 kva to a maximum of 67,500 kva. The three common bank capacities are 4,500, 8,000, and 15,000 kva. One station contains three banks with separate 110-kv air-break switches for each bank.

Transformer banks which have a rating of less than 3,000 kva usually are not equipped with bushing-type current transformers; therefore, protective relays cannot be used. High-voltage fuses serve as protective links and disconnecting devices for such small transformers.

Table I shows that most of the early stations were equipped with 24-volt storage batteries. Some of these have been replaced by others of higher voltage and ampere-hour capacity. The newer stations have batteries rated at 125 volts and at least 160 ampere-hours. In the northern districts, where low temperatures are common in the winter, smaller batteries were installed. These required less heated space in the station, but they were a sacrifice of reliability. The number of low-voltage circuit breakers in a station, the frequency of tripping and closing them, and the amount of other motor-operated station equipment will be factors in deciding the size of the station battery.

Typical Installations of Automatic Switches in Stations

Small transformers stations can be supplied from a radial line, or a single tie line, or from either of two tie lines supported on double-circuit towers. These connections are illustrated in Figure 1. When a single-circuit line is intended for use as either a tie line or a radial feeder, additional manually-operated disconnecting switches are installed, as illustrated in Figure 1(C). Figure 1(D) shows how a small station can be connected to either of two parallel tie lines. Except when the tapped station is being transferred from one circuit to the other, the high-voltage lines are never connected in parallel.

None of these diagrams shows any method for testing the automatic switches. Figure 2 shows how manually-operated switches can be added to the stations in Figure 1, so that the automatic switches can be given a complete test without tripping circuit breakers on the 110-kv supply lines.

A station with two banks of transformers is shown in Figure 3(A). One manually-operated disconnecting switch can be added to this diagram, as shown in Figure 3(B), so that the automatic switches can be tested easily.

The equipment which is not essential for this discussion has been omitted from all of these diagrams, such as disconnecting switches associated with high-voltage circuit breakers, line sectionalizing switches and transfer switches, coordinating gaps, lightning arresters, and line grounding switches that are closed before maintenance work is done on the 110-kv lines. A typical diagram of control circuits for automatic switches is shown in Figure 4. Figure 5 shows the mechanism and blade of the grounding switch in the tripped position. The assembly of the tripping mechanism is shown in Figure 6.

Typical System Connections

The relation of the small transformer stations and the tapped lines to the other parts of the system is shown in Figures 7 and 8. The latter shows growth by the extension of branch circuits with the addition of two circuit breakers in the

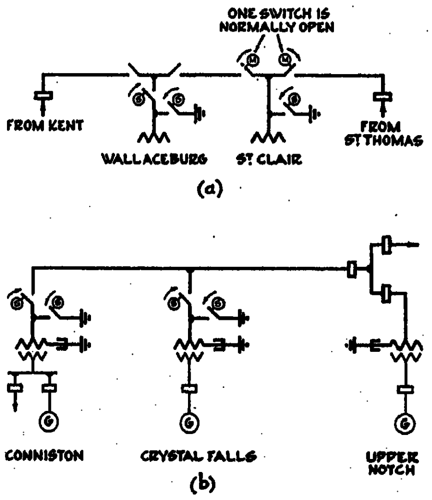


Figure 9. Tapped stations on loop lines in a 110-kv system

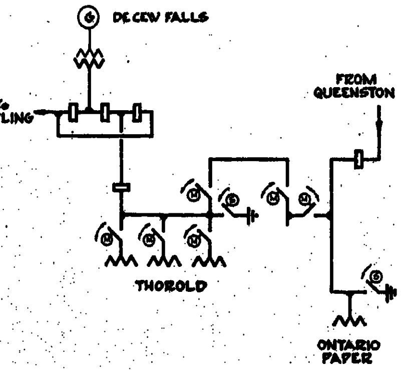


Figure 10. Motor-operated switches in tie lines

main branches. The main disadvantage is that faults which occur on the main supply line cause an interruption to stations on the branch lines as well. A total of seven stations is illustrated in Figure 8.

Figures 9 and 10 show loop lines which supply tapped stations. The loop in Figure 9(A) is closed only during an emergency or to transfer load, because the terminal stations are connected by other transmission lines. In Figure 9(B) no other lines connect these stations; all disconnecting switches remain in the closed position for normal operation. Under these conditions, the loop lines are actually single-circuit tie lines. Figure 10 shows two stations which are operated with tie lines closed through automatic motor-operated disconnecting switches.

Economy by Use of Automatic Disconnecting Switches

The infrequent occurrence of faults in transformers has resulted in the elimination of high-voltage circuit breakers for transformers from nearly all transformer stations, even though circuit breakers are used at the terminals of the lines. Transformer banks are connected in parallel, and automatic motor-operated disconnecting switches on the high-voltage side and low-voltage circuit breakers are used to isolate the faulted bank after the auxiliary tripping relay has operated to disconnect all sources of fault current.

Low-voltage windings on transformers usually are delta-connected, and zig-zag grounding reactors are used as sources of ground current to the feeders. Since faults in grounding reactors do not occur frequently, 3-phase circuit breakers between the terminals of the reactor and the station bus usually are omitted. An automatic motor-operated disconnecting switch is used in place of the circuit breaker to disconnect the zig-zag grounding reactor after all sources of fault current flowing into the low-voltage bus have been removed by operation of the circuit breakers.

Conclusions

Omitting high-voltage circuit breakers from a station gives a considerable saving in capital, but it is accompanied by more interruptions of the power supply to the customers. The reduced carrying charges permit lower rates for electric energy being charged to the customer. The lower cost of power encourages development of marginal projects, such as prospective mines in the northern part of Ontario, Canada. If the installation of the transformer station were delayed until a larger initial load were available, the project might not be undertaken. Such designs of small transformer stations are admirably suited to scattered mining developments and to large rural areas containing hamlets and villages but no large industrial centers.

Although a circuit breaker has been eliminated by two automatic switches, these switches should be put through the same testing routine as circuit breakers. They should be exercised by tripping them once a month to clean the contacts and to check the control wiring for open or short circuits. The stored-energy source for operating the automatic switches should be effective through the whole travel of the switch with a safe margin.

Energy stored in springs appears to be sufficient for single-pole grounding switches. The larger amount of stored energy in the station batteries and motor-operated mechanisms would be preferred for opening the 3-pole air-break switches.

Discussion

W. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): Differential relaying continues to be the basic method for the protection of large power transformers. A survey conducted by the AIEE Relay Committee[1] showed that some companies apply such protection with transformers as small as 1,000 kva, most of them using it with transformer banks rated 5,000 kva and up. However, the cost of the higher voltage circuit breakers and the remarkable improvements in the reliability of modern power transformers have led to the substantial application of local differential relaying, either with automatic line grounding switches or with pilot wire or carrier current transfer tripping of distant line circuit breakers. In this survey, about 75 per cent of the companies reported some installations of one scheme or the other, over half of these being of the transfer tripping variety.

A subsequent survey by the AIEE Relay Committee was concentrated on the protection of stations without high-voltage switching.[2] This detailed analysis covered some 100 installations, divided somewhat equally between transfer tripping and line-grounding switch schemes. This second survey indicated general satisfaction with both the transfer tripping and line-grounding switch methods, but brought out definite limitations of each. Therefore, this detailed analysis of application and performance presented by the author is most timely and should assist in a more accurate evaluation of the proper field for automatic grounding switches. It would be interesting to learn what consideration the author gave to the use of transfer tripping schemes, and the reasons for preferring the automatic grounding switch method.

Speed of operation of the automatic grounding switch is rather important, since its operating time plus that of the ground relays at remote stations adds directly to the total time of fault clearing, when comparing this scheme with either a local circuit breaker or transfer tripping arrangement. The time of operation of the type of spring-operated grounding switches described by the author would be of interest (from energizing the trip coil until the contacts touch).

In view of the relative infrequency of faults in modern power transformers, it seems unfortunate that the grounding switch scheme requires interrupting the load for test purposes, in the case of single transformer installations, as often as once a month. Consideration might be given to additional disconnecting and by-pass facilities to avoid interruptions during tests, but it is doubtful if this added complication and expense would be justified.

Increased speed of operation of automatic grounding switches and improved reliability of the entire scheme seems highly desirable. Accordingly, designs based on a straight line motion might be preferable for line grounding switches.

References

1. Relay Protection of Power Transformers, AIEE Relay Subcommittee. *AIEE Transactions*, volume 66, 1947, pages 911–16.

2. Protection of Stations without High-Voltage Switching, AIEE Committee Report. *AIEE Transactions*, volume 68, part I, 1949, pages 226–32.

E. A. Ricker: The discussion of the automatic grounding switch would not have been complete without references to surveys by the AIEE Relay Committee and reports which were published in AIEE Transactions for 1948 and 1949. For this favor, grateful acknowledgment is given to W. R. Brownlee.

Although the second general survey by the Relay Committee showed that both the transfer tripping and line grounding switch methods of opening circuit breakers in remote locations will perform correctly in the majority of operations, automatic grounding switches have been preferred by the Hydro-Electric Power Commission for several reasons. Carrier equipment for transferring tripping signals has been shown by tests to be not always effective and reliable. Carrier transmitters and receivers require more maintenance and adjustment by specially-trained personnel. The cost of a complete installation of carrier equipment is higher than the cost of installing additional supports and switches associated with the automatic grounding device. No power is required by the automatic switch during the standby condition.

The time delay in operation of the grounding switch was made small by using light materials in the blade to reduce its inertia and co-ordinating designs for the spring, trip latch, and jaw. The amount of energy stored in the spring can be adjusted. It is limited by the ability of the jaw to withstand the impact of the blade without damage or rebounding. The force caused by current in the tripping solenoid must be several times greater than the opposite force caused by the spring on the trip latch even after months without an operation. Automatic grounding switches are normally adjusted so that the time delay is less than 0.5 second from the time the trip coil is energized until the blade makes contact with the jaw of the switch.

To all employees of the Hydro-Electric Power Commission of Ontario who gave assistance by providing opportunities to review records of operating conditions, who passed on their knowledge of early developments, who gave suggestions on the contents of this paper, and to those who prepared the drawings, the first copies of the text and checked the material, the author acknowledges a debt of gratitude.

A New 2-Signal Synchronous Supervisory Control System

W. A. DERR
MEMBER AIEE

THERE are two fundamental methods of selecting a desired unit in a supervisory control system. The one method, known as direct selection, is based on applying signals which are different for each unit. The other method, known as synchronous selection, is based on having both the controlling and controlled station equipment connect to each unit in sequence until the desired unit is reached.

Two different types of direct selection systems have been manufactured. One of these types uses a different code of pulses to select each desired unit, with the station receiving the selection pulses returning a similar code of pulses to verify the correctness of selection.[1] The other type also employs a different code of pulses to select each desired unit, but the coding is made so that only the correct unit can be selected. With this latter type it is necessary only to verify that a selection has been made and not that a particular selection has been made.[2]

In a synchronous system there is no specific code or signal associated with each individual unit which must be selected. Instead, signals are transmitted which cause the controlling and controlled station equipment to step through each unit in sequence and in synchronism. The equipment stops when the desired unit is reached. A new system based on synchronous selection will be described in this paper.

All supervisory control systems require signal responsive elements at the controlling station and at the controlled station. Direct selection systems are available which require only a single type of signal and hence a single signal responsive element at each location.[1,2] However, a minimum of two different types of signals must be employed to provide a reliable synchronous system.

When different signal responsive elements are employed at the controlling station and at the controlled station for each station controlled, it is known as single station operation. This is the basic arrangement for which either direct selection or synchronous selection systems are applicable.

The control of several stations employing common signaling means at the controlled stations as well as at the controlling station is known as multistation operation. Direct selection systems using a single signal responsive element at each location are the only systems which are practical to use for multistation operation. Multistation operation is advantageous when only a single pair of wires or a single power-line carrier channel is available for the control of several stations since only one type of signal must be provided.

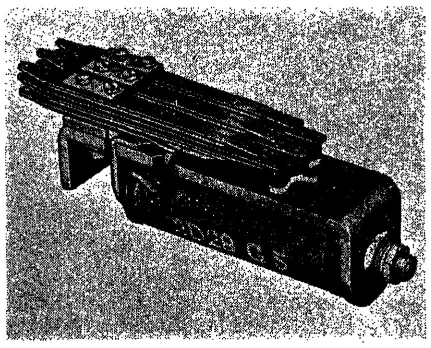


Figure 1. Type of relay employed in new system

Since a reliable synchronous system must employ a minimum of two different signals, multistation operation is not practical.

The synchronous principle provides for rapid checking of the position of devices supervised. A synchronous system also is advantageous where functions must be performed consecutively, since it is only necessary to step to each unit in turn, thus reducing the operating time.

Principle of Operation

This new 2-signal synchronous system employs two different types of signals from both the controlling and controlled stations. The design is such that the same two signals can be used from the controlling and controlled stations. Thus, for operation over telephone line wires, positive and negative battery potential can be applied to the telephone line wires to obtain the two different signals from each station. Signals of two different frequencies can be used from each station as the signaling means for operation over power-line carrier or microwave radio channels.

The selection of units for control is based on the principle of stepping through each unit in turn until the desired unit is reached. A single pulse is used for each step, with all stepping pulses originating from the controlling station. One of the two types of signals employed at the controlling station is used for the odd-numbered steps with the other type of signal being used for the even-numbered steps. Thus the same signal never repeats itself for consecutive steps. This prevents the possibility of the controlling and controlled stations ever operating out of step.

The controlling station waits for a supervision signal from the controlled station after each stepping pulse. The supervision signal consists of a single pulse of either of the two types of signals used at the controlled station. Thus, the

---

Paper 49-144, recommended by the AIEE Substations Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20–24, 1949. Manuscript submitted March 21, 1949; made available for printing April 28, 1949.

W. A. DERR is Supervisory Control Supervising Engineer with the Westinghouse Electric Corporation, East Pittsburgh, Pa.

Table I. Indication of Signal Pulses

Controlling Station Signal Number				Controlled Station Signal Number
1	⟶	Step No. 1		
		Supervision	⟵	1
2	⟶	Step No. 2		
		Supervision	⟵	1
1	⟶	Step No. 3		
		Supervision	⟵	1
2	⟶	Step No. 4		
		Supervision	⟵	1
1	⟶	Step No. 5		
		Supervision	⟵	1
2	⟶	Step No. 6		
		Supervision	⟵	2
1	⟶	Step No. 7		
		Supervision	⟵	2
2	⟶	Step No. 8		
		Supervision	⟵	2
1	⟶	Step No. 9		
		Supervision	⟵	2
2	⟶	Step No. 10		
		Supervision	⟵	2

supervision pulse verifies or changes the lamp indication for the particular unit and also provides the signal that the stepping can continue.

A better picture of the stepping arrangement just described can perhaps be obtained by referring to Table I. This table indicates the signal pulses which would be employed for a supervisory control system providing control and supervision of ten units (for example, circuit breakers), with the first five units being in one position (open) and the last five units being in the other position (closed).

When the stepping is started by the operation of a selection key, the stepping stops when the unit associated with that particular key is reached. Such selection of a desired unit is referred to usually as point selection. When a unit or point has been thus selected, it is possible to initiate the control signals. Signal number 2 is used for both control functions which can be performed, with the number of pulses of this signal determining the control function. For example, two short pulses constitute the control code for closing a circuit breaker and three short pulses constitute the control code for tripping a circuit breaker.

A long pulse of signal number 1 is transmitted from the controlling station to prepare for stepping from the normal at rest condition or for stepping after a unit has been selected. The use of a long pulse eliminates the possibility of unwanted stepping from any extraneous disturbing voltages to which the signal receiver elements may be subjected.

A long pulse of signal number 2 is transmitted from the controlled station if any supervised device changes position automatically. Receipt of this signal at the controlling station results in a long pulse of signal number 1 being transmitted from the controlling station to initiate stepping. When stepping is thus initiated due to an automatic operation at the controlled station, and position of all devices supervised is checked in sequence.

Following is a list of the different signaling means employed in the operation of this system, as just described:

From Controlling Station	
Start	Long pulse, signal number 1
Odd steps	Short pulse, signal number 1
Even steps	Short pulse, signal number 2
Close control	Two short pulses, signal number 2
Trip control	Three short pulses, signal number 2
From Controlled Station	
Alarm	Long pulse, signal number 2
Close supervision	Short pulse, signal number 1
Trip supervision	Short pulse, signal number 2

Operating Features

The supervisory control system described in this paper is an all-relay system employing telephone-type relays as shown in Figure 1.

An escutcheon or key and lamp plate as shown in Figure 2 is used at the controlling location for any of the following functions which may be performed:

1. Control and supervision of any device. (For example, a circuit breaker.)
2. Supervision and alarm of any device. (For example, indication of high transformer temperature.)
3. Telemetering of any quantity. (For example, telemetering of kilowatt load.)
4. Control of any device with simultaneous telemetering of quantity controlled. (For example, control of tap changer with simultaneous telemetering of tap changer position.)

The key at the bottom of the escutcheon shown in Figure 2 is a pull-type key which is operated to initiate stepping of the equipment. This key is called a selection key since it provides the means of selecting the desired unit.

The white lamp above the selection key is the selection lamp. When the equipment steps through each unit, the white selection lamps associated with each unit, light in turn. If the stepping is initiated by operating a selection key the equipment stops when it has stepped to the unit associated with the selection key operated. The white selection lamp for that unit then remains lighted. The desired operation on that unit can then be performed after which the pull-type selection key should be restored. When the key is thus restored, the equipment proceeds to step through the rest of the units.

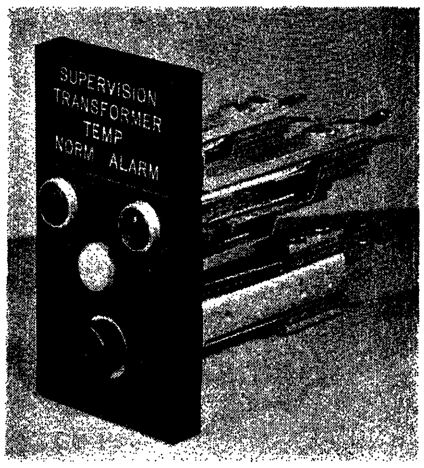


Figure 2. Escutcheon used in new system for each unit controlled and supervised

The two upper lamps on the escutcheon shown in Figure 2 are for indicating the position of the device supervised. If the device supervised is a circuit breaker, these would of course be red and green lamps to indicate whether the circuit breaker was closed or open. If the escutcheon is used for a telemetered indication or for control and simultaneous telemetering, these position indicating lamps are omitted.

To indicate the automatic change of position of a device, the associated position indicating lamp on the escutcheon is flashed. For example, if a circuit breaker is tripped by a protective relay, the green lamp on the escutcheon for that circuit breaker is flashed in addition to an alarm bell being sounded. If a circuit breaker which is equipped with an automatic recloser, trips and is reclosed successfully, the green lamp on the associated escutcheon at the controlling station is first momentarily flashed, after which the red lamp is flashed.

There are five common push-type keys used at the controlling station in the operation of this system. Two of these keys are the control keys which are operated after a unit is selected to change the position of the device controlled. For example, considering a circuit breaker, one of these keys would be operated after selection to close the circuit breaker. The other key would be operated after selection to trip the circuit breaker.

The third common key is designated as the reset key. Operation of this key causes the system to step through all of the points. Thus, it performs the function of checking the position of all of the devices supervised since the position indicating lamps are checked on every step. Operation of this key also restores the equipment to normal in the event that it is off normal for any reason.

The fourth common key is operated to silence the audible alarm which is initiated when any supervised device changes position other than by operation of the supervisory control equipment.

The fifth common key is operated to cause any position indicating lamp or lamps which may be flashing as a result of automatic change of position of the devices supervised to be lighted continuously.

The system is suitable for use with any of the commonly employed types of telemetering equipment. Voltage or current magnitude types as well as impulse types can be used when operation is over telephone line wires. For operation over power-line carrier or microwave radio channels, impulse-type telemetering equipment must be used. It is possible to have the telemetering circuits arranged so that any telemetered indication can be continuously received while the equipment is at rest, without interfering with any other operations. This can be done regardless of the channel means employed.

Communication can be provided readily over the same pair of wires used for operation of the supervisory control equipment. It is customary to have the telephone ringing function provided by the supervisory control equipment.

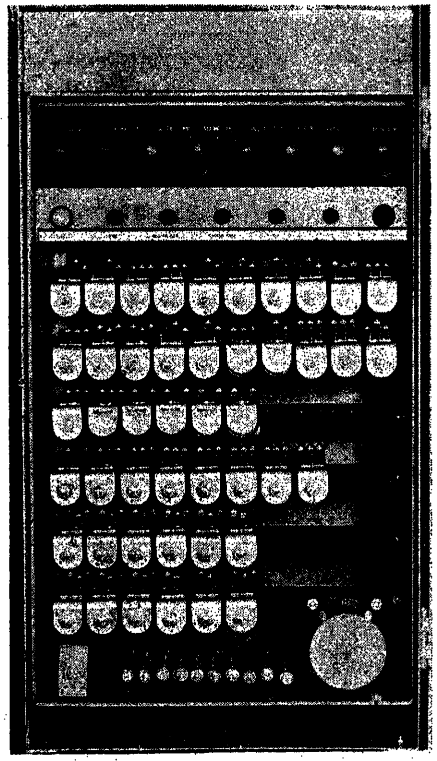


Figure 3 (A). Controlling station end of 2-signal model

When several units are to be controlled or several telemetered readings taken, it is not necessary to step through all of the units for each selection to be made. The selection keys of all of the desired units may be operated one after another. When this is done the equipment will step as far as the unit associated with the first selection key. Then when this key is restored the equipment will only step to the next unit for which the selection key had been operated. This arrangement is particularly advantageous when there are a number of telemetered readings which must be checked at frequent intervals, since a large number of readings can be obtained very rapidly.

Whenever any supervised device changes position automatically at the controlled station, all points are stepped through with the lamp indications being changed for the unit or units which changed position. If any supervised unit changes position after it has been stepped past, the equipment immediately steps through all of the points a second time as soon as the first cycle of stepping has been completed. Thus, no changes of indication will be missed.

Approximately ten points are stepped through in a second. Thus, if fifty circuit breakers are controlled and supervised, the position of all of the circuit breakers can be checked in five seconds.

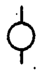
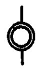
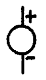
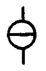
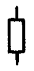
Figure 3 shows the controlling and controlled station equipment of a model of this new supervisory control system which is being used in microwave operation on the property of the Pennsylvania Electric Company. This model is designed for the control and supervision of six circuit breakers and two selective telemetered indications.

Detailed Description of Operation

To describe the operation of this new system, partial schematic diagrams only showing the particular elements involved in the operation being described are presented. Only those circuits necessary to obtain an understanding of the circuit arrangements which can be used to provide for operation on the principle of operation described previously are presented.

On the diagrams to be described, only those coil circuits are shown which were deemed necessary. In some cases only the contacts of relays are shown. For such cases, the time at which the coils of these relays would be energized and de-energized is described.

Following is a description of the elements shown on the partial schematic diagrams of Figures 4 to 10 inclusive:

	Contact of relay which closes when relay is energized or operated.
	Contact of relay which is closed when relay is de-energized or released.
	Fast type telephone relay.
	Telephone relay with copper sleeve around core to delay release when it is de-energized.
	Polarized telephone type relay.
	One coil of two coil telephone relay. Relay will operate when either coil is energized.
	Resistor.

CHANNEL ELEMENTS

Figure 4 indicates how signals are applied and received on the basis of positive and negative d-c pulses being used for signaling. It will be noted from this figure that a single polarized relay (LR) is connected to the line wires at both the controlling and controlled stations through break contacts of all of the relays which can apply potential to the line wires.

Figure 5 indicates how signals are ap-

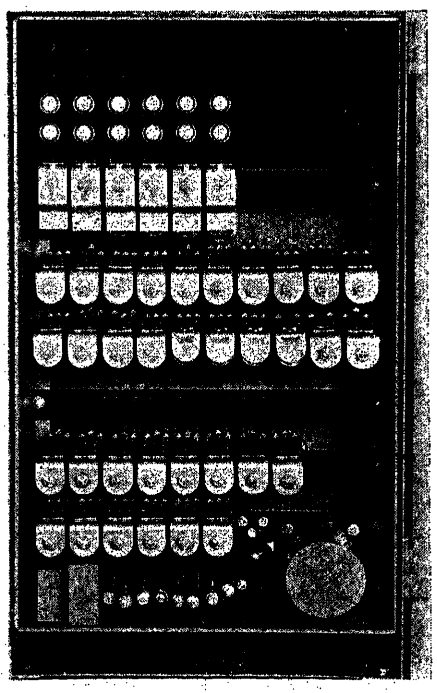


Figure 3 (B). Controlled station end of 2-signal model

plied and received on the basis of two different frequencies being used for signaling, instead of positive and negative d-c pulses with the same two frequencies being used from each station. This necessitates the use of break contacts of the signaling relays in the receiver relay circuits. If two different frequencies are used from each station these break contacts are not required.

With reference to both Figures 4 and 5, the relay contacts shown in the signaling circuit at the controlling station are operated to transmit signals as indicated below:

Relay 1—Transmits odd stepping pulses to controlled station and steps odd point relays at controlling station.

Relay 2—Transmits even stepping pulses to controlled station and steps even point relays at controlling station.

Relay 3—Transmits starting pulse.

Relay 4—Transmits operation control pulses. ("Trip" and "close" pulses in the case of circuit breakers.)

At the controlled station, relay 1 operates after each step to send the supervision pulse. Relay 1 also operates to transmit the alarm signal. Relay 2 determines which of the two supervision pulses is transmitted. In the description which follows, these will concern the indication of circuit breaker positions and will be referred to as "close" and "trip" supervision pulses. When relay 2 is energized a close supervision pulse is transmitted and when relay 2 is de-energized a trip supervision pulse is transmitted.

Stepping Circuit

Figure 6 shows the essentials for stepping at the controlling station. Figure 7 shows the essentials for stepping at the controlled station. The stepping pulses are all transmitted from the controlling station.

With reference to Figure 6, the relays shown which were not mentioned previously, perform the following functions at the controlling station:

Relay 5—Energized when stepping stops and a point is selected.

Relay 6—Energized to start stepping pulses and released at end of first step.

Relay 7—Operates from contact LR1 cn receipt of "close" supervision.

Relay 8—Operates in series with odd-numbered point selection relays.

Relay 10—Operates in series with even-numbered point selection relay

Relays A1, A2, and so forth—Point selection relays.

With reference to Figure 7, the relays shown which were not mentioned, previously, perform the following functions at the controlled station:

Relay 3—Energized by starting pulse which precedes stepping pulses and remains energized until stepping pulses from controlling station stop.

Relay 4—Operates in series with odd-numbered point selection relays.

Relay 5—Operates in series with even-numbered point selection relays.

Relay 6—Operated by starting pulse which precedes stepping pulses. Releases at end of first stepping pulse.

Relays F1, F2, and so forth—Point selection relays.

Relays G1, G2, and so forth—Point indication relays. Each *G* relay is operated if the associated circuit breaker is closed.

Stepping is started from the controlling station as a result of operation of a point selection key, operation of the reset key or receipt of an alarm signal from the controlled station resulting from the automatic operation of a supervised device.

To start the stepping, relay 6 is energized at the controlling station. A contact of relay 6 completes a circuit for energizing relay 1. Contacts of relay 1 transmit the first stepping pulse to the controlled station as shown in Figures 4 and 5. Another contact of relay 1 energizes A1 and 9 in series at the controlling station. When relay 9 is thus energized it opens the energizing circuit of relay 1. When relay 1 releases it removes the first stepping pulse from the channel and opens the energizing circuit for relays A1 and 9. However, these two relays remain energized through a make contact of relay A1 and break contacts of relays 2 and 10 in parallel. The controlling station then waits for the first supervision pulse from the controlled station. It is to be noted that relay 6 releases at the controlling station after the first stepping pulse is transmitted.

When the first stepping pulse is received at the controlled station, relay LR is energized and contact LR1 closes. Relays F1 and 4 are energized then in series. When relay 4 is energized it completes a circuit to energize relay 2 providing relay G1 is energized as a result of the associated circuit breaker being closed. Assume that relay 2 is energized as a result of relay G1 being energized.

When the first stepping pulse is removed from the channel, contact LR1 opens at the controlled station, which results in relay 1 being energized in series with relays F1 and 4. When relay 1 is energized the supervision pulse is transmitted to the controlling station. Which of the two supervision signals is transmitted is dependent on whether or not relay 2 is energized at the time. The energizing of relay 1 releases relay 6, which allows relay F1 to remain energized but releases relays 1 and 4. The supervision pulse is removed from the channel when relay 1 releases.

Receipt of the close supervision pulse at the controlling station results in relay LR being energized, contact LR1 in turn energizing relay 7. The energizing of relay 7 results in the lamp indication on point number 1 being changed to red if it was not already red. The red and green position indicating lamp circuits will be discussed later.

When the supervision pulse is removed from the channel, relay 2 is energized in series with relay 7. Contacts of relay 2 transmit the second stepping pulse to the controlled station. Another contact of relay 2 energizes relays A2 and 10 in series. At the controlling station, when relay 10 is thus energized it de-energizes relays A1 and 9 and also opens the energizing circuit of relay 2. When relay 2 releases it removes the second stepping pulse from the channel and opens the energizing circuit for relays A2 and 10. However, these two relays remain energized through a make contact of relay A2 and break contacts of relays 1 and 9 in parallel. The controlling station then waits for the second supervision pulse from the controlled station.

At the controlled station, relay LR is energized and contact LR2 closes when the second stepping pulse is received. Relays F2 and 5 are then energized in series. When relay 5 is energized it completes a circuit to energize relay 2 providing relay G2 is energized as a result of the associated circuit breaker being closed. Assume that relay G2 is not energized, thus leaving relay 2 de-energized.

When the second stepping pulse is removed from the channel, contact LR2 opens at the controlled station, which results in relay 1 being energized in series with relays F2 and 5. When relay 1 is energized, the second supervision pulse is transmitted to the controlling station. The energizing of relay 1 also releases relay F1. When relay F1 releases, relays 1 and 5 are de-energized but relay F2 remains energized.

Receipt of the trip supervision pulse at the controlling station results in relay LR being energized, contact LR2 then energizing relay 8. The energizing of relay 8 results in the lamp indication on point number 2 being changed to green if it was not already green.

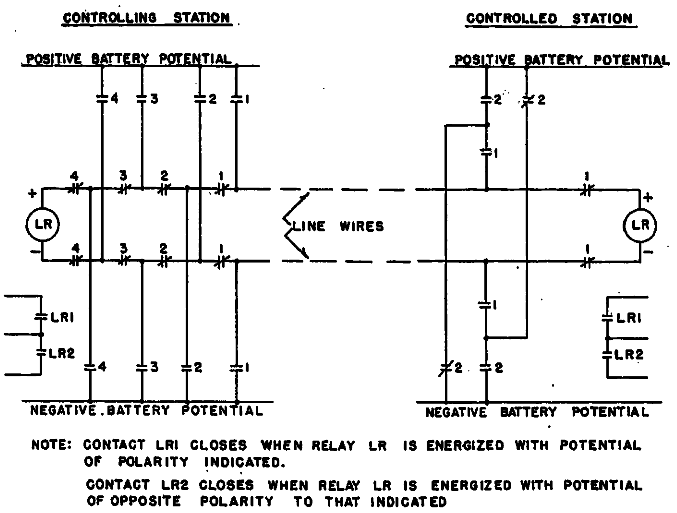

CONTROLLING STATION
CONTROLLED STATION
POSITIVE BATTERY POTENTIAL
LINE WIRES
LR
LR1
LR2
NEGATIVE BATTERY POTENTIAL
NOTE: CONTACT LR1 CLOSES WHEN RELAY LR IS ENERGIZED WITH POTENTIAL OF POLARITY INDICATED.
CONTACT LR2 CLOSES WHEN RELAY LR IS ENERGIZED WITH POTENTIAL OF OPPOSITE POLARITY TO THAT INDICATED

Figure 4. Diagram of essential transmitting and receiving elements using d-c pulses for signaling

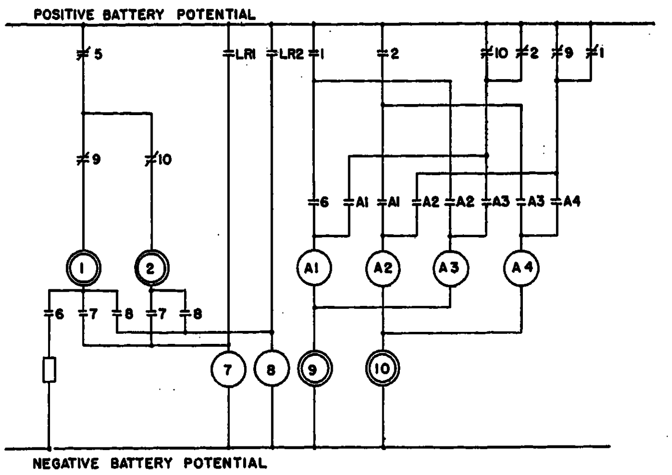


Figure 6. Fundamental circuits for controlling station stepping and supervision

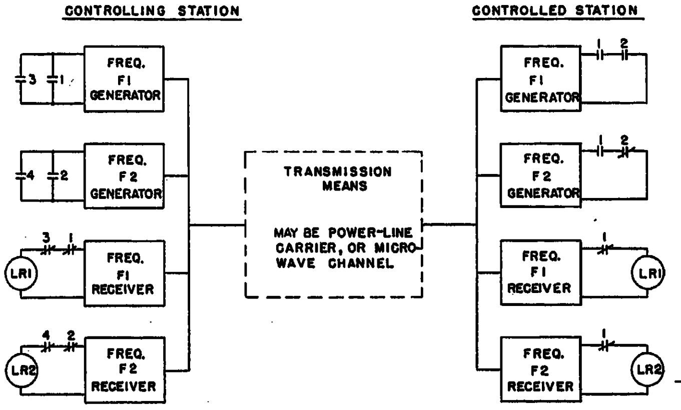


Figure 5. Diagram of essential transmitting and receiving elements using two different frequency pulses for signaling

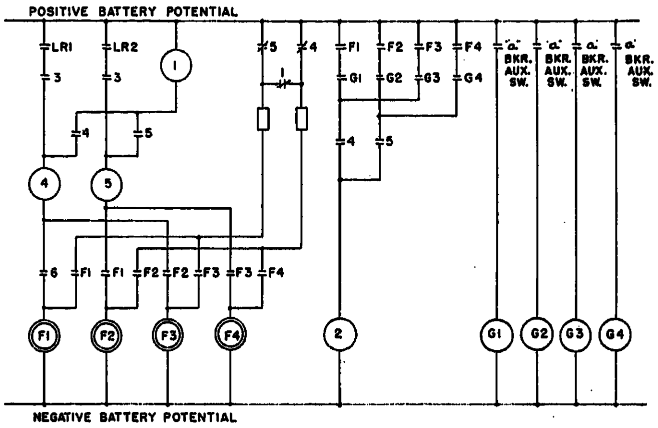


Figure 7. Fundamental circuits for controlled station stepping and supervision

When the supervision pulse is removed from the channel, relay 1 is energized in series with relay 8. Contacts of relay 1 transmit the third stepping pulse to the controlled station.

In a similar manner, stepping is continued through all of the points. If the stepping is started by the operation of a selection key, relay 5 is energized when the A relay associated with the selection key operated is energized. Thus, the stepping stops with the proper A relay energized at the controlling station and the proper F relay energized at the controlled station.

If the stepping is started from an alarm signal or the reset key, the stepping continues through all of the points.

Control Sending and Receiving

Figure 8 shows the essentials for transmitting the control pulses. After a unit has been selected by operation of a selection key, the desired control code is initiated at the controlling station by operation of the close key or the trip key. The close code consists of two pulses and and trip code consists of three pulses. Figure 9 shows the essentials for receiving the control pulses at the controlled station.

With reference to Figure 8, the relays indicated which were not mentioned previously, perform the following functions at the controlling station:

Relays 11, 12 and 13—Control counting chain relays. Count trip and close pulses.

Relay 14—Operates in conjunction with relay 4 to transmit control pulses.

With reference to Figure 9, the relays which were not mentioned previously perform the following functions at the controlled station:

Relay 7—Operates from contact LR2 on receipt of control pulses. Energizing circuit not completed until after point selection.

Relay 8—Slow-to-release relay associated with control counting chain. Remains energized during receipt of trip or close pulses and releases when pulsing stops.

Relay 9—Slow-to-release relay which maintains control circuit closed long enough to allow circuit breaker to operate.

Relays 11, 12, and 13—Control counting chain relays. Count trip and close pulses.

Relays C1, C2, and so forth—Close interposing relays which complete circuits to circuit breaker closing relays.

Relays T1, T2, and so forth—Trip interposing relays which complete circuits to circuit breaker trip coils.

If the trip key is operated at the controlling station after a point is selected, relay 11 is energized. Relay 4 is then energized through a make contact of relay 11 and relay 4 in turn energizes relay 14. A break contact of relay 14 then opens the coil circuit of relay 4. Relays 4 and 14 then continue to operate each other as long as any of the relays 11, 12, or 13 are energized.

With reference to Figure 4, it will be noted that a pulse is transmitted to the controlled station each time relay 4 is energized at the controlling station.

On the first operation of relay 4 after the trip key is operated, relay 12 is energized in series with relay 11. When relay 4 releases, relay 11 releases but relay 12 is held energized through its other coil. The next operation of relay 4 energizes relay 13 in series with relay 12. When relay 4 again releases, relay 12 releases but relay 13 is held energized through its other coil. The third operation of relay 4 de-energizes relay 13. Then when relay 4 releases, relay 14 is also de-energized and the circuit returns to its normal position.

The operation is similar when the close key is operated except that relay 12 is energized rather than relay 11. This results in a code of only two pulses being transmitted.

At the controlled station, receipt of the control pulses results in relay 7 being energized and de-energized from contact LR2. With reference to Figure 9, the first operation of relay 7 energizes relay 8. A contact of relay 8 in turn energizes relay 9. When relay 7 releases, relay 11 is energized. Relay 8 is a slow-to-release relay which will not release until the control pulses have stopped.

The second operation of relay 7 energizes relay 12 in series with relay 11. The second release of relay 7 de-energizes relay 11 but relay 12 is held energized through its other coil. Similarly, on the third control pulse relay 13 is energized in series with relay 12 and relay 12 is de-energized when relay 7 again releases.

When the pulsing stops relay 8 releases, completing a circuit to the T or the C relay of the point previously selected depending on whether relay 12 or 13 is energized. Relay 12 or 13 releases when slow-to-release relay 9 releases.

The supervision pulse is then transmitted to the controlling station in a manner similar to that described previously. The circuits for transmitting the supervision pulse at the end of a control operation are not shown.

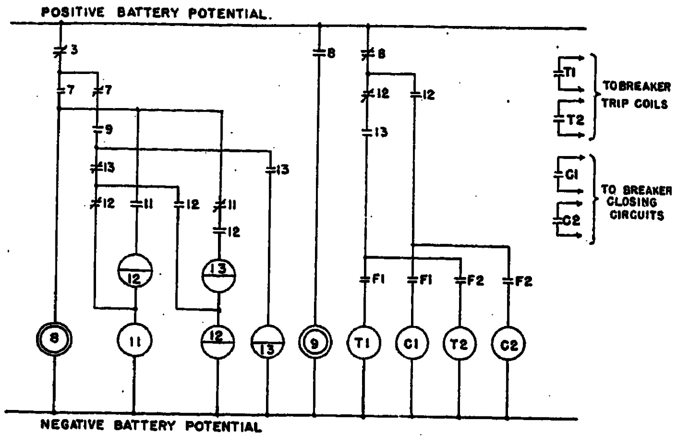


Figure 9. Fundamental circuits for receiving control pulses at controlled station

CHANGE OF INDICATION

Figure 10 indicates how the change of indication of the red and green circuit breaker indicating lamps is accomplished at the controlling station when supervision pulses are received from the controlled station. Relays B1, B2, and so forth are the lamp indication relays, the red lamps being lighted through make contacts of these relays and the green lamps being lighted through break contacts of these relays.

As described previously, relay 7 is energized when a close supervision pulse is received and relay 8 is energized when a trip supervision pulse is received. The B relays are connected to make contacts of relays 7 and 8 through contacts of the associated A point selection relays as shown in Figure 10. The contact of relay 7 completes a circuit to energize the particular B relay. When a B relay is thus energized it seals itself in through a resistor. The contact of relay 8 shunts the particular B relay and causes it to release.

Conclusions

The 2-signal synchronous supervisory control system described is well adapted for the control of any size single station on telephone line wires and also is well adapted for operation on microwave radio channels.

The use of positive and negative d-c signals on either cable or open construction telephone line wires is preferred over the use of signals of two different frequencies for the following reasons.

1. Frequency signaling results in greater susceptibility to interference from electrical disturbances as well as from other services in adjacent or nearby telephone line wires.

2. Frequency signaling is more likely to interfere with services in adjacent or nearby telephone line wires

3. Frequency signaling necessitates the use of expendable parts such as tubes.

4. For operation over long distances of cable wires, repeaters may be necessary with frequency signaling. D-c signaling has been employed over cable wire lengths in excess of 100 miles.

It is to be noted that open construction telephone line wires can be adequately protected for d-c signaling.[3]

The fact that positive and negative pulses can be employed in the system

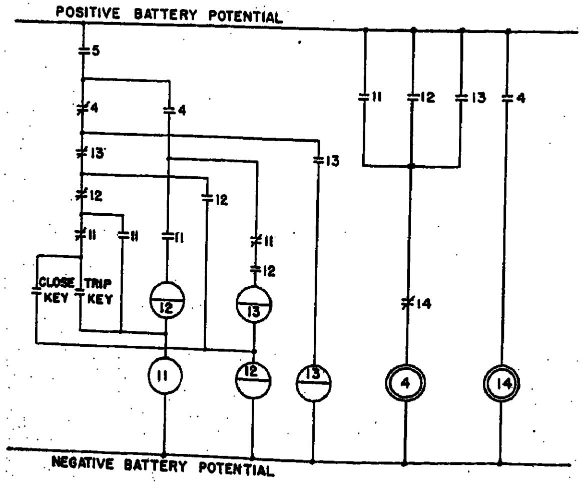


Figure 8 (left). Fundamental circuits for transmitting control pulses from controlling station

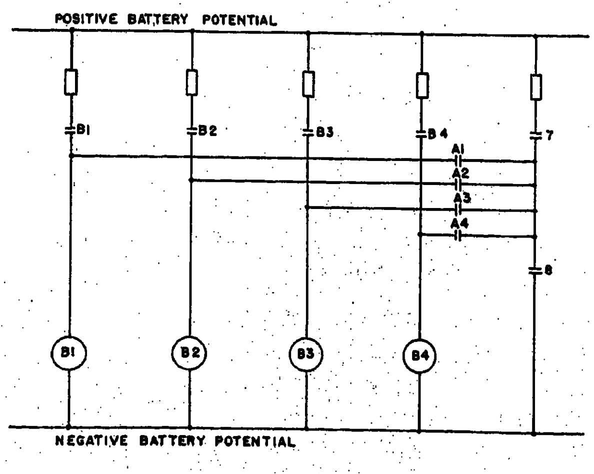


Figure 10 (right). Position indicating relay circuits at controlled station

described means that the preferred d-c signaling means can be used on telephone line wires. If the system is used to control more than one station on a single pair of wires, frequency signaling must be employed for all except one station. For this case, different frequency pulses must be employed for each station controlled.

For power-line carrier operation, a direct selection system using a single type of signal is to be preferred when it is feasible to key the carrier frequency itself as the signaling means.[4] However, there are some cases in which supervisory control must share a single carrier channel with such services as continuous telemetering and automatic load control. This makes it necessary to operate the supervisory control equipment on modulating audio frequencies regardless of the signal requirements of the supervisory control equipment. For such applications the synchronous system described can be applied by using two modulating audio-frequencies to provide the two necessary signals.

The synchronous system can be applied to microwave channels by using two subcarrier or audio modulating frequencies for signaling. This system is well suited for microwave operation, since with the interference problem being essentially eliminated at these frequencies, the use of two modulating frequencies instead of one is no disadvantage.

The system described is fast in operation, a complete check of the position of 50 supervised devices being accomplished in approximately five seconds.

The synchronous system provides for extremely rapid consecutive selection of units. This is a desirable feature when a number of selective telemetered indications must be checked at frequent intervals.

The system of signaling and the check and guard features incorporated in the design assure correct selections and indications.

For applications where either a direct selection system or the synchronous system described could be used, the choice should be governed by the particular requirements of that application.

References

1. Visicode Supervisory Control, L. J. Cissna. *Electric Journal* (East Pittsburgh, Pa.), volume 28, July 1931, pages 424–30.

2. A Self-Checking System of Supervisory Control, M. E. Reagan. *Electrical Engineering* (*AIEE Transactions*), volume 57, 1938, October Section, pages 600–605.

3. Channels for Operation of Supervisory Control Equipment, W. A. Derr, M. J. Brown. *AIEE Transactions*, volume 67, 1948, pages 1654–60.

4. The Combination of Supervisory Control with Other Functions on Power-Line Carrier Channels, R. C. Cheek, W. A. Derr. *Electrical Engineering* (*AIEE Transactions*), volume 64, 1945, May section, pages 241–46.

Discussion

P. W. Schirmer and **J. J. Winsness** (General Electric Company, Philadelphia, Pa.): The supervisory system described in this paper has some design and circuit simplifications. However, its application seems somewhat limited. It does not appear to be suitable for multistation application. Also, if used with carrier the additional cost of equipment for two audio tones is necessary. It has been our experience that there have been objections to the use of locking pull key for selection, because if the operator neglects to reset the key after completing an operation, the equipment remains tied up to the selected point and cannot transmit changes of indication from the remote to the control station.

In addition to these comments, there are certain questions, the answers to which are not clearly brought out in the paper. These are as follows:

1. What would be involved in expanding an equipment with a given number of points to one with a greater number of points after installation?
2. What is the maximum number of points available in a standard equipment and in what steps or groupings can these points be furnished?
3. What happens if one station gets an impulse from an outside interference when the other station does not?
4. Are unit assemblies of relays easily removable?
5. Can telemetering be put on the same point with control and indication?

W. A. Derr: The following comments apply to the discussion submitted by Mr. Schirmer and Mr. Winsness.

The types of applications for which this new system is most suitable are outlined in the conclusion of the paper. As stated in the seventh paragraph of the paper, the new system is not suitable for multistation applications.

Both locking-type pull keys and spring-return push keys have been used on supervisory control systems. Which type is preferable is a matter of personal opinion. The system as described in the paper is based on the use of pull-type keys. However, push-type keys could be used with a small additional amount of equipment.

The following answers apply to the numbered questions of the discussion submitted by Mr. Schirmer and Mr. Winsness.

1. Since no individual selection codes are involved in a synchronous system, it is simpler to increase the capacity of such a system than for any direct selection system. It is only necessary that space be available for mounting the necessary additional escutcheons and relays.
2. There is no limitation on the maximum number of points which can be provided. The equipment can be provided for any desired number of points.
3. If any number of short pulses are received at only one station due to interference, the equipment will not respond since long pulses are involved in starting the equipment. If a long pulse of the proper signal is received due to interference, the equipment will master check. Interference will not result in selection of an individual point.
4. The method of relay assembly is a construction feature. The paper has presented the principles and operation details of a new system and has not discussed construction features. However, this new system is as readily adaptable to various types of assembly and relay groupings as any other all-relay system.
5. Telemetering can be provided on the same point with control and indication with the new synchronous system just as can be done with direct selection systems.

Power Line Carrier for Relaying and Joint Usage—Part I

G. W. HAMPE
MEMBER AIEE

B. WADE STORER
ASSOCIATE AIEE

THIS is offered as the first of a pair of papers which are intended to summarize a number of principles and ideas that are in line with standard practices in carrier and related fields, to emphasize points of uncertainty and controversy, and to explore a number of ideas, both new and old, that are not necessarily standard, but do not deviate widely from what is, or potentially could be, done with existing equipment.

The second part of this paper will deal with these potentialities, and will show that more efficient use may be made of modern power line carrier channels, resulting in substantial economies and in conservation of the carrier frequency spectrum.

Need for Evolution in Carrier Usage

As the title indicates, the point of view is that relaying is a primary carrier function, even though the emphasis on specialized forms of relaying, singly and in combination with other specific functions, is reserved for the second paper under the same title. There are several reasons why carrier relaying is useful in place of, or in addition to, other relay schemes. Until recently, the principal one has been that any system of speedy and reasonably adaptable relaying for *ground* faults has been a pilot scheme, whether by wire, power line carrier, or (quite recently) by space radio. If the recently-developed ground distance relays prove effective[1] they will probably be adapted to work with carrier, resulting in decreased complexity and greater reliability in the control of carrier for ground fault relaying on those systems where the neutrals are essentially solidly grounded. Not all systems are of this type, and even on such systems faults-to-ground range from severe to quite limited, hence other means, perhaps electronic,[2] for simplified carrier control also would be desirable. Statements that limited faults to ground do not need quick clearing are to be discounted for the following reasons:

1. An extended fault arc frequently involves another phase conductor, and occasionally another line.
2. If longitudinal motion of the arc is limited by ice, a static (ground) wire, or even a conductor, may be burned in two.
3. If a static wire has its steel core exposed by an arc, and the burn is not located and repaired, corrosion will cause eventual breakage, probably under ice loading. This danger, which is greater for some types of static wire than others, also may apply to line conductors to some extent.

The newer factors which make it necessary that carrier relaying continue to evolve are immediate automatic reclosing, and the multiterminal line.[3] Economic pressure is forcing power system designers to adopt these, sometimes both on the same line, and means of efficient relaying must be found. The writers predict that such relaying will involve pilot channels, many with carrier as a pilot medium, and that some of them will involve hybrid combinations of relaying principles such as are to be set forth in the second paper.

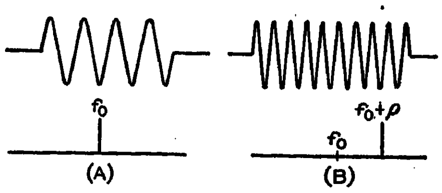


Figure 1. (A) Unmodulated carrier which might be keyed on and off. (B) A side frequency of a suppressed carrier, resulting from a single tone modulation (s-s-b)

(B) Also could be set up by a keyed frequency shift (n-b-f-s)

Joint Usage Considerations

Power line carrier is used widely for services other than relaying, and most carrier relay schemes have certain provisions for limited nonrelaying usages. This paper will point out that these limited provisions lead to inefficient use of carrier energy and spectrum space, and have some hazards which are not recognized by all users. It would therefore seem desirable to examine the joint usage problem.

At present, with separate usages, economic compromise usually results in a communication channel which is not reliable when needed most, as in bad weather. At the same time, high grade equipment is used on relatively short distances to provide relaying channels which are in active use only a few seconds a year, while the nonrelaying usages tend to be frequent or continuous. (The economics of joint usage tend to become increasingly obvious as the number of needed functions increases.) These conditions point toward joint usage of carrier channels and equipment.

Carrier channels and equipment can be shared in varying degrees. Thus two or more functions, such as relaying and voice communication, might be combined, all utilizing the same frequencies and the same carrier receivers, transmitters, and so forth. These same functions might be combined by utilizing jointly the same "yard" equipment such as traps, coupling and tuning units, but using separate electronic equipment all on the same or separate carrier frequencies. Arrangements of the latter type would possibly

---

Paper 49-151, recommended by the AIEE Carrier Current Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20–24, 1949. Manuscript submitted March 21, 1949; made available for printing April 28, 1949.

G. W. HAMPE is with the Relay Settings Section, and B. WADE STORER is with the Electronics and Communications Division, both with the Commonwealth Edison Company, Chicago, Ill.

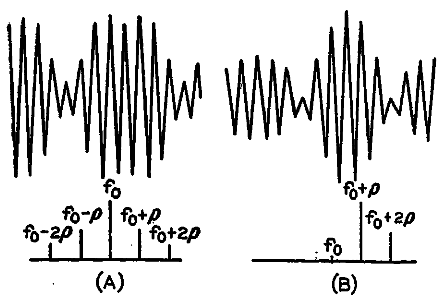


Figure 2. (A) Two methods of representing a carrier, amplitude modulated by two tones of frequency ρ and 2ρ are shown. (B) The modulation components remaining when the carrier and one side band have been suppressed are indicated

overcome special problems that sometimes arise, as in servicing equipment for unlike functions.

In general, every carrier application presents somewhat different requirements, and one form of joint usage or choice of apparatus is not necessarily the best solution to all problems. Hence it is necessary to study the various forms of carrier signals available, as to their particular advantages and limitations.

Carrier Channel Building Blocks

Carrier signals may be classified as to types of signal control or modulation or, the combination of the two. The direct form of control is that of keying the carrier frequency itself after the manner of a telegraph signal. Putting a carrier on (Figure 1) or taking it off, or both, is common in relaying practice. Keying, shifting a frequency, and coding the impulses produced by such controls also can be applied to carrier modulations. Descriptions of methods of transmitting modulated signals are given in subsequent paragraphs.

Carrier signals may be modulated in amplitude (a-m) after the pattern of the original form of broadcast radio. That is, the carrier frequency cycles may be compressed or enlarged in magnitude inside the envelope determined by the modulating frequency or set of modulating frequencies, Figure 2. The modulating frequencies are usually audio tones, that is, in or near the frequencies of audible sound. Such a signal at the receiving end is put through the process of detection, which is essentially rectification. This eliminates the carrier frequency, and the modulating (audio) frequency is reconstituted.

The modulating frequency also may be used to advance or retard the carrier waves in time phase. This is called phase modulation (p-m) as a general term. Since the changes of phase also are instantaneous changes of frequency, one form of phase modulation has been designated as frequency modulation (f-m). A frequency modulated signal when received is converted by a discriminator back to the original modulating signal.

When a carrier signal is modulated it tends to be spread over an appreciable portion of the carrier spectrum. This is true for all forms of modulation, not merely for f-m where it is obvious. (One special exception is indicated in Figure 1.) The resultant signal can be analyzed into the components which are the unmodulated carrier, and side frequencies, which, if modulation is done by a complex of frequencies, become side bands. The manner in which side bands originate can be found in several recent references.[4,5]

It can be seen that if carrier is modulated, the modulation as contained in at least one side band is essential. There are two variations of a-m which are intended to accentuate side-band energy. One involves the transmitter only, and is known as the Taylor[6] or "supermodulation" principle. In this system radio frequency power is supplied to the output circuit during the positive half of the modulation cycle by an auxiliary audio-pulse controlled power amplifier tube. Greater over-all efficiency with compressed carrier and emphasized side-band operation is claimed. In the second, one side band and the carrier are suppressed in the low energy stages of the transmitter, so that the entire transmitted energy is concentrated in one side band.[5] In the nomenclature of this and the sequel paper, the abbreviation s-s-b (single side band) indicates the suppression of both one side band and the carrier. For s-s-b the carrier must be reintroduced at the receiver as part of the process of detection.

For s-s-b, if a single modulating frequency is used, a single-side frequency is transmitted. The result, in wave form and frequency, is the same as if the carrier had been given a keyed shift, Figure 1. In Figure 2 complex modulation by two tones is shown for double side-band a-m, and the same with one side band and carrier suppressed as in s-s-b.

The modulating frequencies as well as the carrier itself can be keyed on and off. For f-m and a-m, the termination of all modulation restores unmodulated carrier. For s-s-b it terminates transmission.

A fourth carrier channel building block is equipment which operates on the narrow band frequency shift (n-b-f-s) principle. Here, instead of a modulation or an on-off keying, the carrier undergoes a keyed frequency shift. The frequency shift transmitter operates on such a narrow band that a number of such channels, each with its own transmitter and receiver, can be operated on line tuning and coupling equipment and traps which are tuned for one conventional broad-band carrier frequency.[7] The frequency shift is small, on the order of 60 cycles (on carrier of 100 kc). The receiver includes crystal filters and a discriminator, (f-m type, see Figure 3) and offers high selectivity with respect to spurious signals. It is stated in the foregoing reference that the spacing between these narrow channels, with frequencies stabilized by crystals need be only 0.5 per cent of the average carrier frequency. This applies to channels with the same direction of transmission. For transmission versus reception at the some terminal, the spacing required is stated to be approximately 5 per cent. Such transmission is useless for voice, but for other services, including some forms of relaying, it offers an alternative to audio tones superimposed on carrier.

Any of the foregoing principles may be used to transmit pulsed signals which may vary in frequency (pulsing rate), or which may be coded, that is, varied in amplitude, duration, spacing, or combinations of the foregoing to form code signals. Such pulses, pulsing rates, and pulse codes may be used for telemetering, supervisory control, and load control. All the forms of transmission are not equally adaptable to all uses, as the pulses may approach too closely to modulating frequencies, or be too rapid for the transitions required between states. As a rule, the narrower the spectrum in which the transmitted energy is confined, the more sluggish it is in changing states. This is a consequence of the energy stored and released by the filter elements. Given

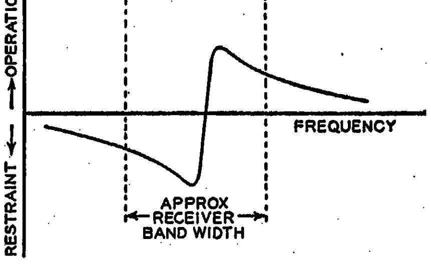


Figure 3. Characteristic of a discriminator (essentially an f-m type) as used for n-b-f-s signal reception

enough spectrum space, as in short-wave radio, even voice can be changed to coded pulses, transmitted as such, and changed back to voice[8,9] sounds. On a much slower basis similar principles might be used in the carrier field.

Carrier Channel Limitations

ATTENUATIONS

The energy impressed on a carrier transmission link is dissipated in various ways, so that the power at the receiving end is considerably less than at the sending end. This loss of energy or decrease of power is called attenuation. If there are irregularities of structure along the conductor paths, the response throughout the frequency spectrum may be quite irregular, limiting the choice of suitable frequencies. Sections of underground cable may have very high attenuation at all frequencies, but this varies considerably with the type of construction. For some types the means of bonding sheath sections is a critical factor. A paper on carrier attentuation measurements was recently presented.[10]

On some lines, particularly those with irregular frequency response, switching conditions may cause wide variations in attenuation. This indicates that in some cases terminal equipment beyond the line section has considerable bearing on carrier transmission—a factor which will be reconsidered in regard to the application of traps. If the extraneous influences are coupled to the line by other wires on the same right-of-way, the choice of a suitable frequency to meet all contingencies may involve a considerable number of test measurements.

Bad weather conditions, such as ice formation, greatly increase attenuation at a time when good relaying and communication are at a premium. Dirty insulators combined with fog, rain, or ice also may cause increased attenuation. The attenuation, as a function of the ratio of transmitted to received power, is expressed usually in decibels.[21] For an interpretation of attenuation in terms of both decibels and wattage loss, see reference 10.

The effect of attenuation can be offset by increased power at the transmitter, increased sensitivity at the receiver, or by receiving, amplifying, and repeating the signal enroute. These are the obvious primary remedies. The receiver selectivity is quite important, but indirectly so, as it is dependent on the nature of the desired signal. This is discussed in detail later. The amount of attenuation can be reduced by other means, such as changing the route of transmission, the frequency of the carrier, the form of coupling, and so forth.

For voice transmission over longer distances, the joint usage of relay channels may be made practical by the use of audio by-passes at intermediate switching stations. Audio by-passes introduce distortion, however, so that the number of links that can be thus placed in tandem is limited. Such by-passes, by reception and repeated transmission into the succeeding line section, make the amplification of the nonrelaying (communication) signals possible. This large gain in signal strength makes practical transmission over much greater distances. Such by-passes are essentially independent of switching conditions, probably to a greater degree than conventional by-passes on yard equipment, and further permit passing the through-type communication signal on to a succeeding carrier relay channel.

SIGNAL VERSUS INTERFERENCES

The principal limitation on receiver sensitivity is that type of interference known generally as noise. Noise, which for present purposes is not defined as being limited to audible range, may be fairly constant for appreciable time intervals (that due to corona for example), or may come in random pulses, or be tied in, more or less predictably, with reproducible phenomena on the line or in its proximity. In interpreting the effects of noise, it is often combined with signal strength values and referred to as the signal-noise ratio. Some functions can tolerate a lower signal noise ratio than others. Although the transmitters and receivers for communication and conventional relay carrier are similar, the communication receiver can usually be adapted to work through a greater attenuation than the relay signal, hence the latter channel must be limited in attenuation or bolstered to meet relay standards. Under average conditions this is logical, as signals too weak or too noise-ridden for relaying may be quite intelligible to experienced personnel. In bad weather when communication is at a premium, the weak and noisy signals become worse, which may be unfair to those depending on them under those conditions. The signal-noise ratio can fall off due to increased attenuation, increased noise, or both, and during bad weather conditions both limiting factors usually become worse, and a simple listening test is not a very reliable indication as to which effect predominates. Greater knowledge of signal-to-noise ratios, and of noise amplitudes on power systems is needed. Work is being done in this field, and progress in getting methods of measurement standardized into readily comparable forms has been made.[11]

Closely related to noise interferences are those which are not noise as such, yet may totally interrupt proper carrier transmission. For example, high-voltage transients (often originated by switching) may flashover safety devices on the carrier apparatus. Interference also may take the form of spurious signals which are essentially indistinguishable from the functional ones. Total signal suppression and spurious signals may result from the same or similar causes.

This brings up a very interesting and important observation. One would expect that fault arcs might set up spurious signals. This they probably do, but experience and staged fault tests indicate that sufficient spurious signal to interfere with relaying is quite rare.

Circuit breaker arcs have been observed to cause considerable interference, and the question arises as to whether automatic circuit breaker operations on one line can suppress (or falsely initiate) signals on an adjacent line. Further evidence on this point should be gathered. One manufacturer has provided relay circuit controls for over-riding a transient of this type. Experience indicates that the most pronounced interferences, both as to suppression of signal and spurious signal, result from low-power arcs in circuit breakers and on disconnect switches in, and adjacent to, carrier terminals.

Signal transmission may be interrupted by short circuits or open circuits on the transmission line. Just what constitutes an effective short circuit or open circuit is questionable, however, where carrier is concerned. The deliberate use of alternate paths has been proposed.[12]

AVAILABILITY OF CHANNEL SPACE

Another class of interferences might be likened to the problem of cross-talk in the field of audio communication over metallic channels. In a given locality the carrier spectrum (bearing in mind that some frequencies may be found unusable because of excess attenuation) often rapidly becomes congested. Each power line is of course closely coupled to the other lines paralleling its path so that frequency separation is necessary. Interference with other line sections is limited by geographical distance and frequency separation, that is, by keeping adjacent frequencies at remote parts of the power system. In special cases, more expensive arrangements, such as trapping every phase of every line beyond a certain bus, may be found expedient. There also are interferences with respect to other power companies and other utilities outside the field of electric service.

It was mentioned before that receiver selectivity is an important factor in limiting interference from random noise. With regard to adjacent channel interference it is even more important. The narrower the acceptance band of the receiver, the less interference it will pick up, and, other things being equal, the closer it can be placed to its neighbors, both in geographical and spectrum space.[20] The acceptance band width will depend on the nature and content of the transmitted signal, and will in general tend to be at least somewhat wider than that covered by the signal. This is due in part to the need for some tolerance in regard to the stability of transmitting and receiving equipment, and partly due to the inherent nature of reception. For example, for some applications there is monofrequency transmission, but there is no way of providing reception that will accept only one frequency. Furthermore, an acceptance band whether narrow or wide, Figure 4, never has perfectly straight sides, hence signals of considerable frequency deviation will be received if they are strong enough. This puts a considerable handicap on close spectrum spacing of a receiver and transmitter at the same terminal.

The f-m discriminator used with the n-b-f-s receiver has a characteristic as shown in Figure 3. It offers a sharp and stabilized point in the frequency spectrum at which the carrier crosses over from restraint to operation and vice versa, hence the narrow band effect is accentuated. Noise covers a wide band, in general, hence cancels itself in the discriminator circuit. Adjacent channel interference may be limited considerably by crystal filters, but placing the n-b-f-s channel so that adjacent carrier tends to produce restraint may be an added factor of safety.

For a-m, f-m, or s-s-b a wide band tone receiver can be used to provide restraint if spurious signals are received. In one proposal[15] everything (so far as practical) outside the narrow band required for the desired signal is to be received and applied through appropriate circuits as a restraint.

Another principle in guarding against interference is to transmit a continuous nonoperating or restraint signal which must be removed or shifted before an operation can be transmitted. By automatic volume control action, such a restraint signal reduces the sensitivity of the receiver to noise and unwanted signals, in addition to whatever direct holding effect the signal provides. The objection is that a continuous transmission of carrier, or a tone modulation thereof, furnishes a constant source of possible interference. This applies both as to the fundamental transmitted frequency and possible harmonics. In some cases a constant source may be worse than an intermittent one as regards other channels. It is questionable whether this is really a valid objection, and data are needed to substantiate it. In other applications provision is made for reception of a restraint signal, but it is deliberately transmitted only when conditions become abnormal.

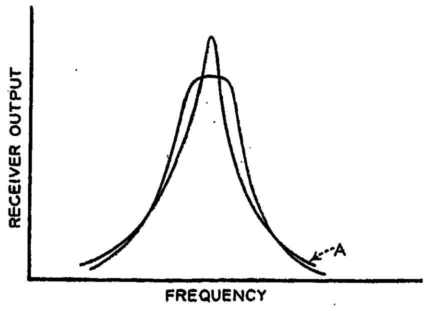


Figure 4. A typical acceptance band of a receiver

A signal impressed at A, if sufficiently strong, will cause interference

For any type of carrier transmission the acceptance band of the receiver should fit the transmitted signal fairly closely, but in trying to cover a variety of standard applications with a minimum variety of apparatus, this is not consistently followed. For example much relaying is done with a monofrequency, keyed carrier, but the receiver is wide band. This permits reception of the side bands which are added to the carrier when test or emergency communication is used, but the receiver also is wide open for noise and for interference from other signals. If a narrow-band receiver were used for relaying, transmitters of present wattage ratings would provide for operation through considerably increased attenuation ranges. If the transmitter wattage should be reduced to cover any but the most exceptional conditions, more relay channels could be fitted into a given spectrum space, both because of increased discrimination against unwanted signals, and the reduction in power of those signals.

For example, if there are four carrier relay channels between two terminals, test and emergency communication is hardly necessary for all of them. Similar logic applies where specific provision for communication has been made. Note that if the relay carrier receiver band can be narrowed only moderately, as for phase comparison relaying, the foregoing still applies, but to a reduced extent. Other considerations may outweigh the saving in spectrum space, and other adaptations, as offered in the second paper, may be more advantageous.

Carrier channels normally operate at frequencies between 50 and 150 kc, with the band between 150 and 300 kc being used mainly because of congestion in the lower band. The higher frequencies will tolerate a more rapid keying or pulsing rate, which is an advantage, but the carrier attenuation tends to be somewhat greater than for the lower band.

DISTORTION

Transmission also is limited by signal distortion, that is, by differences in quality of the input and received signal existing even in the absence of extraneous disturbance. When the signal is produced by keying carrier the transients of the apparatus, which determine the time required for the carrier to go from one steady state to another, are the principal distortion. The same type of distortion occurs when an audio tone is keyed. If an audio tone is keyed at a rate approaching its own frequency, distortion of signal naturally results.

Audio tones, including the voice frequencies, are subject to harmonic distortion, frequency distortion, phase distortion (usually not serious) and cross modulation. All of these forms tend to originate from, or be augmented by, the nonlinear characteristics of carrier apparatus. Such nonlinearities may be the result of imperfections or deliberate design. For example, a flat frequency characteristic from 200 to 3,000 cycles is considered necessary if voice is to be transmitted naturally and intelligibly. Cutting off the low end tends to reduce naturalness, but cutting the high frequencies reduces intelligibility. To transmit the low frequencies requires large amounts of energy, tending to overload equipment such as amplifiers, hence in some cases intelligibility may be improved by eliminating the lowest voice frequencies. This is particularly true on equipment not primarily designed for communication. It is a matter of general observation that test and emergency communication over carrier relay channels is usually better on the lines with weaker signals, as strong signals saturate the receiving apparatus.

Another cause of distortion is overmodulation. For amplitude modulation including single side band, this means the modulating tone or combination of tones is more than sufficient to bring the carrier wave down to zero amplitude. The "super-modulation" principle is an interesting attack on this problem.[6] For f-m transmission, over-modulation means a frequency shift beyond the channel width for which the equipment is designed. For any conventional type of modulation it is evident that elimination of portions of the low audio spectrum, which involve high energy levels, tends to prevent that form of distortion which results from overloading and overmodulation.

Cross modulation arises in various ways, but becomes a particular problem when part of the audio spectrum is set aside for tone signals, as for telephone ringing or for relaying. To avoid it, audio tones which exist simultaneously must not have frequencies which when added or subtracted will give rise to tones where they are not wanted, in the voice band for example. This becomes an interference problem.

Another source of distortion as well as loss of signal strength may be apparatus instability, that is, the tendency of the apparatus components to drift out of line with age or changing atmospheric conditions. This involves problems of detail design and maintenance.

Choice of Carrier Channel Building Blocks

At present, the available carrier channel building blocks are: (1) keyed carrier, (2) modulations transmitted by (a) conventional (a-m) (with "super-modulation" as a possible variation), (b) single side band, (c) frequency modulation, and (3) the narrow band frequency shift. Each has distinctive properties which should be considered for a given set of functions in a given installation. Combining functions in joint usage in varying degrees gives quite a variety of possible combinations, and it is quite possible that more than one building block can be used to an advantage.

The choice of building blocks should be guided according to the following factors:

1. Suitability for the desired function. (There are usually several means of obtaining the same result.)
2. Operating range sufficient to work through the maximum attenuation and interferences. (The pressure in this respect varies somewhat for different functions.)
3. Minimum occupancy of the carrier frequency spectrum.
4. Economy of first cost and maintenance.
5. Maximum apparatus reliability and availability. (This again involves maintenance.)

These factors should be weighted according to the requirements of the application. Thus if keyed carrier or simple a-m will suffice, that is what should be used. Unfortunately, particularly in regard to line characteristics, the necessary data are often lacking, so that an installation may prove to be inadequate in use, or may be made much more than adequate at excess cost, to allow for unknown factors. To avoid misapplications sufficient test data should be taken in advance.

SPECIAL PROPERTIES

Some special properties have already been discussed under the limiting factors of attenuation, noise, cross-talk, and distortion, but there are others which were not emphasized. Thus n-b-f-s can be used for telemetering, and so forth, and some kinds of relaying. It cannot be used for communication, but can be superposed on an a-m communication channel.[7] The s-s-b type of transmission makes possible a minimum acceptance band for voice. As an example of limited joint usage, n-b-f-s could share common yard equipment with s-s-b. The n-b-f-s channels can be placed in the spectrum space on either side of the s-s-b signal.

Conventional a-m and f-m occupy approximately twice the channel width of s-s-b. Where channel width is not a final determining factor, it should be pointed out that f-m transmission (with limiters to eliminate amplitude variations) provides an excellent automatic volume control characteristic that is quite advantageous.

There are many carrier applications where it is desired to accomplish a number of separate functions such as transferred tripping, load control, and so forth. These functions may be accomplished by either tone modulations of a-m, f-m, or s-s-b transmitters or by n-b-f-s transmissions, with possible pulse coding in any case for greater adaptability. A tone carries only a portion of the total energy of the modulated carrier, but it also picks up only a portion of the total interference suffered by that carrier, hence, within limits, tone signals are quite reliable. The greater the number of simultaneous tones, the more the total signal energy must be subdivided. With n-b-f-s channels there is a similar limitation. The greater the number of n-b-f-s channels using common yard equipment at a given terminal, and transmitting signals simultaneously in a given direction, the less the range of each.[7]

For reasons stated previously, the present-day relay practice of using a wide band carrier channel exclusively to transmit a simple on-off signal will in the future meet increasing resistance from economic considerations and spectrum crowding.

Terminal Equipment

Carrier transmitters and receivers are coupled to the power line by capacitors connected between one phase conductor and ground, or between two phase conductors of the same 3-phase circuit (interphase coupling) or between two conductors on different circuits (intercircuit coupling). The same equipment may be used jointly for separate channels on more than one type of coupling. For example, the coupling capacitors (and traps) of two conductors coupled to ground on two circuits with the same station destinations also may be used for intercircuit coupling by the addition of line tuning units for added frequencies.

One advantage of interphase, or better still, intercircuit coupling is that certain types of line work can be done without interrupting the channel. Also it is claimed that the channel can be balanced so as to minimize interference to and from it. The counterclaim is that this balance is too hard to maintain to have any practical value. For some types of relaying, phase-to-ground coupling is not considered acceptable as will be discussed in the second paper. The disadvantages of interphase or intercircuit coupling are greater cost (unless in combination as above) and increased coupling loss (attenuation). The coupling loss is generally a minor factor however.

By the introduction into the power line conductor of an inductance in parallel with a capacitor, a carrier frequency or a band of frequencies with a spread of up to about 12 per cent can be substantially prevented from passing a certain point in that conductor.[18] Such devices are called traps, and may be tuned to block more than one frequency band. Tuning one trap to resonate for more than two frequencies is considered highly impractical, however.

Carrier for relaying is usually blocked by a trap at the end from which a signal is to be transmitted, so that the signal is projected, initially at least, in only one direction. The trap can be omitted if some other device such as a power transformer serves the same purposes, or if 2-way projection is admissible. The principal purpose of trapping relay carrier is to prevent the transmission of carrier energy from being short circuited or seriously attenuated by a fault or switching condition exterior to the protected line at the transmitting terminal. If intercircuit coupling is so arranged that no fault or special system operating setup can reduce the carrier energy to an unsafe level, trapping may be omitted in some cases. Also, if a signal is only received at a terminal, trapping for that signal at that terminal may not be necessary. A trap that is tuned for one or two frequencies offers considerable impedance to other frequencies, particularly the adjacent ones in the spectrum. Often a dead short circuit beyond a protected line section would still leave sufficient signal transmission for satisfactory relaying because a trap that is not resonant to a certain frequency still offers considerable impedance. For relaying, this is not often trusted, however, because the off-resonance value of a trap is either inductive or capacitive, and there may be some condition beyond the line terminal which will reduce the effective value of that inductance or capacitance to practically zero. In particular cases, as when the trap is backed up by a piece of power apparatus that blocks all frequencies, operating more than one frequency on a single tuned trap may be just as efficient as double tuning, or when compared to coupling to additional phases, there may be considerable economy. Limited trapping should be considered for minimum cost installations on low-voltage lines.

The other functions of trapping are to decrease attenuation by largely confining signal energy to a given line section, and to decrease the cross-talk type of interference with other carrier channels. In some exceptional cases all three conductors of a line (sending or receiving or both) may be trapped, as the natural coupling between phase wires tends to put the signal on to conductors from which it can propagate into other sections. How effective this is depends on what other conductors are on the same right-of-way or adjacent to it (and for what distance), and how much is propagated over static wires.

Whether multiconductor trapping with phase-to-ground coupling decreases the effectiveness of transmission is at present disputed. It has been stated that the noncoupled conductors, in parallel with the earth and static wire, form part of the signal return path, and that the extra traps interfere with it. That the return path is important is evidenced by the difficulty of transmitting carrier through underground cable where the cable sheath sections have been connected by means of bonding transformers.

Whatever the final answer on some of the foregoing questions may be, the added care and equipment generally used on relay carrier channels, as compared to carrier channels for other services, becomes available to increase the reliability of those services under joint usage.

Performance Requirements

Power line carrier performance requirements may be classified into three groups according to functions, as relaying, communication, and miscellaneous services.

Carrier Relaying

Relaying by carrier requires a high degree of transmission reliability and availability. This means a good received signal even under adverse conditions and means of readily checking and maintaining the over-all carrier and relay performance. Other things being equal, carrier relay equipment which can be checked and maintained independently of the noncarrier relay elements (which may be either first line or back-up relay elements) is preferable. With respect to joint use of carrier relaying equipment for nonrelaying purposes, there are advantages and disadvantages. As most of the other services are constantly in use they automatically monitor the carrier equipment which otherwise functions only during system disturbances.

Maintenance of a jointly-used carrier channel may run into difficulties where separate groups of personnel, communication and relay sections of the same organization, for example, are both handling carrier transmission equipment. Fundamentally there is little difference in this equipment whether it be for relay carrier or communication carrier. Since power line carrier is a highly specialized field it would seem that the simplest solution to this maintenance problem would be to have a separate carrier section responsible for all types of carrier equipments. If such an arrangement cannot be worked out, the problem must be recognized, and the carrier equipment for joint use be laid out so that the functions may be separated satisfactorily. In some cases separation of the functions into separate cabinets is a satisfactory solution for maintenance under divided authority with joint usage.

Carrier Communication

Communication by carrier can be classified into three functional grades. The lowest is for testing and emergencies. The next is for operating, that is, switching orders, load dispatching, and possibly work dispatching. This calls for a high degree of reliability and availability, quite similar to that for relaying, but high voice quality (for example, personal voice recognition) is not so essential. It is surprising how much distortion and noise can be tolerated by experienced personnel. The highest grade is administrative communication, which in addition to high reliability and availability needs fairly good tone quality. Those who use the channels regularly will accept distortions which others will not. Good quality requires a band 2,500 to 3,000 cycles wide, hence uses a considerable slice of the carrier spectrum.

Communication also may be classified as point-to-point, push-to-talk, party-line, selective ringing, duplex, and so forth, which are not all equally suitable for the three functional grades mentioned before. Signaling (as by tones) is required for some of the above functions.

Miscellaneous Services

The miscellaneous services follow:

Glaze (ice) Detection. This is essentially a by-product of received signal strength measurements, but may justify equipment designed for the purpose.

Supervisory Control. This includes remote control of switching and remote indications of system conditions—usually on an on-off or alarm basis. It is accomplished usually by coded signals. High reliability is desirable, but the signals are merely keyed pulses.[19]

Automatic or Semiautomatic Load Control. This is similar to supervisory control, but reliability under adverse conditions is not at a high premium.

Telemetering. Next to voice and some forms of relaying this may be the hardest form of signal to transmit, but reliability requirements are much like those for load control.

A Fundamental Relay Control Problem with Joint Usage of a Single Signal

Types of Carrier Relaying

There are at present three standard types of carrier relaying. These are:

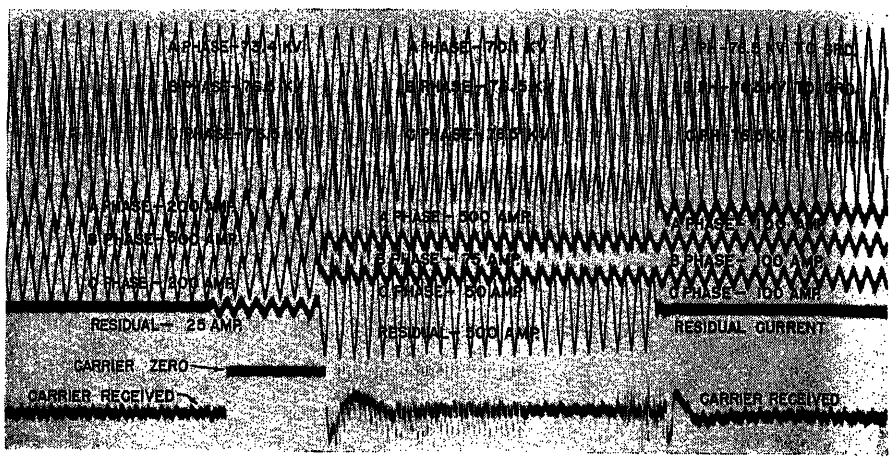


Figure 5. A record of a fault in which relay control of carrier was lost when the line opened at one end

(a) transferred tripping; (b) directional comparison; and (c) phase comparison.

Type (a) is usually used to protect terminal apparatus.[3] It is limited to special cases and is not often combined with types (b) and (c). A continuous carrier blocking tone is sometimes used to prevent false trip-outs. To date, the most important applications of (a) have been on two terminal lines, but multiterminal applications show promise.

In types (b) and (c) carrier signals are not normally transmitted continuously for relaying. Standard relay equipment provides for the use, or ready adaptation, of the carrier frequency for test and emergency communication and for keyed signal functions such as telemetering. One disadvantage in having relay carrier (or a tone) held on for relaying or any other purpose, such as signal strength tests, or measurements for sleet detection, is that on-off control of the signal by relay elements must be provided at both terminals when a fault occurs.[16]

If at a given terminal there is a firm source of fault current, present equipment is designed to accomplish this purpose. If there is no fault current source for a terminal, or if the path of that fault current is interrupted, there are no standard provisions for the relay elements to secure control. With the types (b) and (c) if a steady signal is transmitted (as under joint usage) from a terminal which does not supply fault current, carrier relaying at the terminal which does supply fault current is suspended.

The cases in which no effective fault current can flow from a given terminal are as follows:

1. No source of positive phase sequence current. The failure to open where there is no source is not generally serious, but the carrier restraint at the supply end may be objectionable. Generally noncarrier relays must function at the source terminal.

2. A feeble or transient positive sequence current source. If there is no immediate automatic reclosure, this may be viewed the same as (1), but with such reclosure, immediate opening at both ends may be required to prevent the feeble source, motor regeneration for example, from sustaining an arc which would cause retripping.

3. A positive sequence source, but no effective source of zero sequence current at a terminal. In this case a fault to ground (even though cleared from one end by noncarrier relays) may lead to continued abnormal voltage stresses, possibly augmented by an arcing ground at the point of fault.

4. The circuit breaker being initially open, or open pending automatic reclosure. During glaze storms lines may be open at one end for several minutes, and occasionally, to shift load to other lines for glaze prevention, single end excitation (called "holding a line alive on back feed") may extend to hours.

5. Sequential operations. Occasionally, because of system or fault conditions, a line opens quickly at one end only. The open end relays then have lost control of the carrier, and the remaining end is still to be opened, Figure 5.

6. A breakage of one or more phase conductors. This interrupts the path of the fault current from one terminal in many instances.

An Example of Loss of Relay Control

The oscillogram of Figure 5 shows an example of (5). Carrier had been placed on at the remote terminal for glaze detection and was received as shown on the oscillograph trace. A fault-to-ground occurred and the remote end circuit breaker opened promptly from the instantaneous (noncarrier) ground relay. For unexplained reasons the local end did not furnish enough ground current to actuate the carrier control relays. The received carrier ceased momentarily, but as soon as the remote circuit breaker opened the carrier stopping relay at that end reset and carrier was again transmitted from the remote terminal and received as shown. The local circuit breaker would have been opened by the ground backup relay, but meanwhile the arc extinguished of its own accord as shown by the neutral current trace. The local circuit breaker did not open.

The same difficulties can be encountered if carrier is transmitted from the no-source end for telemetering, communication, or even push-button tests. Inquiries have revealed that in some utilities that use carrier for relaying, this aspect of joint usage on standard equipment is not recognized, or at least not fully evaluated.

If deliberate, not incidental, joint usages of carrier relay channels become common, as economics and spectrum crowding already dictate, the problems of relay control must be solved so that carrier relaying is not handicapped under any system conditions. Solutions of this fundamental carrier control problem under joint usage will be presented in the second paper.

Summary

1. Carrier relaying according to the present practice, or the equivalent, is needed, particularly, but not exclusively, for ground faults, and will be increasingly needed with adaptations for automatic reclosing and multiterminal lines.

2. Joint usages, which may be adopted in various degrees, will alleviate spectrum crowding, and for many installations such usages will provide either reductions in installation and maintenance costs, or improved performance, or both.

3. Various types of carrier control and modulation are available.

4. The problems of carrier transmission and reception can be discussed qualitatively, but quantitative knowledge is meager.

5. In comparing carrier types or systems there are distinctive characteristics in each, both as to function and performance, which may be advantageous.

6. The choice between the available systems will depend largely on the functional characteristics desired, but it appears that there are undeveloped potentialities in each, and possibilities of making combinations.

7. The nonrelay usages vary in their requirements, making each application a distinct problem.

8. Present standard relaying equipment provides for limited joint usages of a single carrier signal with possible consequences which do not seem to be generally recognized, namely, the wide receiver acceptance band, with corresponding inefficient use of carrier energy and spectrum space, and in-

complete carrier control by primary relays.

9. Carrier relaying needs to evolve to meet new problems. Suggestions along that line, including solutions to the primary relay control problem, will be offered in the second paper.

References

1. A New Distance Ground Relay, S. L. Goldsborough. *AIEE Transactions*, volume 67, part II, 1948, pages 1442–46. (Digest in *Electrical Engineering*, January 1949, page 27.)

2. Electronic Relay Developments, James J. Loving. *AIEE Transactions*, volume 68, part I, 1949, pages, 233–40.

3. Protection of Stations Without High-Voltage Switching, Project Committee. *AIEE Transactions*, volume 68, part I, 1949, pages 226–31.

4. Comparison of the Amplitude-Modulation, Frequency-Modulation, and Single-Side-Band Systems for Power Line Carrier Transmissions, R. C. Cheek. *Electrical Engineering* (*AIEE Transactions*), volume 64, May 1945, pages 215–20.

5. A New Single-Side-Band Carrier System, B. E. Lenehan. *AIEE Transactions*, volume 66, 1947, pages 626–30.

6. The Taylor 'Super-modulation' Principle, R. E. Taylor. *Radio and Television News*, (Chicago, Ill.) volume 40, number 3, September 1948, pages 42–44, 96–102; also number 4, October 1948, pages 44–46, 192–195.

7. The Characteristics of New Carrier-Current Equipment for Telemetering and Load Control, R. W. Beckwith. *AIEE Transactions*, volume 67, part II, 1948, pages 1649–53.

8. Coded Pulse Modulation Minimizes Noise, "F.R." *Electronics* (New York, N. Y.), volume 20, December 1947, pages 126–31.

9. PCM Equipment, H. S. Black, J. O. Edson. *AIEE Transactions*, volume 66, 1947, pages 895–99.

10. Loss Measurements Made on Underground-Cable Overhead-Conductor 132 kv Transmission Line at Carrier Current Frequencies, H. A. Cornelius, B. Wade Storer. *AIEE Transactions*, volume 68, part I, 1949, pages 597–601.

11. Report on Method of Measurements at Carrier-Current Frequencies, Carrier Current Committee. *AIEE Transactions*, volume 67, part II, 1948, pages 1429–32.

12. United States Patent Number 2,387,146, H. W. Haberl.

13. Symposium on Operation of the Boulder Dam Transmission Line—Transmisssion Line Relay Protection, L. L. Draper. *Electrical Engineering* (*AIEE Transactions*), volume 58, April 1939, pages 151–56.

14. Bandwidth vs. Noise in Communication Systems, "D.G.F." *Electronics* (New York, N. Y.), volume 21, January 1948, pages 72–75.

15. United States Patent Number 2,307,771, W. T. Denton, et al.

16. Possibilities and Methods of Extending a Carrier Current Relay Channel to Other Uses, R. M. Smith, S. L. Goldsborough. *Electrical Engineering* (*AIEE Transactions*), volume 60, January 1941, pages 23–7; Discussion page 645.

17. Field Tests on Power-Line Carrier-Current Equipment, R. H. Miller, E. S. Prud'homme. *Electrical Engineering* (*AIEE Transactions*), volume 65, December 1946, pages 824–27; Discussion page 1177.

18. Application of Carrier to Power Lines, F. M. Rives. *AIEE Transactions*, volume 62, 1943, pages 835–44, Discussion page 945.

19. The Combination of Supervisory Control With Other Functions on Power-Line Carrier Channels, R. C. Cheek, W. A. Derr. *Electrical Engineering* (*AIEE Transactions*), volume 64, May 1945, pages 241–6.

20. The Distinction Between Effective and Circuit Bandwidths, Wm. S. Kessler. *AIEE Transactions*, volume 68, part I, 1949, pages 98–99.

21. Power-Line Carrier Channels, M. J. Brown. *Electrical Engineering* (*AIEE Transactions*), volume 64, May 1945, pages 246–51.

Discussion

C. E. Asbury (The Commonwealth and Southern Corporation, Jackson, Mich.): This paper gives quite a complete outline of the present-day equipment available, conventional methods of application and summarizes the present problems and limitations of carrier relay applications.

We agree with the authors that there is need for evolution in present-day carrier usage which will extend its application and usefulness. These developments should extend carrier relaying for protection of modern systems being built up to include multiterminal lines, lines supplying tap loads and lines terminated in transformer banks without high-voltage circuit breakers.

One of the major factors leading to joint usage of carrier channels is that of economics. Most of this joint usage is brought about in order to justify the installation of the carrier relay equipment and is not chosen just to accommodate other services. Much of the cost of a carrier installation is in the terminal equipment, such as coupling capacitors and wave traps. If these devices could be eliminated and a more economical method found to perform their functions, it is quite probable that there would be much less need for joint usage especially on carrier relay channels. There are a number of disadvantages of joint usage on carrier relay channels when using present-day practices. The authors apparently have studied all of these various objections and plan to outline methods of solution in their following paper on the subject. Such information should be helpful and welcomed by the industry. Some of the problems that need to be considered are:

1. Any additional equipment which may key the carrier is likely to block carrier relaying with that line terminal open when the line is being tested from the opposite end. It is not always desirable to block these additional functions while the circuit breakers are open. This loss of relaying control is very undesirable, and some provision should be made to avoid it where joint usage of carrier relay channels is to be utilized.

2. In a number of applications, joint usage is made of the coupling capacitors and occasionally of the wave traps. Such joint use results in the installation of filters to block one carrier frequency from the other. Not only are these filters expensive, but they complicate the maintenance of the equipment and are a potential source of trouble due to failure or possible shifting of frequency, requiring frequent checks on tuning.

3. Additional functions often are performed over a given carrier relaying channel by the use of audio tone equipment. Where such applications are used, it is necessary that provisions be made to avoid the loss of relay control, especially for conditions of one circuit breaker being open. The amount of carrier energy available for each tone channel is reduced as the number of tones is increased. Thus there is a limitation as to the decibel range of such equipment and to the number of tones that may be applied successfully.

4. There is a limitation to the number of frequencies that may be passed through a given coupling capacitor. Usually no more than two such frequencies can be utilized successfully on one coupling capacitor where amplitude modulation equipment, such as carrier relaying, is utilized.

It appears that considerable advantage could be realized in carrier current relay applications if the band width occupied in the frequency spectrum could be reduced. With present design carrier relay sets, it usually is considered necessary to separate frequencies on adjacent lines or on parallel lines by as much as 15 to 20 per cent. It is interesting to note that crystal control transmitters are available for metering; 15 or 20 of these channels can be worked successfully through a coupling capacitor. The decibel range of these metering carrier equipments is approximately two times that of the conventional carrier used for relaying and yet the cost of carrier relaying transmitters and receivers is approximately twice as much as that of the metering equipments.

Even with the present broad-band frequency required for carrier relaying and communication, little real difficulty is experienced with interference between channels. Regional committees have been appointed by various power groups to study and to assign frequencies in the regions to avoid interference. However, there is a dire need for 150–300 kc frequency band to extend the use of power line carrier equipment. With continued co-operation among the power companies in wisely utilizing the frequencies available in the 50–300 kc band for metering communication, supervisory control, relaying, and other similar functions, it is believed that with the continued co-operation of the carrier current equipment manufacturers in reducing the amount of frequency spectrum required per channel that little difficulty should be experienced due to interference between channels for the next several years. In the application of carrier relaying, it usually is possible to utilize the same frequency for a number of channels since each channel is trapped. Usually one intervening line section operating on a different frequency is all that is required for proper separation of lines having carrier relay channels operating at the same frequency.

H. W. Lensner (Westinghouse Electric Corporation, Newark, N. J.): In the development of power line carrier equipment for relaying, simplicity always has been one of the main factors. The transmitter usually is a conventional oscillator-amplifier combination using a stable oscillator which is adjustable over the carrier-frequency band. The amplifier increases the output power to a reasonable level. The relaying receiver has been kept as simple as possible consistent with good engineering practice. More than 20 years of experience has shown that a transmitter-receiver combination capable of working through an attenuation of 30 decibels is satisfactory for very nearly all carrier relaying applications. A 10-watt transmitter and a single tube receiver will provide this operating range, which is sufficient for most overhead line sections.

A single tube receiver will have a response curve several kilocycles wide, but its limited sensitivity makes it possible to use a frequency only a few kilocycles away at a remote point on the power system without interference. A more selective receiver will of necessity be more complex, and if the transmitted power is reduced to lessen interference in other channels, the increased sensitivity of the receiver makes it more susceptible to line noise, although this may be somewhat offset by the increased selectivity. Up to the present time, the simple relaying receiver has been adequate, but with the limited frequency spectrum available, the increased use of carrier in the future well may require the use of more selective receivers.

One subject briefly discussed in the paper is the unfortunate situation (which usually exists) that line characteristics are often lacking in the negotiation stage of a carrier application. This lack of information is a great handicap, particularly in considering a carrier communication channel running through several hundred miles of power lines with their taps, busses, parallel lines, and re-entrant circuits. It would be very desirable to have frequency-attenuation data on a power system in advance of the purchase and installation of suitable equipment. Such data would be of value to the manufacturer in determining what equipment was necessary, and to the user in putting the channel in operation. Unfortunately, in most cases suitable test apparatus is not available, and it is necessary to wait until the equipment is installed to check the characteristics of the power system. At the present time, there is no known method of calculating with any degree of accuracy the frequency-attenuation relation of a complex power network, so field tests are necessary to obtain this information which is needed to assure a dependable channel with various conditions of weather and system switching.

Another subject discussed is the carrier relaying of a line in which no effective fault current can flow from one terminal. When such a condition exists, some means must be provided at that terminal for on-off control of the carrier signal, so that if a fault occurs during communication, proper relaying can take place. The requirement has been met in several installations by using high-speed undervoltage relays, with their contacts in the communication carrier-start circuit. Upon the occurrence of an internal fault, at least one of the undervoltage elements will drop out, thus stopping carrier and allowing the other terminal to trip. An extension of this idea has been used to provide high-speed tripping of both ends of a line section with little or no source of fault current at one end. A continuous carrier signal is transmitted from the terminal which has a firm source of fault current. At the opposite terminal, the received carrier signal energizes a relay element to hold its contact open. This contact is in series with the paralleled contacts of three undervoltage elements to complete the trip circuit. Upon the occurrence of an internal fault, carrier is stopped by normal relay operation at the terminal with a firm source, and the loss of carrier plus a reduction in voltage on at least one phase completes the trip circuit at the opposite terminal. While this system requires a continuous carrier signal, it does solve a rather difficult relaying problem and allows the use of normal carrier relays at both line terminals with a few additional relay elements to provide complete protection.

G. W. Hampe and B. Wade Storer: We agree readily with Mr. Lensner's desire to keep power line carrier equipment as simple as practical. Compared with some equipment designed to compete with power line carrier, it is simplicity itself.

Specifically, where a wide band receiver and a transmitter in a combination capable of working through 30 decibels is satisfactory, that is what should be used. There have been and probably will be a number of lines where the application is not in question.

However, our present knowledge of the effect of weather conditions and line irregularities, such as taps and cable sections, is meager. The makeup of some present line installations is such that the attenuations and noise levels are high normally and go still higher under abnormal conditions. The newer lines, and the older lines with added taps, need high standard relaying, such as only carrier can offer at the present stage of the art. Thus we find ourselves attempting to solve carrier problems of progressively increasing difficulty. One of our several needs is for increased operating ranges, in which is included a good signal-to-noise ratio.

One way to improve this situation seems to be to increase receiver selectivity. This fortunately falls in line with the need for conserving spectrum space, as Mr. Lensner agrees. Considering the great improvements in receiver component parts made possible during and since the war, a more complex receiver may well be more reliable than the present ones.

Mr. Lensner mentioned the handicap of not having beforehand knowledge of transmission characteristics on long communication channels. The AIEE Carrier Committee is gathering data which should be of substantial benefit in such cases. The writers question, however, whether long hauls at a single carrier frequency are really necessary. Under joint usage the problem can be simplified. By utilizing the well trapped carrier relay channels with repeater points at the ends of the line sections, the effects of attenuation and varying system operating conditions can be greatly reduced. The repeater points can use audio by-passes, provided the number in tandem is not excessive. An application of this type, with one audio by-pass, is now in service on the Commonwealth Edison System, and a second application using two such by-passes has been planned for future use.

Another similar solution has been proposed which would use a nondemodulating carrier repeater (so called for lack of better name). This would repeat the communication signal at a different carrier frequency in the next line section without descending to the audio-frequency level.

In reference to the use of undervoltage relays to remove auxiliary services during fault conditions:

This is quite feasible in some applications, but to do so for the open circuit breaker condition requires an adequate set of potential transformers or potential devices on the line itself. In many cases bus, not line, potential transformers are used. In such cases there may be only a single-phase potential device for synchronizing and back-feed indication. In other cases, the line potential transformers are "VV" connected, and their secondary output gives no clear-cut indication of the existence of a single line-to-ground fault.

If the auxiliary services are at all important, it is desirable to continue them past an open circuit breaker or even with the line dead. This means that an additional device, such as a fixed time relay, must be used to restore the auxiliary services. It would be desirable to have a solution to the control problem which would be more generally applicable and simpler. One should not lose sight of the voltage variation principle in setting up carrier relay schemes, however, as Mr. Lensner indicates.

Mr. Asbury indicates that much of the cost of a carrier installation is in the terminal equipment such as coupling capacitors and traps. There is no generally accepted way of dispensing with the coupling capacitor, but it can, of course, be used jointly for various services.

In regard to trap requirements it is necessary to consider the type and location of the line itself, the attenuation and interference problem, and the number and nature of the desired services. On trunk lines, for example, adequate trapping is essential. Economies in trapping should be obtained, not by eliminating traps, but by making greater use of each trap that is required. For example a wide band of frequencies might be trapped by utilizing the trap as a band elimination filter. Likewise, utilization of the coupling capacitor as a wide band-pass filter will provide joint usage with the resulting economies that both the authors and Mr. Asbury feel are necessary. A proposal covering this use of traps and coupling capacitors is presented in a paper scheduled for the AIEE Pacific General Meeting.[1]

On subtransmission lines there may be few desired services other than relaying, and costs must be cut. Traps should be used sparingly. Just how sparingly depends largely on the interference and attenuation problems.

In restating the problems presented in our paper, Mr. Asbury also mentioned cost and maintenance difficulties of filters. To the extent that we have dealt with filters to date, we have found them simple to maintain and quite stable. The objection should be made more explicit.

Mr. Asbury points out that the power companies have generally worked out their interference problems. He states that they can continue to do so, if the band now available between 50 and 300 kc is used. To this we heartily agree.

Mr. Asbury also agreed to the main premise of our paper, which was that power line carrier needs to evolve to extend its application and usefulness. From the relaying standpoint, this includes protection for multiterminal lines, lines supplying tap loads, and lines terminating without high-voltage circuit breakers. The factor of immediate automatic reclosing should be included.

Reference

1. A Proposal for Broad-Band Coupling of Power-Line Carrier Equipment, R. H. Miller. *AIEE Transactions*, volume 68, part II, 1949, pages 1028–31.

Relaying of Transmission Lines from the New Sunbury Generating Station

H. H. GREEN
NONMEMBER AIEE

J. A. G. OEWEL
MEMBER AIEE

O. RAMSAUR
MEMBER AIEE

IN PLANNING the development of the transmission facilities needed for the output of the new Sunbury plant of Pennsylvania Power and Light Company, provision of spare lines has been avoided by placing full dependence on the ability of modern line protective and restorative relaying to reduce to short durations all outages except those caused by relatively rare persistent faults. As a result, the number of lines is being held to those already in existence in close proximity and those required for carrying capacity to certain regional loads.

This paper deals with the functional requirements of relaying in the development of the transmission outlet for the initial 150-megawatt installation at Sunbury, some of the special problems encountered, and the kinds of relaying selected to meet these requirements and to solve these problems. Circuit and elementary diagrams are omitted, on the assumption that relay engineers are familiar with the common types of protective and reclosing relay methods being used.

A brief discussion of the new generating station and the transmission system will provide background for the discussion on relaying.

The New Plant

The new plant is situated on the west bank of the Susquehanna River, about two miles below Sunbury, Pa. The site was acquired many years ago because of its central location in the western part of the system, which was the logical point of the next large power source, and because of the availability of sufficient water, proximity of fuel sources, and so forth. Its ultimate installation has been kept continually in mind in the planning of all new major facilities in that vicinity.

The initial installation comprises two 75,000-kw units, each connected through a 13.8–69-kv step-up transformer bank directly to one of the two sections of the 66-kv double bus, as indicated schematically in Figure 1. These first two units are expected to go in regular service in 1949. A third unit of 100,000-kw capacity is already under way, to be completed in 1951.

Transmission System

The major transmission system is shown in map form in Figure 2. Basically, it consists of a 66-kv network which is fed at dispersed points by generating stations of various sizes, a 220-kv interconnection, and several 66-kv interconnections. Transformer neutrals are solidly grounded at main supply points. This 66-kv network plays the dual role of distribution system as well as transmission system, being tapped at will to supply power to lower-voltage distribution systems and large industrial plants.[1]

All but two of the 66-kv lines tying into Sunbury already existed in the general proximity and are simply being extended into Sunbury from other terminations. New 66-kv lines have been built, however, to Lock Haven and to Millersburg, tying in with existing lines to provide additional capacity required by increasing load. The 132-kv line between Sunbury and the large 220–66-kv substation at Siegfried forms a bulk-power channel between these two major supply points, terminating on the 66-kv bus at each end through a 50,000-kva 132/66-kv autotransformer. The 132-kv line initially is made up of a new section of 220-kv construction and an existing section of 132-kv construction. Later on, as required by the need for more capacity, the 220-kv construction will be completed all the way and the line then will be operated at that voltage.

Requirements of Transmission System

Basically, of course, the function of a transmission system is to transport power, for which it must have adequate capacity from the standpoint of conductor loading and voltage drop. In this case, the transmission is mainly to two regions: the main part of the system, lying eastward from Sunbury; and the northwestern portion, the load in which will be taken over largely by Sunbury. The one 66-kv line to the south will not only take over the load en route, but will bolster supply to the southern region.

All of the connections to Sunbury shown in Figure 2 have been governed either by the proximity of existing lines and the ease of tying them in, or by the need for additional transmission capacity to carry normal peak-load requirements in the regions to be served.

As to reliability, every reasonable effort is being made to provide lines that will stay out of trouble. New lines and extensions are of modern construction, resistant to lightning and sleet and located on private rights of way remote from highway and other such hazards. The older lines are being reinforced to improve their resistance to lightning and sleet.

The record of experience with lines of these types shows predomination though infrequent occurrence of inherently nonpersistent faults and a very rare occurrence on peak of persistent faults due to mechanical damage of some sort. The grouping of lines is such that momentary outage of any one line will not affect

Paper 49-153, recommended by the AIEE Relay Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20–24, 1949. Manuscript submitted February 18, 1949; made available for printing May 23, 1949.

H. H. Green, J. A. G. Oewel, and O. Ramsaur are all with Pennsylvania Power and Light Company, Allentown, Pa.

appreciably any but those customers fed directly from the line itself. The cost of providing spare lines to avoid possible drastic measures for those extremely rare occasions when a long outage occurs on peak is not justified.

Functional Requirements of Relaying

Recognizing that no over-all line relaying can alleviate situations arising from persistent faults, the functional requirements of relaying for these lines are:

1. To clear all nonpersistent faults due to lightning and other transient causes quickly enough to avoid their becoming persistent because of arc damage at the point of fault. Experience has indicated that this can be accomplished with standard 8-cycle circuit breakers and fast protective relaying.

2. To reclose the terminal breakers quickly enough to give reasonable assurance that the faulted line will have been restored to service before another of its group is faulted. Experience with other 66-kv tie lines, including double-circuit lines, indicates that, in practically all cases where the faults were not simultaneous at the start, a reclosing time of two to three seconds is adequate. Even when outages are simultaneous, at least one line usually is restored successfully. An additional objective, of course, is quick restoration of service to loads tapped off the lines.

From the standpoint of channel reliability, it is recognized that persistent faults caused by mechanical damage are very infrequent and that they seldom result in simultaneous outage of two lines in a group, because their cause usually is localized. On the other hand, nonpersistent faults usually are caused by lightning, which occurs during a relatively few hours a year, so that outages of two lines are more apt to occur close together. It is for the latter type that relaying achieves its real aim.

The problem of stability does not enter into the need for fast relaying in this case, as the ties to the main network are stiff enough to give adequate synchronizing power under the assumed conditions.

Based on past experience on 66-kv tie lines with fairly fast protective relays and sequential reclosure of the 2 terminal circuit breakers within 3 seconds, it is reasonably certain that the transmission requirements enumerated will be satisfactorily met with the medium-speed protective and reclosing relaying about to be described, because:

1. There are comparatively few trip-outs of modern or modernized lines.

2. Most faults are transient in nature, so that reasonably fast clearing will keep the line ready for successful reclosure.

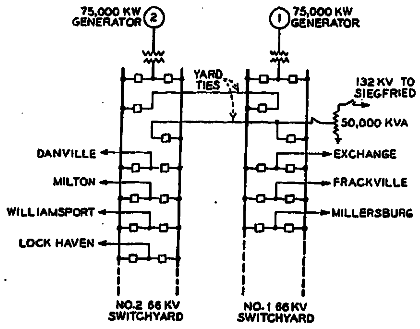


Figure 1. One-line diagram of initial installation at Sunbury

3. Since there are no double-circuit lines around Sunbury, there is little chance of simultaneous outages.

4. Even though two lines should go out together, it can be expected that at least one will be successfully restored within a short enough time to prevent serious results.

It is pointed out that fast tripping and reclosing of lines also gives improved service to tapped loads, which include some important customers and distribution supply substations.

Line Protective Relays

As might be surmised from what has been said about fast clearing of all faults, carrier-pilot relays are a practical requirement for all the lines leaving Sunbury. Only pilot relays can meet the requirements of simultaneous tripping of both terminal circuit breakers (or at least instantaneous sequential tripping, as may be encountered on two lines to be mentioned later), to minimize fault damage and to permit faster reclosure than would be possible with conventional relays. In fact, pilot relays are inherently fast enough to meet the requirements of fast clearing with circuit breakers of normal speed. This is especially important at the terminals away from Sunbury, where some of the circuit breakers are already in existence. The carrier pilot also provides a channel for emergency telephone communication.

Having decided upon carrier-pilot control of the relays as an essential requirement, there remained only the choice of basic relay types to suit the conditions imposed by the various lines. In this connection, the policy of tapping lines introduced some problems not found on untapped tie lines:

1. The difficulty of reaching faults at the ends of the taps, because of the increase in the apparent impedance seen by the relays at one terminal by current flowing over the tap from the other terminal. The permissible lengths of taps are limited by the fact that relay settings must be high enough, in terms of distance, to reach the tap ends with adequate margin, but, at the same time, must be low enough to permit carrying the maximum expected loads. Where the two limits of settings approach each other, there is also danger that a fault will produce enough current in one terminal to operate a carrier-starting element and send a blocking signal, but not enough current to operate the tripping element at the same terminal and stop the signal. This condition can be overcome by making the carrier-starting element directional, so that it will start carrier only for a fault in the nontripping direction at its terminal, thus establishing the condition that the tripping relays are free to trip the circuit breaker at the terminal where the fault on the tap is recognized, after which the other terminal will trip immediately. In no case encountered is it necessary to protect a tapped transformer, as fuses are used for this purpose.

2. The desirability of avoiding overtripping for faults on the low-voltage feeders from the tapped step-down substations. For very large transformers, a carrier terminal often is required for this, but for the lines covered by this paper, there are no tapped transformers large enough to require it for the type of relaying chosen. (Phase-comparison relays were rejected because certain transformers tapped to the lines were of sufficient capacity to require carrier blocking terminals with this type of relaying.) Consequently, no line has more than two carrier terminals. In this connection, it is pointed out that no attempt is made to avoid tripping the line for faults on the high-tension side of a tapped transformer, because it is expected that the transformer fuse will blow during the tripping time and thus disconnect the fault from the line and permit its immediate reclosure. These faults are so rare that special equipment to prevent line tripping is not warranted.

3. The necessity for opening low-voltage circuit breakers on transformers tapped off the lines, the low-voltage sides of which tie in to other transformers tapped off other lines. This back-feed into 66-kv line faults requires slowing up line reclosure long enough to open the ties, in addition to creating the problem of when and how to open the low-voltage circuit breakers. The problem is further aggravated by the fact that all of the tapped transformers have delta primaries, so that the only current flowing for a ground fault after the terminal circuit breakers trip is that caused by the unbalance in line-charging current.

For clearing phase-to-phase faults, review of the above and other conditions and restrictions imposed by the line connections and their maximum loads led to the conclusion that the relays best suited were the 3-element 3-zone impedance type *HZM*, with modified (offset) characteristics.[2] These relays offer a wide range of choice of settings and characteristics, and appear adaptable to any condition likely to arise. Also, the offset characteristic facilitates directional carrier starting.

As is customary for carrier-controlled

relays, all three elements will trip independently of carrier control, after their respective time settings. Zone *1* will be set to cover about 80 per cent of the line and will trip instantaneously through a directional element for faults in this range. Zone *2* will be set to cover the entire line and the taps, plus adequate margin, to trip through a directional element for faults in the far-end zone after a suitable co-ordinating time delay. Zone *3* will have its impedance characteristic almost completely offset, to make it largely directional and thus operative only for faults behind the bus and within a short distance out on the protected line; it will be set to outreach the second-zone element at the far end and will trip after a time delay longer than that of the second-zone element. It should be noted that the third-zone element will trip the circuit breaker at the terminal nearest a prolonged external fault, thus keeping the protected line in service to carry its tapped load.

For carrier-controlled tripping, the second-zone time element will be bypassed in the usual manner by the carrier auxiliary relay, to permit instantaneous tripping for faults within reach of the second-zone impedance and directional elements that are not blocked by a carrier signal. Operation of the third-zone element will start the carrier signal, and, being largely directional, it usually will start carrier only for faults requiring blocking, thus avoiding the condition of a blind terminal mentioned previously. Operation of the second-zone elements to trip will stop any carrier signal originating at the same terminal. While the third-zone element will also start carrier for faults within a short distance on the protected line, the second-zone element will definitely overreach it and stop carrier.

The scheme of operation outlined in preceding paragraphs assures instantaneous tripping of both terminal circuit breakers simultaneously for nearly all phase-to-phase faults anywhere on the protected lines. For those few cases where a fault on a tap will not operate the second-zone tripping relay at one end until the other end has tripped, at least instantaneous sequential clearing is assured.

Separate back-up relays for phase-to-phase faults are not being provided, because the relays on each phase operate as conventional 3-zone elements independently of the carrier pilot control, and because the phase relays will be taken out of service only one at a time for test. This is acceptable because full ground fault protection will be maintained at all times, because the phase relays remaining on the other two phases still offer limited back-up for the removed relay, and because relay testing is restricted to conditions of good weather, and so forth, when the probability of a pure phase-to-phase fault on the particular phase with limited protection is extremely low.

For phase-to-ground faults, the carrier-pilot protection is provided by two sets of zero-sequence directional and instantaneous overcurrent elements, one for tripping and one for starting the carrier blocking signal. As in the case of the phase relays, the starting combination operates only for the flow of zero-sequence current away from the line, so that the carrier blocking signal is transmitted only when required to prevent overtripping by the relays at the far terminal and is not transmitted for faults on the protected line.

The directional carrier-start feature is particularly valuable on the lines to Lock Haven and Williamsport, where there are no grounded-neutral transformers and where the main source of zero-sequence current is over the lines from Sunbury itself. For ground faults near Sunbury, there is practically no zero-sequence current in the Lock Haven and Williamsport terminals until after the Sunbury circuit breaker opens. This leads to the existence, at the Sunbury end of each of these lines, of a long zone in which a ground fault would operate a nondirectional starting element at the far end but would not operate the higher-set tripping element, thus blocking tripping by the carrier-controlled relays. With a directional starting element, this condition is avoided; instantaneous sequential tripping of the two terminal circuit breakers will result. While the back-up ground relay also should trip instantaneously for such a fault, it is desirable that the carrier-controlled elements stand on their own feet as far as possible.

Back-up protection for phase-to-ground faults is provided at Sunbury by simple inverse-time induction overcurrent relays with instantaneous attachments. At all terminals away from Sunbury, except at Siegfried, it was necessary to make these relays directional; one direction element was used to supervise both the inverse-time and instantaneous overcurrent elements.

The ground back-up relays are entirely independent of the carrier-controlled relays, so that either can be taken out of service for calibration and maintenance without losing full coverage for ground faults.

All directional relay elements are potential polarized, except the ground relays on the Siegfried line. Current polarization of the ground relays at Sunbury would have been unduly complicated by the double-bus arrangement, which would require special safeguards to insure that relays always would be polarized by a proper source of zero-sequence current. To insure correct potential for polarization of all relays and for other purposes, each line at Sunbury is being equipped

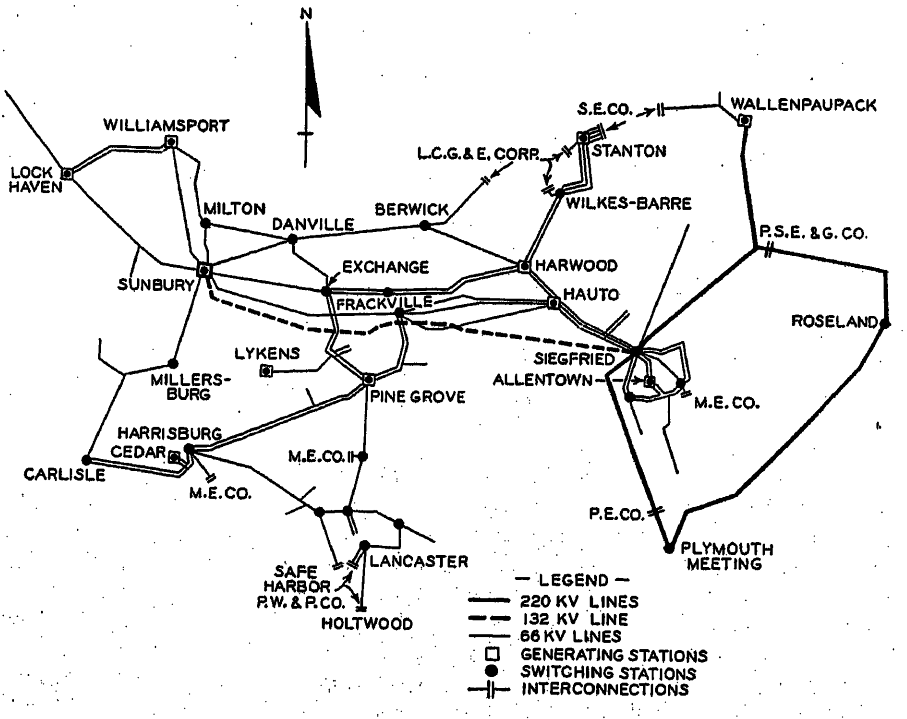


Figure 2. Major transmission network

with a complete set of three 66-kv potential transformers, connected Y-Y with neutral grounded, to supply all required potentials directly from the line itself. This cost little more than other methods, because it eliminates bus potential transformers. Each line does have a manually-operated switch for transferring the relays to another potential source whenever it is necessary to take the normal source out of service. In most cases, the relays on the terminals remote from Sunbury receive potential from bus potential transformers, as none of the adjacent stations except Siegfried has a double bus. The ground relays on the Siegfried line are current polarized. The current is supplied from a current transformer within the delta tertiary winding of the 132/66-kv autotransformer.

Since the 132-kv line to Siegfried is terminated by 66-kv circuit breakers only and a 132/66-kv autotransformer with solidly-grounded neutral at each end, the protective relaying is complicated a little by the necessity of providing complete protection of the terminal transformers. Because the protection offered by the line relays was not considered adequate under all conditions, differential protection for each transformer is provided in addition to the line relays. For this purpose the transformers are equipped with bushing-type current transformers on the 132-kv side; a single-pole 132-kv grounding switch is provided at each terminal to force tripping of the far terminal. The transformer differential relays trip both the terminal circuit breaker and the grounding switch, and block reclosure of the circuit breaker at that terminal.

Back-Feed from Tapped Transformers

To avoid delaying automatic reclosure unduly, it is necessary to open quickly the circuit breakers on the low-tension sides of the tapped transformers where cross-feed exists. So far as is economically practical, it is desirable to do this for phase-to-ground faults as well as phase-to-phase faults, even though the tapped transformers are all delta-connected on the line side and so cannot feed ground current, because the back-feed keeps the line energized after the two main-terminal circuit breakers open and may sustain an arcing ground, preventing successful reclosure. While it is expected that the ground arc will not be sustained in most cases on 66-kv lines of this length, self-extinction of the arc is not being depended upon. For this reason, it is considered good practice to interrupt the back-feed, if it is possible to do so without resorting to expensive equipment for this purpose.

Remote tripping of the back-feed points over the carrier channels may have been practical but would have been expensive, so, after consideration of several other schemes, sensitive reverse-power relays were adopted.

Taking a tip from network relays, sensitive high-speed polyphase directional relays with watt characteristics are being used for this purpose. These relays are given a slight spring bias in the "reverse" direction, so that the contact will stand closed with full potential and zero current. Under normal conditions, there always will be enough load current toward the low-voltage bus to keep the relay open. With the line circuit breakers open, however, the presence of the spring bias, transformer core loss, and, in some cases, other load tapped off the line, will insure prompt closing of the relay as soon as the line circuit breakers open, if it has not closed already because of flow of positive- and negative-sequence currents into the fault.

The watt characteristic of the relay was selected because the wattless component of the reverse current may be either lagging because of transformer magnetizing current or tapped-load current, or leading because of line-charging current. The watt component is therefore the only one that can be depended upon for the proper direction.

For faults near Sunbury, some of these sensitive reverse power relays may operate for faults on other lines. To avoid this, they are given a short time delay by interposing an adjustable auxiliary timing relay, to allow time for the main-terminal circuit breakers to trip. Since the reverse power relay will stand closed with the circuit breaker open, the timing relay is de-energized by a circuit-breaker auxiliary switch, so that it will not prevent reclosure of the low-voltage circuit breaker when the line is restored to service.

Reclosing Relays

All of the new 66-kv oil circuit breakers at Sunbury, and five of those at the far terminals, are pneumatically operated and have a minimum reclosing time of 20 cycles. Those remaining at the far terminals are solenoid operated and have minimum reclosing times of about 60 cycles. Since any of the circuit breakers can be reclosed within the time of two to three seconds set as a limit, the policy is to reclose as quickly as possible with existing circuit breakers and under the particular conditions on each line. This means that reclosure will be fast enough if it is done as soon as it can be certain that the line is de-energized. On the other hand, reclosure is being speeded as much as possible without resorting to special equipment to do so. Therefore, the reclosing time of any circuit breaker is determined by the expected clearing time of the circuit breaker at the other terminal and on any low-voltage ties, plus time for arc deionization.

For reclosing the Sunbury circuit breakers, time is being allowed for sequential tripping of the far circuit breakers, because it is not certain that ground faults near Sunbury always will result in sufficient ground current at the far end to trip properly until after the Sunbury circuit breaker opens. In fact, as mentioned before, sequential tripping is almost a certainty for ground faults near Sunbury on the lines to Lock Haven and Williamsport. A time of only about 10 cycles is allowed for this, which is of little consequence.

Time is being allowed for tripping of the circuit breakers feeding back from low-voltage cross ties after the main-terminal circuit breakers trip, because of the time delay purposely introduced, as mentioned before, and because it is not certain that they will even start timing out before the terminal circuit breakers open. This adds some 35 cycles.

The result is that it is expected that both terminal circuit breakers will be reclosed within 30 cycles on the lines to Millersburg, Lock Haven, and Siegfried, which have no back-feed from low-voltage ties, and within about 65 cycles on all others except the line to Exchange, which will be about 100 cycles because it has two back-feeds that may trip sequentially with respect to each other as well as to the 66-kv circuit breakers.

The reclosing time is varied by setting up the reclosing circuit as for one-shot instantaneous reclosure, but delaying it with an auxiliary relay with adjustable time delay.

Because each line is terminated on two circuit breakers at Sunbury, a selector switch is provided to select the circuit breaker that will be reclosed automatically. If reclosure is successful, the other circuit breaker will be reclosed manually. If the one automatic reclosure is not successful, any further tests also will be manual.

Automatic reclosure of any circuit breaker is blocked by the manual switch that removes carrier control of tripping

It is also blocked by operation of bus differential relays or by manual tripping of the circuit breaker by the control switch. Manual reclosing restores the automatic reclosing feature.

On the low-voltage ties, circuit breaker reclosure will take place after a time delay following restoration of the 66-kv line to service. As on a secondary network, proper conditions for reclosure are indicated by a directional relay so connected that it acts as a phasing relay.

Bus Protection

While bus protection does not come under the heading of line relaying, a brief description of the Sunbury bus protection may be of interest.

Linear couplers were selected for differential protection of each of the four 66-kv bus sections because of their superiority over other conventional methods considered.[3] This method eliminates any possibility of bushing current transformer saturation by the heavy currents to be expected for nearby line faults, and, at the same time, retains the simplicity of a straight differential scheme. Simplicity is highly valued.

The linear couplers are connected by multiconductor control cable, without benefit of shielding, because calculations of induced voltages in the relay circuits for ground faults in the vicinity of the switchyard were reassuring, even when based on pessimistic assumptions such as no spiraling. Actually the cable is spiralled about every 15 inches, so that induced voltage between conductors should be practically zero. Voltages induced along the conductors are equal in all conductors and hence produce no relay current.

Considerable thought was given to the selection of a satisfactory method of supervising or checking the linear coupler circuits. The method adopted was suggested by the manufacturer. Absence of short circuits is checked by switching high-resistance low-reading voltmeters in parallel with the relays to test for abnormal differential voltages, such as would be caused by short-circuited linear couplers. Continuity of the circuits is checked by introducing about 0.5 volt from an external source. This is done, without opening the circuits, by applying the voltage across a resistor of low ohmic value which is left permanently in the circuit. Correct readings of the voltmeters, which are in parallel with the relays, indicate continuous circuits.

The yard tie circuits are protected by percentage differential relays. Since both yard tie circuits are tapped, 3-way differential circuits are employed. The 66–132-kv autotransformer in the Siegfried line at Sunbury is included in the yard-tie differential relay zone of protection. The operation of the differential relays trips the two 66-kv circuit breakers and closes a single-phase 132-kv grounding switch which insures tripping the Siegfried terminal. A similar arrangement is provided for the transformer protection at Siegfried substation. The line relays receive current from the 66-kv current transformers and thus provide limited back-up protection for transformer failures.

References

1. Power System Voltage Control without Feeder Voltage Regulators, N. N. Smeloff. *AIEE Transactions*, volume 67, part II, 1948, pages 1462–6.

2. A Distance Relay with Adjustable Phase-Angle Discrimination, S. L. Goldsborough. *Electrical Engineering* (*AIEE Transactions*) volume 63, November 1944, pages 835–8.

3. Linear Couplers for Bus Protection, E. L. Harder, E. H. Klemmer, W. K. Sonnemann, E. C. Wentz. *Electrical Engineering* (*AIEE Transactions*) volume 61, May 1942, pages 241–8.

4. Linear Couplers, Field Tests and Experience at York and Middletown, Pa., E. L. Harder, E. H. Klemmer, R. E. Neidig. *Electrical Engineering* (*AIEE Transactions*) volume 65, March 1946, pages 107–113.

Discussion

C. E. Asbury (The Commonwealth and Southern Corporation, Jackson, Mich.): This paper describes some interesting relay applications and is perhaps typical of some modern trends in system planning such as tapping high-voltage transmission lines to serve load without the use of high-voltage circuit breakers. This particular plant is perhaps unusual in one respect, in that it has eight major ties into the transmission system. Often in the initial installation of one or two units at a new plant site, only two or three transmission lines connect between the plant and system, and often these are quite long electrically. Such conditions, of course, are brought about because of the availability of plant sites and their location relative to the existing transmission system and load centers.

It is noted that the switching arrangement used at Sunbury consists of two sets of double 66-kv busses, one for each generator. It would be interesting if the authors would point out the advantages of the two sets of double busses with two yard tie circuit breakers between them, over that of one double bus often used for installations similar to this. It also would be interesting to know how future generators will connect to these busses.

This switching arrangement includes two circuit breakers for each line connection, and it is not clear in the paper whether these are normally operated both closed or with one normally open. It would be interesting to know the method of operation and the location and connections of the current transformers used to supply relays at Sunbury. The approximate length of the transmission lines connecting Sunbury with the rest of the system also would be of interest. It appears that some of these lines, especially the two from Sunbury to Milton and Sunbury to Danville, might be reasonably short. We were wondering if these lines are short enough for the application of pilot-wire relays, and if pilot-wire relays were considered.

Different relay engineers would have different solutions to the same problem. Some other points of view on protection for the system outlined in this paper would appear to be of interest. Knowledge of the specific system characteristics would, of course, influence some of the applications.

The paper states that stability was not a problem in this particular application, and that an over-all relay and reclosing time of three seconds was considered to be adequate for transmission lines between Sunbury and the rest of the system. Based on a total reclosing time of three seconds, it appears that modern conventional relaying could have been used without power line carrier resulting in a considerable over-all saving in cost. The communication requirements mentioned could be satisfied with a much more economical power-line carrier of the Simplex multistation type. Such a communication channel probably would not require the use of line traps at a number of points where they were required for relaying.

Modern distance phase relays are available that can have zone *1* set for 90 per cent of the line section, leaving only 10 per cent of the line from each terminal to be protected by zone *2*. Considering the application of modern relays without a carrier pilot channel, it appears that at least one of the main terminals of these lines would trip by zone *1* step distance relays, and, based on three seconds this would leave about $2^{8}/_{10}$ seconds for relaying and reclosing of the other two terminals. The second terminal should trip sequentially on zone *2*, which could probably be set for 15 or 20 cycles, and even if the third terminal required 35 cycles for tripping as mentioned in the paper, this still would leave approximately two seconds for reclosing time. Ground faults probably could be cleared similarly sequentially, by the use of directionally controlled instantaneous relays at the remote terminals from Sunbury and instantaneous overcurrent attachments at the plant.

In this particular application, step distance relays with offset circle characteristics were used. Zone *3* was offset at each line terminal to give essentially the effect of a directional distance element, directional out of the line section at each terminal. It must, of course, be realized that where this arrangement is used about the only function of zone *3* is for carrier starting and that zone *3* provides little back-up protection. One reason for being limited as back-up protection is the interposed feed of short-circuit current from other transmission lines terminating on the same bus. This infeed at each line terminal limits considerably the reach of zone *3* as a back-up element. This would be especially true for all lines terminating at Sunbury Plant. Such limitation is not placed on zone *3* when it is set to provide back-up protection of its associated line section. Another reason for the limitation of zone *3* as back-up protection, where this method is applied in a system equipped with other distance relays applied in the conventional manner, is that zone *3* of all lines which are set for back-up protection on its particular line section and, therefore, any additional back-up protection such as provided from adjacent carrier having zone *3* set looking out of its line section, is of little benefit. Such an arrangement would provide double back-up protection for some lines and will probably result in zone *3* having the same time delay setting for two or more zone *3* settings at the same substation. If this arrangement is used and a fault occurs in a line section requiring the operation of zone *3* back-up relays, then two lines will be opened at the same substation instead of one. It would be interesting if the authors would point out how zone *3* time delay was co-ordinated with zone *3* of adjacent line sections which may have been equipped with conventional distance relays.

Apparently, the zone *3* elements were offset to look out of the line section at each terminal to avoid starting carrier due to load currents, assuming zone *3* elements had been set to cover the line section, including tapped spurs to loads. It is apparent that with the settings as described in the paper, it is not necessary to start carrier by zone *3* before tripping the line by zone *2*, since zone *3* covers only a small portion of the protected section. With this type of operation, a standard impedance element could have been used at each line terminal to start carrier as long as it was set to reach farther out of the line section than zone *2* at the opposite end of the line. Since it is only necessary to cover the line plus a small margin by zone *2*, such an impedance starting element could have a short setting and can be less susceptible to pick-up on load than zone *2* at the opposite end of the line. This arrangement would permit the use of zone *3* as back-up protection for its own line section.

The paper describes the use of ground relays where one directional element was used to supervise both the inverse and the instantaneous over-current element. It would be interesting to know what type of relays were used for this arrangement and whether or not the instantaneous elements were directionally controlled.

Mention is made of T-tap loads on a number of 66-kv lines which have back-feed from other low-voltage lines. Apparently the other low-voltage sources of these loads are sufficient to carry the loads with the 66-kv line out of service, since high-speed tripping and reclosing is not utilized. It appears that if the load had been such that the emergency supplies were inadequate with the 66-kv line open, which probably will be true in the future as the load increases, it may be desirable to add additional terminals of carrier to provide high-speed tripping and reclosing of these terminals. Application of reverse power relays biased with spring tension to trip on full voltage with no incoming power is an interesting application. Apparently, the system loading and supply is such that there is no back-feed at any of these terminals from low-voltage ties during light-load conditions.

O. Ramsaur: Mr. Asbury asks a number of questions that are quite pertinent to the subject and have probably arisen in the minds of other readers of the paper.

The two sets of 66-kv busses were adopted for physical reasons. This arrangement provides the greatest number of future bays in the available space, greatly facilitates connections to the generators and get-away of the east and west lines without having circuits crossing each other, and minimizes the distance from the switchyard to the control house, which is located between the two sets of busses. It should be noted that the amount of switchyard equipment is practically the same as for one long double bus, because the yard ties serve dual purposes (at present they are used for the 66/132-kv transformer and a 66/4-kv stand-by auxiliary supply transformer; in the future they will be used to terminate two 220/66-kv transformer banks).

The double-bus arrangement, together with the automatic sectionalizing afforded by the yard ties, is required for reliability and for flexibility of operation. Initially all circuit breakers shown in Figure 1 of the paper will be operated closed. As the station grows in capacity, however, it will be necessary to "split" the 66-kv bus to limit short-circuit currents. The double-bus arrangement facilitates a flexible mixture of east and west lines on each of two independent busses, by opening the proper circuit breakers, with provision for transferring to the other bus for outage of any bus section.

Future generation will be connected to a 220-kv bus, which will be tied to the 66-kv busses through transformers, as mentioned before. The current transformers for the line and yard-tie relays at Sunbury are bushing type, located in the bus sides of the circuit breakers. The two sets associated with each circuit are paralleled. The linear couplers for bus protection are located in the line sides of the circuit breakers.

The line lengths vary from 15 miles for the line to Milton to 75 miles for the 132-kv line to Siegfried. Pilot-wire relays were considered for the Milton line; but the carrier-current pilot was selected for its greater reliability, there being no great difference in over-all cost.

Speed of fault clearing is usually dictated by the lesser of two times: (1) that required to avoid instability and (2) that required to avoid line damage. It was the intention of the authors to bring out in the paper that, since there is no problem of instability, the aim of fast fault clearing is to prevent line damage. Also, since the allowable outage time of two or three seconds is comparatively long, there is plenty of time left for reclosure, even after waiting to disconnect the line from sources of feedback.

Considering co-ordination with existing relays on adjacent circuits, neither conventional nor impedance relaying would have been fast enough for safety from line damage, especially for faults in the end zones away from Sunbury. Instantaneous ground relays would not operate sequentially, because there is practically no increase in current at Sunbury after the far-end circuit breaker opens. Carrier-current relaying was required, therefore, to avoid line damage; and emergency communication was mentioned only as a by-product, although it is considered quite valuable.

It is true that the reversed zone *3* elements offer only partial back-up protection for faults on either the protected line or other lines, because of the interposed currents mentioned. For back-up coverage of the protected line, it is believed that little would be gained by using zone *3* in the conventional manner, however, because failure of the zone *1* and zone *2* elements to trip is almost always due to failure of something common to all three elements—open trip coils, open current circuits, loss of polarizing potential, loss of tripping potential, stuck timers, faulty auxiliary switches, and so forth. Rarely can failure to trip be attributed to something associated only with a particular relay element. Also, it is seldom that a fault does not involve ground current; so that the ground relays usually back up the phase relays anyhow. There is little chance that a pure phase-to-phase fault will occur at the same time that failure to trip is due to something peculiar to one relay element. And back-up coverage of adjacent lines by the reversed zone *3* is greater for the same setting than a conventional zone *3*. This is important where coverage is limited by ability to carry load.

Since the coverage of the reversed zone *3* elements is less than that of the instantaneous relays on adjacent line sections employing conventional relays, there is no problem of co-ordination. Nor would there be any if zone *3* were used in the conventional manner, for the same reason. Where adjacent lines have distance relays, it is assumed that if an adjacent zone *2* does not trip, then zone *3* also will not, for the reasons mentioned; and again there is no need for co-ordination. The use of a standard impedance relay to start carrier in the manner described by Mr. Asbury should be satisfactory, although it would add three impedance elements at each terminal.

Directional supervision of both instantaneous and inverse-time ground relays is accomplished with two standard relays. One is a combination of a high-speed directional element and a directionally controlled inverse-time element in one case. The other is an instantaneous plunger-type relay, which trips through the directional element in the first relay, an extra stud having been provided for this purpose. The instantaneous element is not directionally controlled. Each of the low-voltage sources of loads with backfeed is able to carry the load alone, the backfeed resulting from 2-way supply to these important loads. As the loads increase, additional transformer capacity will be provided; but this capacity is not expected to be large enough to require carrier-current terminals. No backfeed under light-load conditions is expected. Should it occur unexpectedly, the reclosing relay set-up inherently provides for holding the circuit breaker open until voltage of the proper magnitude and phase angle to carry load is again established; and, if the other source fails, the load will be transferred.

A Thermal Converter for Telemetering and Totalizing

WILLIAM C. DOWNING, JR.
MEMBER AIEE

JUST 20 years ago, Professor Paul M. Lincoln described a "thermal converter" for use in the telemetering and totalizing of large power loads.[1] At that time the installations employing this instrument were less than a dozen in number. Today the thermal converter is generally accepted as an important primary element in the measurement and control of a large portion of the power systems of this country. A number of these installations have been described in *Electrical Engineering* and elsewhere.[2-8]

The thermal converter may be described briefly as an instrument which generates a direct electromotive force accurately proportional to the power flowing in the a-c circuit which it is connected to measure. The voltage so generated may be transmitted readily over considerable distances and recorded or indicated on a potentiometer for telemetering; the output circuits of two or more converters may be connected in series to totalize the power from a number of sources. Since only the series addition of direct voltages is involved, the circuits totalized may differ in types or frequencies, and loads may be subtracted, as well as added, in attaining a record of total load.

In principle, the thermal converter comprises an electric circuit in which the difference of the power generated in two heaters is proportional to the power measured. The temperature difference so produced is measured by thermocouples, the voltage from which is thus a measure of power. In addition to the measurement of power, the thermal converter may be adapted to the measurement of reactive power, power factor, current, or voltage.

Types of Thermal Converters

The two types of converters commercially available may be classified according to the means by which heat is transferred from the heaters to the thermocouple junctions. In the "indirectly-heated" type,[1] the a-c heater circuit is insulated electrically from the thermocouple circuit but is in close thermal contact with it. In the "directly-heated" type,[9] a common circuit is provided for heaters and thermocouples, separation of the a-c and d-c components being obtained by a bridge circuit which, properly balanced, eliminates the alternating voltage from the output circuit.

In the indirectly-heated type, the interposing of electrical insulation inevitably results in some temperature drop between heater and thermocouple junction and in a delay in the response of the thermocouple temperature to a rapidly changing load. Nevertheless, the time of response of the commercial thermal converter of this type, defined, in the usual manner for thermal meters, as the time required for the output to reach 90 per cent of its ultimate value, is approximately 4 seconds. In the directly-heated type of thermal converter, on the other hand, where the heat is generated directly in the thermocouple circuit, the speed of response depends chiefly upon the dimensions and material of the wires composing the thermocouples; a response time of less than 1/2 second readily is obtainable.

In the majority of installations it may be questioned whether the faster speed of response is of more than academic interest, since, where load changes occur only as small increments of the total load, the difference in the record obtained may be barely discernible, but, in control applications, the faster response does have marked advantages. This means, in general, that a high-speed electronically-balanced potentiometer recorder is required to take full advantage of the faster response of the directly-heated-type thermal converter.

Against the disadvantage of the indirectly-heated type of converter in response speed, there may be considered its advantage of complete freedom from a-c component in its d-c output circuit, due to the electrical insulation between circuits. In the directly-heated type described, however, alternating voltages exist simultaneously with the direct voltages in the thermocouple circuit, and precise balancing must be attained to reduce the alternating voltage component to a value which can be tolerated by the receiving equipment. While the functioning of a mechanically-balanced potentiometer is not adversely affected by a considerable a-c component, the electronically-balanced potentiometer, in which the advantage of faster converter response is best realized, may be rendered sluggish in its action by an alternating voltage component of only a few per cent of the magnitude of the direct voltage output.

In order to combine the advantages of efficiency and speed of response of the directly-heated type of converter with the freedom from a-c component of the indirectly-heated type, and at the same time to provide facility of adjustment and manufacturing control of characteristics, a new thermal converter recently has been developed.

In this converter each thermocouple junction is in direct contact with its associated heater, but each heater circuit is isolated by means of multiple-winding potential and current transformers; since but one point of contact exists between the thermocouple circuit and each of these isolated heater circuits, the development of an alternating voltage in

---

Paper 49-159, recommended by the AIEE Joint Subcommittee on Telemetering and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20–24, 1949. Manuscript submitted March 18, 1949; made available for printing April 29, 1949.

William C. Downing, Jr. is with the Sangamo Electric Company, Springfield, Ill.

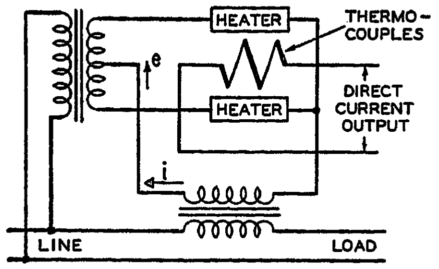


Figure 1. Basic thermal converter circuit

the output circuit is effectively eliminated.

Basic Circuit of New Converter

The basic circuit of any thermal wattmeter is illustrated, for the case of an indirectly-heated converter, in one of its simpler forms in Figure 1 (for simplicity only two thermocouples are shown). Two heaters of equal resistance, R, are supplied in series from the low-voltage secondary of a potential transformer whose primary is excited from the line, so that, due to voltage, e, alone, a current, $e/2R$, circulates through the heaters. A current transformer, excited by line current, is connected through a center tap in the potential-transformer secondary to supply the heaters in parallel so that, due to current, i, alone, current $i/2$ passes through each heater. In one heater these currents are additive and, in the other, subtractive; one heater, accordingly, develops heat at a rate proportional to $(e/2R+i/2)^2R$ and the other heater, $(e/2R-i/2)^2R$. The difference in the power developed in the two heaters is ei, or is proportional to the instantaneous power of the circuit measured; the temperature difference, measured by the thermocouples associated with these heaters, is thus a measure of the average power.

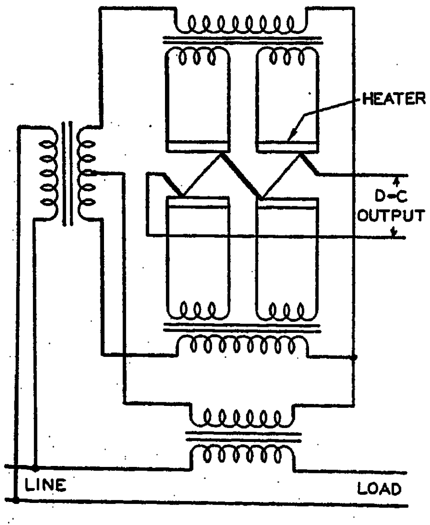


Figure 2. Basic thermal converter circuit with isolating transformers to permit direct contact between heaters and thermocouples

In the foregoing example it is obviously impossible to connect the thermocouple circuit directly to the heater circuit at more than one point. If, however, each heater is replaced by an isolating transformer with as many separate secondaries and heaters as there are thermocouples, an equivalent circuit is obtained, as shown in Figure 2, in which each thermocouple junction may be in direct contact with its associated heater.

In the new thermal converter, the two extra transformers introduced in Figure 2 are eliminated by the use of multiple-winding potential and current transformers, as shown in Figure 3. For each thermocouple junction there is an associated heater and a completely isolated secondary winding on both the potential transformer and the current transformer. Each heater circuit consists of a heater in series with a potential-transformer secondary coil and a current-transformer secondary coil; in half of the heater circuits, the polarity of the connection between the potential- and current-transformer secondary windings is such that the effects of line voltage and line current are additive, while in the other half the polarity is reversed to provide a subtractive effect. The general condition, then, that one set of heaters is heated proportionally to $(e/R+i)^2$ and the other to $(e/R-i)^2$ is met as before.

It will be noted that the series interconnection of potential- and current-transformer secondary coils of Figure 3 introduces no adverse inductive effects, such as might produce appreciable phase-angle errors in the resultant measurement, since the current transformer represents a noninductive burden to the potential-transformer output, and, conversely, the potential transformer appears noninductive to the current-transformer output. For example, if potential only is applied to the converter, all circuit constants being equal, the magnetomotive force resulting in the current transformer due to the circulating current flowing through the coils of additive connection will be exactly balanced by the circulating current flowing in the opposite direction through the subtractively-connected coils.

The single thermocouple, with each of its junctions in contact with a heater, also may be replaced, without affecting the principle of measurement, by two opposing thermocouples, each measuring the temperature difference between a heater and some common reference point. The use of opposing thermocouples in pairs, instead of the single thermocouple, offers structural advantages and reduces the thermocouple junction temperatures without affecting the temperature difference between junctions.

Thermocouple Construction and Characteristics

The construction of the basic thermocouple-heater unit is illustrated in Figure 4. The thermocouple consists of #38 molybdenum wire butt-welded to #33 constantan wire. The heater, of #33 constantan wire also, is welded at right angles to the thermocouple wire on the constantan component close to the thermocouple junction. The cross thus formed is in turn welded to four terminals, so located that the distance from junction to terminal is 0.125 inch for the constantan wires and 0.225 inch for the molybdenum wire. The terminals are in intimate thermal contact with, but electrically insulated from, the plate in which they are mounted; the plate, in turn, being of high thermal conductivity, insures that all terminals are maintained at substantially the same temperature and functions as a "heat sink."

To avoid conductive pickup of alternating voltage from the heater circuit by the thermocouple, the ideal junction between heater and thermocouple should be a point contact. Such a contact obviously would be undesirable from the point of heat transfer, as well as unattainable, but it follows that the cross weld between heater and thermocouple, and the butt weld between the components of the thermocouple, must be symmetrical to prevent any component of alternating voltage along the path of the d-c circuit, By careful control of these welds, it has been found entirely practical to keep this a-c component well within the allowable limits.

When current is passed through the heater, the temperature rises until the

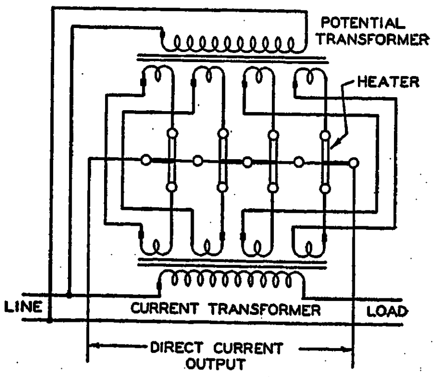


Figure 3. Basic circuit of new thermal converter

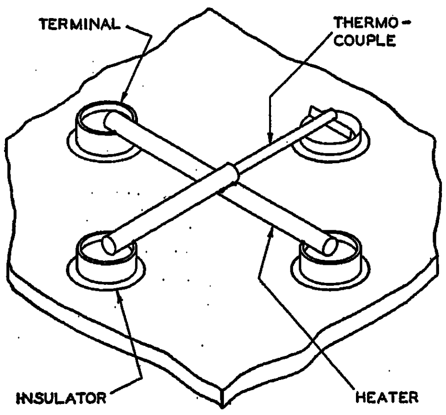


Figure 4. Heater-thermocouple unit

heat loss by conduction and by convection (radiation being negligible) balances the heat input. The "cold junctions" of the thermocouple at the terminals are held, by their close thermal association with the heat sink, at the reference temperature, while the "hot junction" approaches the maximum temperature of the heater.

The rate at which the temperature of the thermocouple junction rises—and, therefore, the speed of response of the converter—depends in general upon the ratio of heat storage capacity of the system to the rate of heat loss from it. If only the heat loss by conduction, which constitutes the major portion of the total heat loss, is considered, it will be seen that for any given materials the lengths of the heater and thermocouple wires become the most important factor in speed of response, since both the heat storage capacity and the conductive heat loss vary directly with the cross section of the wire. Faster response means shorter lengths; this in turn involves either a smaller cross section or higher conductive losses. Practical design, then, indicates a compromise between losses and speed of response such that the response is limited to that which is necessary to, and which can be utilized by, the equipment operating from the thermal converter. With the thermocouple unit described above, the output voltage follows closely an exponential curve, reaching 90 per cent of its ultimate deflection in approximately one second; with this response speed, the potential loss at rated voltage is limited to 2.4 watts and the current circuit loss at rated current to 0.9 watt per element.

Since the accuracy of the thermal converter requires that the output voltage must vary proportionally with the load measured, the electromotive force developed in any thermocouple pair must be proportional to the difference of power input to the heaters associated with that pair. It also may be shown that, if this proportionality is to be maintained for all conditions of voltage, power factor, and load (including reversal of load), each individual couple must generate a voltage proportional to the power developed in its associated heater.

The proportionality of power input to voltage generated in a thermocouple-heater unit depends upon the relation between the power input and the temperature difference between thermocouple junctions, and upon the relation between this temperature difference and the thermocouple voltage. If either of these relations varies from strict proportionality, the other relation must vary in the same degree, but in the opposite manner, if the output voltage is to vary directly with the power input.

In a constantan-molybdenum thermocouple the electromotive force increases more rapidly than the temperature difference. This factor is compensated, however, by the tendency for the temperature difference to increase less rapidly than the heat input. This latter factor is the combination of several effects: the heat loss by conduction through the constantan and the heat loss by convection both increase more rapidly than the temperature difference, whereas the heat loss by conduction through the molybdenum rises less rapidly than the temperature difference.

The temperature coefficient of thermal conductivity of molybdenum, as for most pure metals, is negative (that is, the conductivity decreases with rising temperature), but the coefficient is positive for constantan, as is characteristic of alloys in general. This difference provides a means for controlling the over-all relation between heat loss and temperature difference, by so varying the dimensions of the two materials as to change the proportion of heat flow through them. With the present dimensions, for example, the conductive heat loss through the molybdenum is roughly 1/2 that through the constantan, and the resultant load curve of the thermal converter is linear within ±1/4 per cent. If the diameter of the molybdenum thermocouple wire were increased and the diameter of the constantan wire decreased, the net heat loss might be left unchanged, but the proportion of heat flow through the two materials would be altered; in such an example, then, the increase in proportion of heat flow through the molybdenum would cause the converter output voltage to rise more rapidly than the measured load.

Adjustments

A schematic diagram of a complete, single-element, thermal converter is shown in Figure 5. Each such unit is complete in itself, and as many identical units are employed as are necessary for the type of circuit to be metered. For the measurement of 3-phase, 3-wire circuits for example, two single-element units are provided in a single enclosure.

Where a number of thermal converters are connected in series for totalizing, if the instrument transformer ratios of the various circuits to be totalized differ, the maximum output ratio is specified for the circuit having the highest instrument-transformer ratio. The output ratios of all other converters are reduced propor-

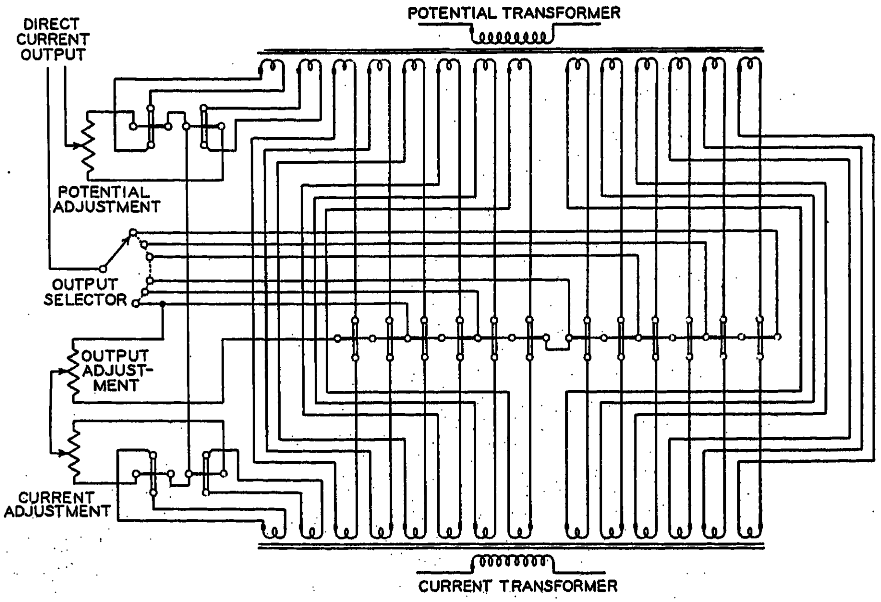


Figure 5. Schematic wiring diagram of single-element thermal converter

tionally to the ratios of the instrument transformers with which they are used, so that the millivolt output per kilowatt of primary power will be the same for each converter. Means should be provided, therefore, for the continuous adjustment of the output ratio from zero to the maximum output of the instrument.

Each converter element includes, for the measurement of power, six thermocouple pairs permanently connected in series. The external circuit is connected into this string of thermocouple pairs through a selector switch, in such manner that any number of thermocouple pairs, from six to one, may be included in the output circuit. A small voltage divider, permanently connected across the final thermocouple pair, provides for fine adjustment of the output ratio over a range of 1/6 of the maximum output ratio; this, in conjunction with the selector switch, permits continuous adjustment of the output ratio to any value up to the maximum rating of 60 millivolts per kilowatt for a 120-volt 5-ampere converter.

In addition, means must be provided for balancing out any errors which may exist due to dissymmetry of the input or output circuits. It may be shown (see the Appendix) that differences of heater resistances, or of the "efficiency" with which heat is translated into voltage in various thermocouples, result in an equation of the form

$$\text{Output voltage} = \Delta V = KEI \cos\theta + K_eE^2 + K_iI^2 \qquad (1)$$

The E^2 and I^2 terms, while small compared to the power term $EI \cos\theta$, nevertheless represent errors for which correction must be made if accuracy is to be attained. At no load, but rated voltage, for example, an appreciable E^2 term would cause an incorrect zero indication on the potentiometer.

The corresponding condition, of current but no voltage on the converter, would of itself be of no practical significance; however, when superimposed upon the main term, $EI \cos\theta$, of the expression for electromotive force, an appreciable I^2 term would result in load-curve distortion, power-factor errors, and inequality of output ratio on forward and reverse power flow.

These errors may be corrected readily by the inclusion, in series with the output circuit, of adjustable sources of direct voltage, proportional, respectively, to E^2 and I^2, which may be set to be exactly equal and opposite to the error terms. Two thermocouples, with heaters supplied from the potential transformer, are additively connected in series, across a small adjustable resistor connected as a voltage divider; by varying the position of the resistor brush, the voltage between it and the connection between the two thermocouples may be caused to vary in either direction from zero. A similar voltage divider and pair of thermocouples, heated by the current transformer only, provides a voltage proportional to I^2 which can be adjusted in either direction. In adjusting the thermal converter, then, the E^2 voltage divider is adjusted until, with rated voltage but no load, the output of the converter is zero, and the I^2 voltage divider is adjusted until the output at some load is the same in magnitude as, but of opposite polarity to, the voltage obtained when the same load is applied in the opposite direction.

General Construction

The thermocouple and heater assembly of a single-element thermal converter is shown in Figure 6. The six pairs of thermocouples which comprise the main output circuit are in the two center rows, while the four additional couple units are provided for E^2 and I^2 correction. The terminals, extending through the mounting plate, provide studs for soldering the external connections for the input and output circuits. This assembly, with its cover, is electrically insulated from, but in close thermal contact with, four aluminum studs which support it in the base. By providing this additional insulation between the output circuits and ground, the level of the insulation between the thermocouple terminals and their mounting plate may be reduced to that necessary to insulate between parts of the thermocouple and heater circuit.

The potential transformer is of conventional shell-type design, with 12 2-turn secondary coils for supplying heaters of the power group of couples and two 1-turn secondary coils for supplying the E^2 thermocouple heaters. The current transformer is of the core type and is made of a high permeability alloy in order to provide adequate ratio and phase-angle characteristics. In order to take full advantage of the high permeability of the core material, solid laminations (without gaps) are employed, and the primary and secondary coils are wound on the assembled core. Twelve 8-turn secondary coils provide for supplying heaters of the power group of thermocouples; two 6-turn secondary coils, for the I^2 heaters. The primary coil is wound on

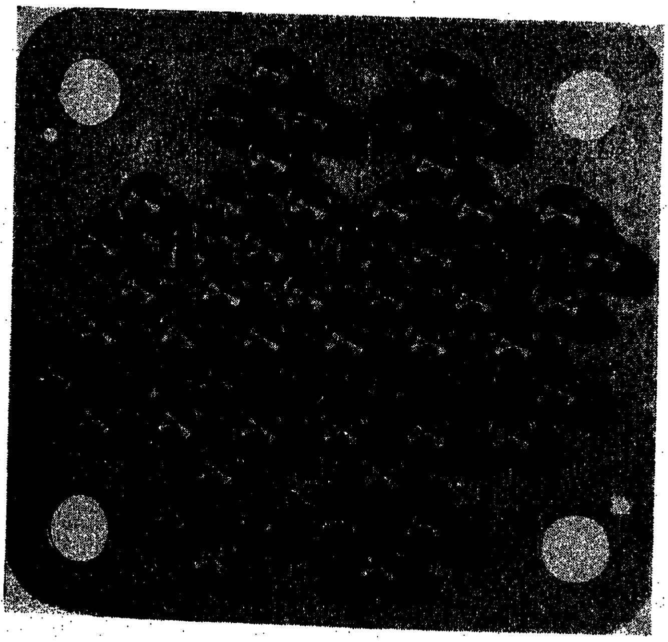
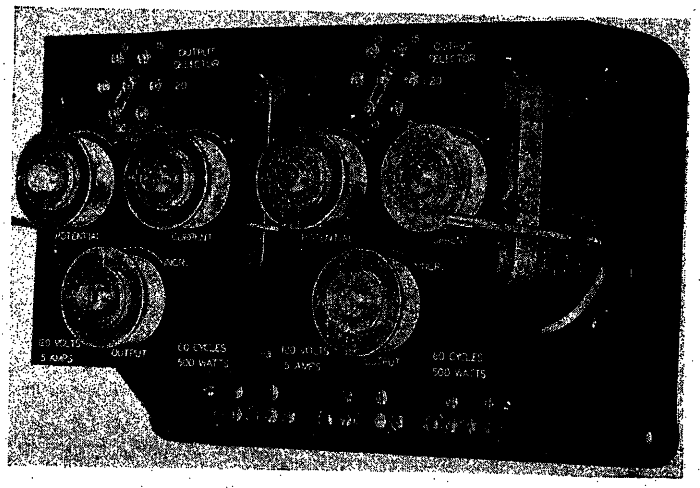


Figure 6 (left). Thermocouple assembly of single-element thermal converter

Figure 7 (below). Two-element thermal converter, with cover removed



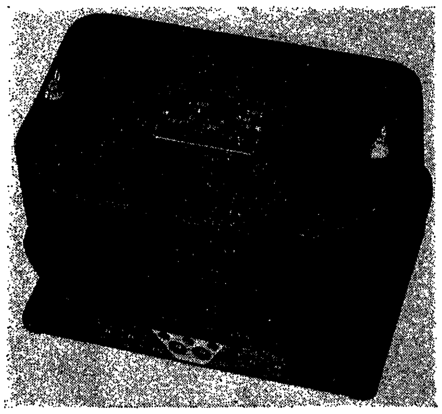


Figure 8. Two-element thermal converter

one end section. The secondary windings are disposed along each leg of the rectangular core. The interconnections between potential-transformer and current-transformer secondary coils are arranged so that the magnetizing effect of current due to potential cancels in adjacent coils of the current transformer. The combined phase angle of both transformers thus may be kept within ±15 minutes throughout the operating range of the instrument, a feature which is of importance in the case of a tie-line application where the power transferred may be largely or wholly reactive.

The voltage dividers for adjustment of potential balance, current balance, and output ratio, consist of six turns of manganin wire supported by a spirally-grooved steatite spool; an adjustment knob, operating on a thread of the same pitch as that of the spool, carries the brush which makes contact, under heavy pressure, with the manganin wire. A knurled locking nut is provided to secure the adjustment. These three resistors, together with a link and studs for selection of the output range, are mounted on a panel in the front of the converter element.

A dust-tight case and cover, with molded-melamine terminal-block construction, encloses the thermal converter. A 2-element converter, complete except for cover, is illustrated in Figure 7, and, with cover in place, in Figure 8.

Conclusion

In output ratio, speed of response, and freedom from a-c component, this new converter represents a compromise between the characteristics of existing converter types, which, it is felt, is distinctly practical in its application to present-day receiving instruments. Early tests indicate that its accuracy under all conditions of load, voltage, or temperature equals or exceeds that of existing converters. Its characteristics are controllable in manufacture and its adjustment facilities permit full advantage to be taken of its inherent accuracy. The design is, in general, adaptable to the measurement of any type of circuit of commercial voltage, current, and frequency rating.

Appendix. Derivation and Illustration of Equation 1

In Figure 9:

e, E = line voltage
i, I = line current
i_1, i_2 = heater currents
R_1, R_2 = heater resistances
p_1, p_2 = instantaneous heater power
P_1, P_2 = average heater power
θ = phase angle between E and I
V_1, V_2 = output voltages
N_1, N_2 = "efficiency" of conversion from power to thermocouple voltage
ΔV = net output voltage
K, K_e, K_t = constants

By Kirchhoff's laws

$$e=i_1R_1-i_2R_2$$
$$i=i_1+i_2$$

From which the heater currents are

$$i_1=(iR_2+e)/(R_1+R_2)$$
$$i_2=(iR_1-e)/(R_1+R_2)$$

The instantaneous heater inputs are

$$p_1=i_1{}^2R_1=(iR_2+e)^2R_1/(R_1+R_2)^2$$
$$p_2=i_2{}^2R_2=(iR_1-e)^2R_2/(R_1+R_2)^2$$

The average heater inputs are

$$P_1=[(I^2R_2{}^2R_1)+2EI\cos\theta\times R_2R_1+E^2R_1)]/(R_1+R_2)^2$$
$$P_2=[(I^2R_2R_1{}^2)-2EI\cos\theta\times R_2R_1+E^2R_2)]/(R_1+R_2)^2$$

The thermocouple outputs are

$$V_1=N_1P_1=(I^2R_2{}^2R_1N_1+2EI\cos\theta\times R_2R_1N_1+E^2R_1N_1)/(R_1+R_2)^2$$
$$V_2=N_2P_2=(I^2R_2R_1{}^2N_2-2EI\cos\theta\times R_2R_1N_2+E^2R_2N_2)/(R_1+R_2)^2$$

The net output is

$$\begin{aligned}\Delta V&=V_1-V_2\\&=I^2\times\frac{R_1R_2(R_2N_1-R_1N_2)}{(R_1+R_2)^2}+EI\cos\theta\times\\&\quad\frac{2R_2R_1(N_1+N_2)}{(R_1+R_2)^2}+E^2\times\frac{(R_1N_1-R_2N_2)}{(R_1+R_2)^2}\\&=I^2K_t+EI\cos\theta\times K+E^2\times K_e\end{aligned}\tag{1}$$

To illustrate the effect of the I^2 term in load-curve distortion, power-factor errors, and difference of output ratio on forward and reverse power flow, let $K=0.1$ millivolt per watt, $K_t=0.04$ millivolt per ampere squared, and $K_e=0$.

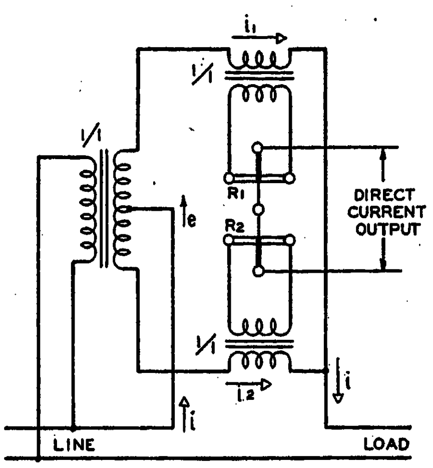


Figure 9. Basic thermal converter circuit for illustrating effect of unequal resistance and conversion efficiency

Case I. $E=100$ volts, $I=10$ amperes, $\cos\theta=1.0$

$\Delta V=100+4=104$ millivolts

Case II. $E=100$ volts, $I=5$ amperes, $\cos\theta=1.0$

$\Delta V=50+1=51$ millivolts

(ΔV should be 52 millivolts.)

Case III. $E=100$ volts, $I=10$ amperes, $\cos\theta=0.5$

$\Delta V=50+4=54$ millivolts

(ΔV should be 52 millivolts.)

Case IV. $E=100$ volts, $I=-10$ amperes, $\cos\theta=1.0$

$\Delta V=-100+4=-90$ millivolts

(ΔV should be -104 millivolts.)

References

1. TOTALIZING OF ELECTRIC SYSTEM LOADS, P. M. Lincoln. *AIEE Transactions*, volume 48, July 1929, pages 775–82.

2. TOTALIZING THE LOAD OF FOUR STATIONS, Perry A. Borden. *Electrical News* (Toronto, Ont., Canada), volume 35, December 1, 1926.

3. LOAD TOTALIZING IN THE NEW YORK AREA, F. Zogbaum. *Electrical Engineering*, volume 53, June 1934, pages 886–89.

4. INTERCONNECTION ECONOMIES THROUGH TELEMETER TOTALIZING, W. J. Lank. *Electrical World* (New York, N. Y.), volume 103, June 30, 1934, pages 948–9.

5. POWER TELEMETERED ON PENNSYLVANIA LINE, E. H. Brown. *Electrical World* (New York, N. Y.), February 29, 1936.

6. TIE-LINE CONTROL AND TELEMETERING ESSENTIAL IN POWER INTERCHANGE, N. C. Pearcy. *Electric Light and Power* (Chicago, Ill), May 1947.

7. TELEMETERING PROVES INVALUABLE SYSTEM OPERATING TOOL, R. G. Meyerand. *Electric Light and Power* (Chicago, Ill.), volume 26, number 4, April 1948, pages 82–8.

8. TELEMETERING—FUNDAMENTALS FOR POWER SYSTEM APPLICATIONS, N. Cohn. *Electric Light and Power* (Chicago, Ill.), volume 26, number 8, August 1948, page 72.

9. A HIGH-SPEED THERMAL WATTMETER, J. H. Miller. *Electrical Engineering* (*AIEE Transactions*), volume 60, January 1941, pages 37–40.

Discussion

G. S. Lunge (General Electric Company, Schenectady, N. Y.): In discussing different types of thermal converters, the author states that for control applications faster response has marked advantages, although seldom of importance as far as the chart record is concerned, in the majority of installations.

In spite of this conclusion, however, the author states that in general a high-speed potentiometer is required to take full advantage of the faster response of the directly heated type of thermal converter.

It should be pointed out that while a high-speed potentiometer is essential to take full advantage of the fast response of any high-speed thermal converter, this potentiometer need not necessarily include recording features.

High-speed response in a recorder for rapidly varying measured quantities will simply result in producing a wide band of ink and hence a less legible chart record if ordinary chart speeds such as 1, 2 or 3 inches per hour are used, as is customary in measurement of electric power, for instance.

Consequently, it is suggested that consideration be given to using normal-speed recorders rather than high-speed recorders, in the interest of producing a more legible and considerably less expensive record than could be secured by the use, for instance, of exceptionally high chart speeds so as to resolve the record sufficiently to make its rapid variations clearly legible.

Wherever possible, the signal required for control purposes should be transmitted directly to the controller, without having to be retransmitted from a recorder. The recorders then are used only for their usual functions, namely, producing legible and continuous records of the quantities measured.

Perry A. Borden (The Bristol Company, Waterbury, Conn.): Although the combination of the thermal wattmeter network with differentially connected thermocouples to obtain a d-c effect proportional to a-c power has been known for 60 years, it is interesting to note that the device described by Mr. Downing represents only the third embodiment of this principle to have been developed to what may be called a practical form for the measurement of commercial power. The earlier two, both mentioned in this paper, were introduced in 1926 and 1940 respectively. In fact, the latter of these was first publicly described by Mr. J. H. Miller in his classical paper[1] at the Summer Meeting of the Institute, held in this place just nine years ago. Despite the wide and important application which has been made of the thermal wattmeter principle in telemetering and totalizing, as well as in demand measurement, our textbooks have given it little attention; and in the most recent (1946) edition of Encyclopedia Britannica the subject is dismissed with a scant two lines.

The Lincoln thermal converter, as pointed out by Mr. Downing, is characterized by a relatively long time interval of response, this being due both to the mass of material whose temperature must follow each variation in the measured magnitude and to the unavoidable thermal insulation between heaters and thermocouples. Also, this device is known to have a high internal resistance, with a correspondingly low power output and consequent limitation of practical measurement of its d-c potential to null methods.

The "Thermoverter," described in Mr. Miller's 1940 paper, is characterized by a rapid response—of the order of 1/2 second for 90 per cent of the final reading—and also by a reasonably high power output, making possible under some conditions the determination of output potentials with a direct-deflecting millivoltmeter of the switchboard type. Since the temperature rise of the heaters tends to be proportional to the square of the current which they carry, instruments of the thermal type are especially subject to damage by abnormal currents, as would result from short circuits or other faults in the line under measurement.

While the high thermal inertia of the Lincoln converter tends to offset this susceptibility to damage, it has been found necessary in the low-inertia unit described by Mr. Miller to meet such conditions "artificially," by designing the current transformers which form a part of the instrument in such a manner that their cores become saturated at a current value somewhat above the working range of the device, thus eliminating from the heaters the flow of a secondary current of objectionable magnitude.

In the light of the foregoing comments, we should have further information on the following two points:

1. As a criterion of practical application under normal conditions of use with a null-type recorder, and also as an indication of its adaptability to use with direct-deflecting instruments, it would be of interest to know the resistance value of the output circuit of the new converter, and also the maximum milliwatts available when supplying a matched external impedance.
2. What provision, if any, is made for protection of the heating elements against damage by abnormal currents?

REFERENCE

1. See reference 9 of the paper.

William C. Downing, Jr.: In reply to Mr. Borden's question as to the d-c circuit resistance of this thermal converter, the major portion of the resistance is in the potential dividers provided for the adjustments and therefore is dependent upon the position of these adjustments. For a single thermal converter element, with output ratio of approximately 30 millivolts for 500-watts input, the resistance of the d-c output circuit would be between 1 ohm and 7 ohms.

This converter is designed primarily for use with recording or indicating potentiometers. For use with indicating instruments of the d'Arsonval type, where a somewhat lower degree of accuracy might be acceptable than with the potentiometer, the E^2 and I^2 adjustments may be omitted and the output ratio adjustment modified so that the d-c circuit resistance might be of the order of 2 ohms.

It is quite true, as Mr. Borden states, that the older type Lincoln thermal converter obtained some degree of protection on heavy overloads of short duration due to its thermal inertia. However, where it has been necessary to meet specifications of ability to withstand 35 times rated current for 1/2 second, we have for a good many years supplied these converters with a saturable reactor shunted across the current element. In this new thermal converter, the saturable reactor is unnecessary as the design requirements of the current transformer, to give adequate phase angle accuracy, automatically give protection to the converter through saturation of the current transformer core. Incidentally, it may be shown, I believe, that the best that either an ideal saturable reactor or saturable current transformer can accomplish is to reduce the heating effect so that it approaches the first power, rather than the square, of the line current.

Heat Flow Ratio as a Design Parameter in Thermal Demand Meters

J. S. NELSON
ASSOCIATE AIEE

THE time of response of demand meters is of critical importance wherever a demand charge is made for electricity. In demand meters of the mechanical, block-interval type the demand interval is usually governed by a gear train and resetting mechanism connected with some synchronous device, such as a clock motor, and response time presents no special problems in design. The response time of thermal meters depends upon less-easily evaluated constants and coefficients. This paper examines these factors and presents equations from which the time of response may be calculated.

Specifically, these equations pertain to a thermal meter designed with opposed, self-heated, bimetallic spirals connected by a shaft. The effect of relative heat flow from the two opposing bimetallic actuators upon the time constant of a thermal demand meter is explored. To accomplish this, the time equation for a device responding to the difference in temperature of two similar bodies connected by a thermal shunt and each supplied with constant heat input is derived for the general case wherein the heat flow from one of the bodies is not equal to that from the other, that is, heat flow ratio other than unity.

This approach has yielded valuable information pertaining to the effect of differential heat escape upon response time. By making use of these equations, a linear-scale meter (for watt demand) and a square-law scale meter (volt-ampere demand) have been designed for the same response time to meet the same meter code requirement without the necessity for different designs of bimetallic spirals. The significance of this becomes clear when it is recalled that response time is defined in the Code for Electricity Meters[1] as time to reach 90 per cent of ultimate indication (in watts or volt amperes). Hence if two meters having different scale distribution are to meet the same code requirements, their physical response times, in terms of angular deflection, must differ correspondingly.

It is hoped that these equations may find application also to the design of integrating or indicating instruments employing thermal storage such as I^2T indicators for welding energy, small panel instruments of the bimetallic spiral variety, and differential thermometers composed of pairs of resistance-temperature detectors, bourdon tubes, thermocouples, or bimetallic actuators.

Although the effects described may be appreciated intuitively, mathematical equations are developed concurrently with the text for precision and clarity. A complete listing of the symbols used in this analysis, together with their relationships, is shown in Appendix I.

While it is not the purpose of this paper to examine electrical rates and metering methods, it is felt that a brief explanation of the purpose and operation of demand meters will be helpful in orienting the general reader with respect to the device serving as the subject for this analysis.

The demand of an installation or system is the load at the receiving terminals averaged over a specified interval of time.[2] This average may be a simple arithmetic average such as obtained with mechanical, block-interval meters or it may be a continuous logarithmic average[3] as obtained with a thermal demand meter of suitable design. In either type of metering, the information used for billing purposes is usually the maximum demand recorded between meter readings, which is combined with the kilowatt-hour consumption in a formula determined by the particular rate structure in effect. Rate structures with penalty clauses based on demand are used in many localities, in an effort to apportion more equitably the cost of providing generating and distributing capacity adequate for peak loads on the system.

The demand interval is usually made generous in length to avoid penalizing a customer unduly for normal heavy starting loads and other momentary peaks. A typical demand interval is 15 minutes.

Registration of maximum demand is accomplished normally by means of a pointer which can be driven only in the upscale direction by the action of the meter. The position of this pointer at the end of the month (or other billing period) is noted by the meter reader, who then resets it to zero or to the current position of the driving means. The driver, or pointer pusher, is itself a pointer, indicating the demand on a system at any instant (averaged over the current demand interval) for the convenience of the customer and for use in checking the operation of the meter.

Watt-demand and volt-ampere demand are frequently the basis for demand charges. The present paper deals with an assembly, the thermal unit, which may be used as the primary detector for measuring either quantity. The associated electrical circuit is designed to supply this unit with electric currents which are functions of the quantity measured.[3,4] The thermal unit combines the heating effects of these currents and translates these effects into a pointer position that is the proper function of the current flowing and the time during which it flows. It is

---

Paper 49-160, recommended by the AIEE Instruments and Measurements Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20–24, 1949. Manuscript submitted March 21, 1949; made available for printing April 29, 1949.

J. S. NELSON is with the Meter and Instrument Divisions, General Electric Company, West Lynn, Mass.

The author is indebted to the authors of references 3 and 5, shown at the end of the paper, whose analyses have been extended to the more general case of unequal heat escape from the two bodies.

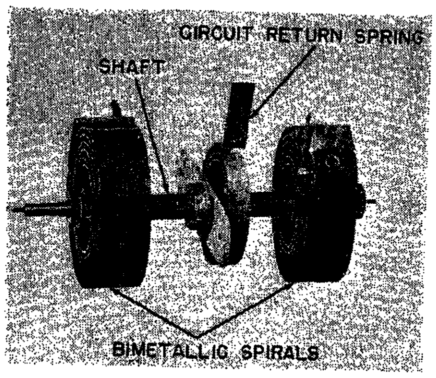


Figure 1. Basic thermal mechanism for demand metering

the purpose of this paper to demonstrate how small changes in thermal geometry may be used to effect differing time functions for this thermal unit and analogous thermal systems.

Analysis

The analysis presented is based on the performance of the thermal unit, Figure 1, which is used for thermal watt demand[3] and thermal volt-ampere demand metering.[4] This unit is composed essentially of two bimetallic spirals mounted on the same shaft in such a manner that increasing temperature in the two spirals produces opposing torques on the shaft. The angular position of the shaft at any time is therefore a function of the difference in temperature of the two spirals. The circuit is such that the current in spiral number 1 is greater than the current in spiral number 2 for up-scale deflection of the associated pointer. The shaft is of thermally conductive material and at a point between the two active spirals is a circuit-return spring connected to a copper lead whose thermal conductance accounts for a significant portion of the total heat loss from the assembly. If this spring is not located at the center point, it will conduct away relatively more heat from the nearer spiral. Similar differences in heat flow may be effected by varying the sizes of the wires connected to the spirals or the characteristics of the insulating shrouds. This relative heat flow, or, more precisely, the relative heat escape, expressed as a ratio, is a useful design parameter.

The utility of heat-escape ratio as a design parameter is not limited to cases of bodies of equal mass, but to avoid complication, M, the heat stored per degree of temperature, is assumed to be the same for the two bodies (bimetallic spirals). The thermal conductance from spiral number 1 to the ambient is denoted by h_1,* and h_2 represents the thermal conductance from spiral number 2. H_1 and H_2* are the rates of heat developed in (or supplied to) the two spirals, respectively, and Q is the thermal conductance of the connecting means (principally the shaft). θ_1 and θ_2 are temperature increments in the spirals resulting from the application of current to the meter. θ_{01} and θ_{02} are the initial temperatures at the beginning of the period under study, expressed in degrees above ambient temperature.

The heat input to spiral number 1, less the heat being lost down the shaft and by other means to spiral number 2, must at any instant equal the heat escaping to ambient added to the heat being stored in the spiral. In symbols

$$H_1dt-Q(\theta_1-\theta_2+\theta_{01}-\theta_{02})dt \\ =h_1(\theta_{01}+\theta_1)dt+Md\theta_1 \qquad (1)$$

and similarly,

$$H_2dt+Q(\theta_1-\theta_2+\theta_{01}-\theta_{02})dt \\ =h_2(\theta_{02}+\theta_2)dt+Md\theta_2 \qquad (2)$$

At some loss in generality, the solution to this pair of equations may be simplified by letting $\theta_{01}=\theta_{02}=\theta_0$. This limits the specific analysis to the case of initial equilibrium of the thermal unit, with pointer indicating zero, and with ambient temperature constant.

$$H_1dt-Q(\theta_1-\theta_2)dt=h_1(\theta_0+\theta_1)dt+Md\theta_1 \qquad (1a)$$

$$H_2dt+Q(\theta_1-\theta_2)dt=h_2(\theta_0+\theta_2)dt+Md\theta_2 \qquad (2a)$$

The substitution in equation 2a of the value of θ_2 and its derivative from equation 1a yields a second-order differential equation in θ_1

$$\frac{d^2}{dt^2}\theta_1+A\frac{d}{dt}\theta_1+B\theta_1=C-B\theta_0 \qquad (3)$$

wherein A, B, and C are shorthand for combinations of the primary design constants of the system (see Appendix I). By primary design constants are meant those constants such as mass, conductivity, thermal capacity, and so forth, which are controllable in design. The general solution may be obtained by the method of Euler and d'Alembert as follows

Let m be such that $\theta_1=\epsilon^{mt}$. Substituting

$$m^2\epsilon^{mt}+Am\epsilon^{mt}+B\epsilon^{mt}=C-B\theta_0$$

from which is obtained the auxiliary or characteristic equation

$$m^2+Am+B=0$$

from which, by the quadratic formula

$$m=\frac{-A\pm\sqrt{A^2-4B}}{2}$$

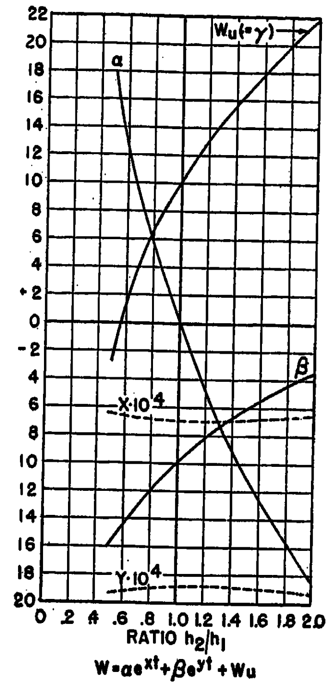


Figure 2. Effect of heat-escape ratio on thermal-unit coefficients

and the general solution may be written

$$\theta_1=c_1\epsilon^{\frac{-A+\sqrt{A^2-4B}}{2}t}+c_2\epsilon^{\frac{-A-\sqrt{A^2-4B}}{2}t}+c_3 \qquad (4)$$

wherein c_1, c_2, and c_3 are constants of integration.

An exactly analogous process yields the solution for θ_2:

$$\theta_2=c_1'\epsilon^{\frac{-A+\sqrt{A^2-4B}}{2}t}+c'_2\epsilon^{\frac{-A-\sqrt{A^2-4B}}{2}t}+c_3' \qquad (5)$$

It will be recalled that the angular position of the shaft and pointer is a function of the temperature difference, W, of the two spirals. Defining W as $\theta_1-\theta_2$, performing the subtraction, and introducing more shorthand to combine the integration constants

$$W=\alpha\epsilon^{\frac{-A+\sqrt{A^2-4B}}{2}t}+\beta\epsilon^{\frac{-A-\sqrt{A^2-4B}}{2}t}+\gamma \qquad (6)$$

rewritten for convenience.

$$W=\alpha\epsilon^{xt}+\beta\epsilon^{yt}+\gamma \qquad (7)$$

The only variables in equation 7 are W and t. This then is an expression for instantaneous temperature difference as a function of time. It remains to evaluate the constants α, β, and γ in terms of primary design constants.

Evaluation of Constants

THE CONSTANT γ

In equation 7, the exponent of ϵ may be

---

* See Appendix I for definition.

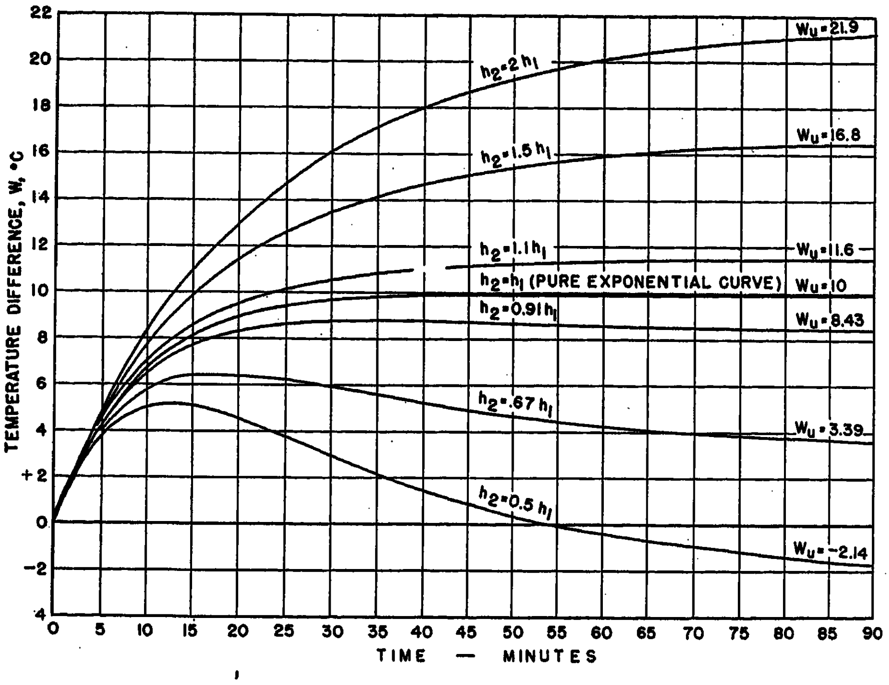


Figure 3. Calculated effect of heat-escape ratio on thermal unit time-performance curve

demonstrated negative for all real values. Hence at $t=\infty, W=\alpha\epsilon^{-\infty}+\beta\epsilon^{-\infty}+\gamma, =\gamma$; or, $W_{t=\infty}=\gamma=$(by definition) W_u, ultimate temperature difference.

From equation 3 and the analogous equation in θ_2, it is easy to find the ultimate values of θ_1 and θ_2, their difference, W_u, being rendered as

$$W_u=\frac{h_2H_1-h_1H_2}{Q(h_1+h_2)+h_1h_2} \tag{8}$$

In the special case where $h_1=h_2=h$

$$W_u=\frac{H_1-H_2}{2Q+h} \tag{9}$$

the same equation derived directly for this case in the literature.[3,5]

The Constants α and β

A new boundary condition is needed for the evaluation of the constants α and β. Before any heat can be lost from a body at room temperature, a temperature gradient must be established. If this hypothetical body has a finite heat capacity and a sudden input is applied, these two factors will, as t approaches zero, govern the rate of temperature rise.

This line of reasoning gives the equations

$$\left.\frac{d\theta_1}{dt}\right|_{t=0}=\frac{H_1}{M} \text{ and} \tag{10}$$

$$\left.\frac{d\theta_2}{dt}\right|_{t=0}=\frac{H_2}{M} \tag{11}$$

from which

$$\left.\frac{dW}{dt}\right|_{t=0}=\frac{H_1-H_2}{M} \tag{12}$$

Letting $W=0$ at $t=0$, from equation 7 is derived

$$\alpha+\beta=-\gamma \tag{13}$$

Differentiating equation 7 yields

$$\frac{dW}{dt}=x\alpha\epsilon^{xt}+y\beta\epsilon^{yt} \tag{14}$$

At $t=0$,

$$\left.\frac{dW}{dt}\right|_{t=0}=x\alpha+y\beta \tag{15}$$

Equating equations 12 and 15 and solving simultaneously with equation 13 yields β and α

$$\beta=\frac{h_2H_1-h_1H_2}{Q(h_1+h_2)+h_1h_2}\times\left(\frac{h_1+h_2+2Q-\sqrt{(h_1-h_2)^2+(2Q)^2}}{2\sqrt{(h_1-h_2)^2+(2Q)^2}}\right)-\frac{H_1-H_2}{\sqrt{(h_1-h_2)^2+(2Q)^2}} \tag{16}$$

$$\alpha=-\frac{h_2H_1-h_1H_2}{Q(h_1+h_2)+h_1h_2}-\frac{h_2H_1-h_1H_2}{Q(h_1+h_2)+h_1h_2}\times\left(\frac{h_1+h_2+2Q-\sqrt{(h_1-h_2)^2+(2Q)^2}}{2\sqrt{(h_1-h_2)^2+(2Q)^2}}\right)+\frac{H_1-H_2}{\sqrt{(h_1-h_2)^2+(2Q)^2}} \tag{17}$$

The exponents x and y were developed previously and their expanded forms are listed in Appendix I.

The fundamental equation of the thermal unit, equation 7, may now be expressed in terms of primary design constants. These constants, namely heat input, heat escape, thermal conductance, and thermal capacity, are all under the control of the designer and when these values are known thermal unit response may be predicted. As has been pointed out, when the heat-escapes from the two spirals are made equal a well-known result is obtained, equation 9, and when the thermal conductance between the bodies is reduced to zero, equation 7 reduces to an expression wherein the terms involving body 1 and body 2 are separated, as would be expected.

Interpretation of Results

The coefficients in the expression for temperature difference are presented graphically in Figure 2, and the performance of thermal units designed with such coefficients is illustrated in the succeeding figures. The shape and disposition of these curves is of greater interest than the numerical values, which are arbitrary.

The Ideal Design Parameter

The ideal design parameter, academically speaking, is one which influences only one characteristic of the device. The designer or experimenter can vary this parameter to produce a desired result with no effect on any other characteristic, and such a parameter is a very effective tool.

At first glance the parameter M, heat capacity, appears to fulfill the requirements for an ideal parameter. It appears only in the exponential terms and not in the expression for ultimate value. This appearance is illusory, at least with regard to the thermal mechanism of Figure 1, for practically it would be very difficult to change the heat capacity without affecting also the heat-escape properties, the electrical resistivity, or the thermal deflection properties of the bimetallic spirals.

On the other hand, the construction of Figure 1 affords a ready means of varying another parameter, namely heat-escape ratio, with relatively small effect upon the other design constants of the thermal unit, provided the changes in heat escape ratio are kept small. Quite significant changes in performance may be effected simply by placing the circuit return spring a short distance to left or right of the midpoint of the shaft. In order to appreciate the advantages and limitations of this parameter as a design tool, it is well to examine the effects of wide variations in h_2/h_1.

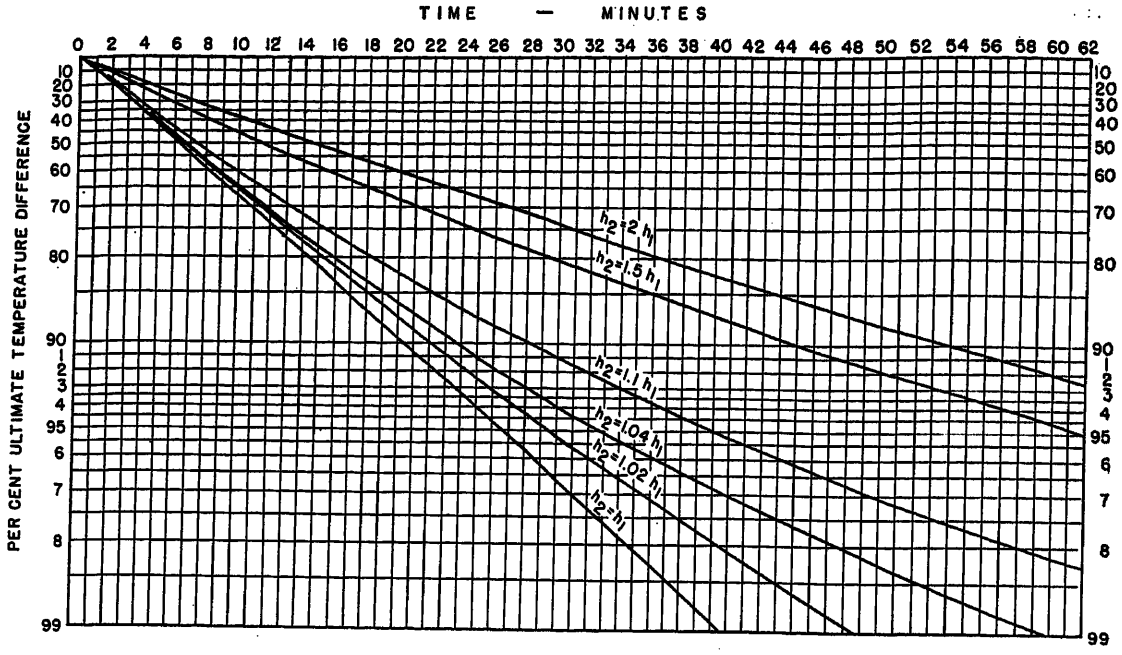


Figure 4. Per cent ultimate temperature difference as a function of time

Effects of Varying the Heat-Escape Ratio

The major effect of changing the design parameter h_2/h_1 is to operate upon the coefficients of the equation, the exponents x and y being very little affected by large changes in heat-escape ratio, Figure 2.

Referring again to Figure 2, it may be seen that selecting a heat-escape ratio other than unity is, to a first approximation, equivalent to adding or subtracting a definite fraction of a temperature increment that follows a different exponential curve from that at unity ratio. By adjusting the proportions of the two exponential terms through appropriate selection of the heat-escape ratio, various overall times of response may be attained, Figure 5. In cases of systems not required to approximate a simple exponential curve, a range of response times varying by a factor of ten or more may be achieved by doubling the heat-escape ratio.

Four categories of heat escape ratio are of interest and will be commented upon:

Ratio unity: A simple logarithmic curve results.

Ratio small: Temperature difference will lessen or reverse with time.

Ratio large: Response time is increased, but time constant is rendered complex.

Ratio slightly larger than unity: Response time may be controlled with the least effect upon other characteristics.

Unity Ratio

Referring to Figure 2, the coefficient, α, of the first term has become zero and the coefficient, β, of the second term is equal numerically, but opposite in sign, to W_u, the ultimate temperature difference. A thermal unit designed with these coefficients will approach the value shown in equation 9 along a simple exponential curve with time (see Figure 3, curve labeled $h_2=h_1$). This performance curve is ideal for a demand meter indicating a continuous logarithmic average.[3]

Small Ratio

At values of heat-escape ratio less than unity the performance curve will overshoot, Figure 3, or dip, a condition to be avoided particularly in demand meters which contain maximum demand pointers. In indicating instruments such a condition is analogous to underdamping and is less serious the shorter the time required to reach the designed indication.

At very small values of h_2/h_1, W_u becomes negative, corresponding to an ultimate deflection in the direction governed by the spiral receiving the less input.

Large Ratio

A thermal mechanism designed with heat-escape ratio equal to or greater than unity will not overshoot, Figure 3. The response time increases with increasing ratio as depicted in Figure 5, which was plotted from the intersection of the curves in Figure 4 with the 90 per cent line. The ultimate difference also increases with h_2/h_1, as shown in Figures 3 and 5, so that no independent adjustment of response time is possible. At values greater than unity the performance does not follow a simple exponential curve, for two exponential functions are involved. In this connection note the departure from linearity of the response curves plotted on semi-logarithmic co-ordinates, Figure 4, at ratios other than unity. If this "trailing off" is great enough, the result is known as "creeping"[3] which is, of course, objectionable in measuring devices.

Ratio Slightly Larger Than Unity

The region between 1.0 and 1.1 or less is the useful area for demand meter thermal-unit design. In this region, a wide variation in response time may be obtained without the objectionable creep that results from the introduction of too large a portion of a second exponential term. As noted before, the unit cannot overshoot when h_2/h_1 is greater than unity. The value of deflection response time is very sensitive to variations in heat-escape ratio near unity, Figure 5, and it is in this region that heat-escape ratio partakes to the greatest extent of the characteristics of an ideal parameter. The change in heat-escape ratio necessary to produce a 10 per cent change in response time from the value at unity ratio will produce only a 4 per cent change in ultimate deflection or sensitivity of the thermal unit. As the thermal conduction path from a spiral through the shaft to the circuit return spring, Figure 1, is an important source of heat escape, a dislocation of the circuit return spring will cause a significant change in heat-escape ratio without materially affecting total heat escape. It may be seen from equation 8 that any change in the material or cross-section of the shaft which reduces h_1 and h_2 will increase the sensitivity or ultimate deflection of the thermal unit. This effect is made more pronounced by the reduction in Q accompanying such a modification. Because of the great influence of shaft design upon the response time and thermal unit sensitivity, it has been possible to adapt one basic thermal unit to the requirements of a thermal watt demand meter and a thermal volt-ampere demand meter by changing only the design of the shaft, which is made of solid brass in the former instance and of stainless steel tubing in the latter. The inherent or angular deflection response time of the volt-ampere demand meter must be materially greater than that of the watt demand meter, because 90 per cent indication occurs at only 81 per cent angular deflection when indication follows a square law. The bimetallic spirals themselves, the circuit return springs, the cups, covers, shrouds, and end shields remain the same for the two units, a standardization of great value in the manufacture and maintenance of this line of meters.

Summary

The role of heat-escape ratio in the performance of thermal units for demand metering has been illustrated. The curves presented were calculated with the use of arbitrary, although reasonable values for the design constants and do not in all cases agree with values chosen for the final designs of the thermal unit. The actual ratio of h_2 to h_1 also is affected by the lower temperature-rise-per-watt at higher temperatures, a radiation phenomenon neglected in the computation of these curves. These curves do, however, give a qualitative picture of the effect of varying the heat-escape ratio.

The use of the heat-flow ratio parameter in the design of thermal units for demand metering or similar thermal systems is subject to two limitations: First, it influences not only response time but also, to less extent, ultimate deflection. Secondly, this method of controlling response time will produce some departure from simple exponential response. In the example cited, one thermal unit was adapted for measuring at two different rates. In this particular case, the increase in ultimate deflection accompanying the increased response time was desirable and the small departure from simple exponential response was negligible in its effect.

The use of these equations, permitting the intelligent variation of the appropriate design constants, should result in the saving of much experimental, development, and testing time in the solution of related thermal problems, particularly in cases where it is desired to adapt one basic assembly to different times of response.

Appendix I. Definitions of Symbols and Their Relationships

H_1 = rate of heat applied to spiral number 1, in gram-calories per second

H_2 = rate of heat applied to spiral number 2 (exclusive of that received by conduction from spiral number 1), in gram-calories per second

h_1 = thermal conductance from spiral number 1 by all paths to ambient (exclusive of the path from spiral number 1 through the connecting means to spiral number 2) (gram-calories per second per degree centigrade)

h_2 = thermal conductance by all paths from spiral number 2 to ambient (gram-calories per second per degree centigrade)

Q = thermal transfer coefficient of shaft, or rigorously, heat transferred from one spiral to the other by all paths (gram-calories per second per degree centigrade)

θ_{01} = initial temperature of spiral number 1, degrees centigrade above ambient

θ_{02} = initial temperature of spiral number 2, degrees centigrade above ambient

θ_1 = instantaneous temperature of spiral number 1 above θ_{01} (degrees centigrade)

θ_2 = instantaneous temperature of spiral number 2 above θ_{02} (degrees centigrade)

M = thermal capacity of each spiral (assumed equal) (gram-calories per degree centigrade)

$$A=\frac{h_1+h_2+2Q}{M}$$

$$B=\frac{Q(h_1+h_2)+h_1h_2}{M^2}$$

$$C=\frac{Q(H_1+H_2)+h_2H_1}{M^2}$$

$$C'=\frac{Q(H_1+H_2)+h_1H_2}{M^2}$$

c_1, c_2, c_3 and c_1', c_2', c_3': integration constants evolving α, β, and γ

W = instantaneous temperature difference = $(\theta_1+\theta_{01})-(\theta_2+\theta_{02})$

$\gamma=c_3-c_3'$, a constant of integration, evaluated as W_u

ϵ = base of Napierian logarithms

$$x=\frac{-A+\sqrt{A^2-4B}}{2}$$

$$=-\frac{h_1+h_2+2Q-\sqrt{(h_1-h_2)^2+(2Q)^2}}{2M}$$

$$y=\frac{-A-\sqrt{A^2-4B}}{2}$$

$$=-\frac{h_1+h_2+2Q+\sqrt{(h_1-h_2)^2+(2Q)^2}}{2M}$$

t = time measured from application of load (seconds)

$\beta=c_2-c_2'$, a constant of integration evaluated as follows:

$$\beta=\frac{h_2H_1-h_1H_2}{Q(h_1+h_2)+h_1h_2}\times\left(\frac{h_1+h_2+2Q-\sqrt{(h_1-h_2)^2+(2Q)^2}}{2\sqrt{(h_1-h_2)^2+(2Q)^2}}\right)-\frac{H_1-H_2}{\sqrt{(h_1-h_2)^2+(2Q)^2}}$$

In special cases

$$\beta\Big|_{h1=h2=h}=-\frac{H_1-H_2}{2Q+h}\text{ (value at unity heat-escape ratio)}$$

$$\beta\Big|_{Q=0}=-\frac{H_1}{h_1}\text{ (value for an isolated spiral)}$$

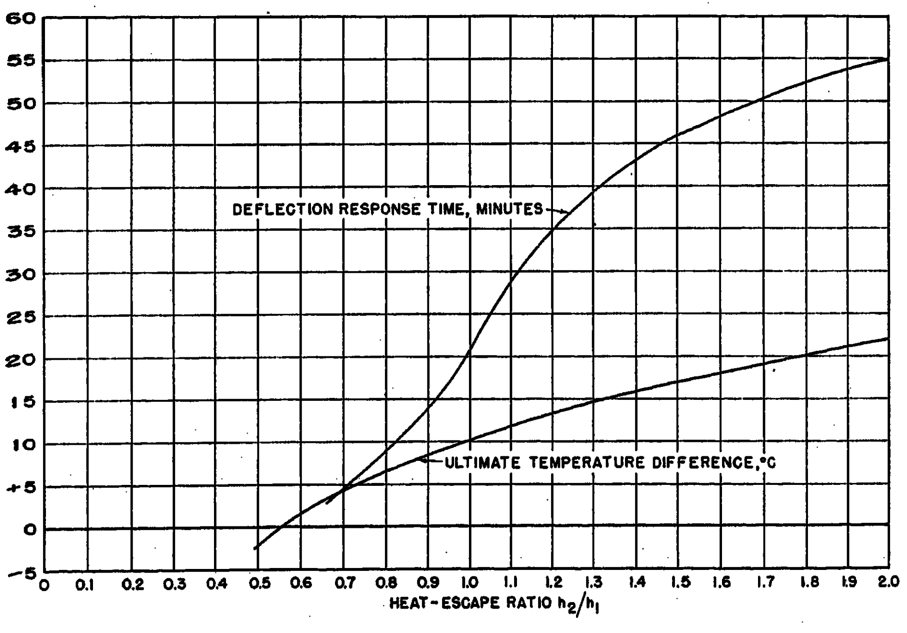


Figure 5. Response time and ultimate temperature difference as functions of heat-escape ratio

$\alpha = c_1 - c_1'$, a constant of integration evaluated as follows:

$$\alpha = -\frac{h_2H_1 - h_1H_2}{Q(h_1+h_2)+h_1h_2} - \frac{h_2H_1 - h_1H_2}{Q(h_1+h_2)+h_1h_2} \times \left(\frac{h_1+h_2+2Q-\sqrt{(h_1-h_2)^2+(2Q)^2}}{2\sqrt{(h_1-h_2)^2+(2Q)^2}}\right) + \frac{H_1-H_2}{\sqrt{(h_1-h_2)^2+(2Q)^2}}$$

In special cases

$$\alpha\Big|_{h_1=h_2=h} = 0 \text{ (value at unity heat-escape ratio)}$$

$$\alpha\Big|_{Q=0} = \frac{H_2}{h_2} \text{ (value for an isolated spiral)}$$

The fundamental equation 7:

$$\left.\begin{aligned} &W = \alpha\epsilon^{xt} + \beta\epsilon^{yt} + W_u \\ &\text{or,} \\ &W = -\alpha(1-\epsilon^{xt}) - \\ &\qquad \beta(1-\epsilon^{yt}) \end{aligned}\right\} \text{(Where } x \text{ and } y \text{ are; negative constants the only variables, } W \text{ and } t\text{)}$$

The ultimate temperature difference (full-scale value)

$$W_u = \gamma = -(\alpha+\beta) = \frac{h_2H_1 - h_1H_2}{Q(h_1+h_2)+h_1h_2}$$

References

1. Code for Electricity Meters. *C*-12-1941, American Standards Association (New York, N. Y.) 1941.

2. American Standard Definitions of Electrical Terms. AIEE, August 1941, section 35.20.-075, page 121.

3. Theoretical Possibilities of an Internally Heated Bi-Metal Type of Thermal Watt Demand Meter, E. E. Lynch. *Electrical Engineering* (*AIEE Transactions*), volume 61, October 1942, pages 760–70.

4. A New Thermal Volt-Ampere Demand Meter, M. E. Douglass, W. H. Morong. *AIEE Transactions*, volume 68, part I, 1949, pages 79–83.

5. Rates and Rate Making, Paul M. Lincoln. *AIEE Transactions*, volume 34, part II, October 1915, pages 2279–2318.

Discussion

Wm. C. Downing, Jr. (Sangamo Electric Company, Springfield, Ill.): In presenting his excellent analysis of the effects of thermal dissymmetry on the time of response of thermal demand meters, Mr. Nelson has made a worthwhile contribution to the literature on this subject. While these general principles have been appreciated, in the past, and have been employed to control the response time characteristics of thermal meters, there has not been available, to my knowledge, any such quantitative analysis. This paper, therefore, should provide a valuable reference for future work in this field.

While the heat flow ratio has been selected as the parameter for this analysis, it is inferred that a comparable set of equations might be developed based upon the thermal capacity ratio as a parameter, the heat flow ratio being unity. In other words, the time of response also may be controlled by unbalancing the heat storage factors instead of the heat flow. Practical considerations, based on constructional features, normally determine the choice of one of these methods, or a combination of them; for, while it is quite true that it is difficult in general to unbalance the thermal capacity without disturbing the heat flow ratio, unbalancing of the heat flow ratio must be used with caution to avoid the introduction of errors which may be of much more importance than the time characteristic. As this latter point is not considered in this paper, it may be well to amplify it.

Let us assume, for example, a thermal watt demand meter in which the heat flow ratio is 1.1. If for a certain load, H_1 is 3 watts and H_2 is 1 watt, the numerator of the expression for W_u, equation 8, is $3.3-1$, or 2.3. For the same power measured, but at a different power factor, for example, H_1 might be 4 watts and H_2 2 watts, since the electrical circuit provides only that the difference between H_1 and H_2 remains proportional to the power measured; however, in this latter case, the numerator of equation 8 becomes $4.4-2$, or 2.4. As W_u should be alike in these two cases, the introduction of a heat flow ratio other than unity has introduced an error in meter registration. This would appear to be a more serious limitation to the use of heat flow ratio as a parameter than either of those mentioned in the paper.

Actually, of course, some such "error" may be desirable to compensate for the tendency of the temperature-rise-per-watt to decrease when the total power input is increased as in the foregoing example, and watt demand meters have been built for years with a heat flow ratio slightly greater than unity for this reason. Nevertheless, it may be argued that the importance of the heat flow ratio in minimizing power factor, voltage, or balance errors should dominate, in design, its effect on time of response.

In only one respect, however, would I question Mr. Nelson's paper and that is in reference to the opening statement that "the time of response of demand meters is of critical importance." Insofar as the timing accuracy of a demand register is concerned, this is quite true; but I believe it has been rather well established by a number of independent surveys that whether the time interval is 15 minutes or, say, 20 minutes, for either a demand register or a thermal demand meter, makes negligible difference in the billing of the great majority of demand loads. For one such reference, refer to P. A. Borden's paper presented before the Institute in 1920[1]; "Except upon the most fluctuating loads, the demand meters of one type, and of differing time periods give indications which, generally, within the limits of accuracy of the individual instruments, were in close agreement with one another." The Code for Electricity Meters[2] recognizes this, I believe, in specifying only a unilateral limit for the response time of a lagged-demand meter.

However, while the time characteristic of a thermal meter may not be critical from the point of view of application of the meter, it is frequently a critical problem for the design engineer; and Mr. Nelson, in presenting this analysis, has, I feel, performed a real service to the engineers concerned with the design of thermal meters.

References

1. The Measurement of Maximum Demand and The Determination of Load Factor. Perry A. Borden. *AIEE Transactions*, volume XXXIX, part II, 1920, pages 1847–94.

2. See reference 1 of the paper.

J. S. Nelson: The author wishes to express his appreciation to Mr. Downing for bringing into the picture a pertinent discussion of the consequences of allowing thermal dissymmetry to affect ultimate deflection. He states that "the electrical circuit provides only that the difference between H_1 and H_2 remain proportional to the power measured," and deduces therefrom certain errors in registration at different levels of spiral energization. In this connection it is well to point out that, in certain instances, circuit elements may be provided with resistance characteristics such that the expression proportional to the quantity measured is not $H_1 - H_2$, but $kH_1 - H_2$, where k is a constant containing the parameter h_2/h_1. A circuit designed for such proportionality in the measurement of volt ampere demand has been described in a previous paper before the Institute by Douglass and Morong.[1]

Mr. Downing's reservation may therefore be regarded as particular, rather than general, specifically applying to the use of unbalanced heat escape in thermal watt demand meters, and with this reservation the writer is in full agreement. In a family of meters using the same basic thermal motor, the watt demand meter should be assigned the position at or nearest unity ratio. Power factor errors still present may be compensated successfully by the appropriate relationship between the resistances of the upscale and downscale bimetallic spirals, which resistances are themselves functions of temperature and current. The design criteria for accomplishing this compensation, and circuit theory in general, are considered beyond the scope of the present discussion.

Mr. Downing's point with regard to the use of the term "critical" is well made. Aside from the design considerations necessary to conform with the unilateral code limits and with good metering practice, and for the majority of applications, it is agreed that there is no particular value of response time which is critical in the sense of mathematics or physics. Perhaps the author's meaning could have been made more clear by the terms "intrinsic importance," as response time is a property of demand considered in itself.

For example, in the measurement of a steady-state voltage, response time may be an important consideration in the convenience of the voltmeter to the user, but it may have any value whatever without effecting the accuracy of the final measurement. In the case of demand measurements, on the other hand, the meter must measure not only a load, but the time-average of the load, and any modification of the time of response will directly affect the possible error of the measurement. This means that, in the ideal, the time of response is just as critical as the demand measurement itself (but no more so). If a 15-minute demand interval meter produces the same maximum reading as a 20-minute demand interval meter connected to the same terminals, it must be due to the fortuitous circumstance that the system had equal 15-minute and 20-minute maximum demands during the period of the test. Happily, as Mr. Downing points out, this ordinarily will be the case, and this fact should be recognized in the application of thermal demand meters.

Reference

1. See reference 4 of the paper.

An Improved A-C Network Analyzer

W. A. MORGAN
MEMBER AIEE

F. S. ROTHE
MEMBER AIEE

J. J. WINSNESS
NONMEMBER AIEE

SINCE the first a-c network analyzer was installed in 1929 at the Massachusetts Institute of Technology,[1] it has become the accepted tool for the solution of the complex circuit problems involved in the design, planning, and operation of power systems. There are now over 20 a-c network analyzers in the United States[2] and several more built or planned in Canada, England, Europe, South America, and India. Many of these analyzers are used by a number of different groups on a rental or co-operative basis. The improvements in design and performance of the analyzer which are described in this paper will thus be of interest to a large number of users of such equipment. These improvements include: master instruments which read power and reactive flows directly in megawatts and megavars for any one of three system megavolt-ampere bases and on any one of six current scales; a complete set of generator instruments which allows volts, megawatts, and megavars to be preset; load-adjusting autotransformers with voltmeters, which permit the system loads to be held constant without reference to the master instruments; a flexible method for the exact representation of a transmission line by means of an equivalent circuit; push button selection of circuits to be metered, either singly or in groups; and complete factory wiring, assembly, and testing. An analyzer of this improved design recently has been installed at the United States Bureau of Reclamation in Denver, Colorado.

Value of the A-C Network Analyzer

There are several reasons for the acceptance of the a-c network analyzer as a tool in solving power-system problems. In contrast with numerical calculating machines, it uses voltage, current, and impedances to solve the problem. Thus a power system covering several states can be represented realistically on an analyzer in a medium-sized room by simply replacing the elements of the power system with electric circuits. The power engineer is familiar with the use of equivalent circuits for such elements as synchronous machines, transformers, and transmission lines. By the use of equivalent circuits a variety of problems in electromagnetic fields, vibration, heat flow, and other subjects also have been solved on network analyzers.[3]

The modern a-c network analyzer is a very flexible device. System changes such as a load increase, a line outage, a change in transformer taps, or a change in the supply of reactive power, can be simulated rapidly and their effect found immediately. Since power-system design is based on the collective judgment of several persons, the problem is not one of specifying a set of initial conditions and obtaining a result correct to four decimals but rather a determination by trial and error of the best design based on a number of initial assumptions. The a-c network analyzer is essential for the expeditious study of complicated networks under variable conditions; even on simpler systems the accuracy of a numerical calculator is not necessary.

The first cost of a modern analyzer might seem high, but is actually low in comparison with possible savings in system design. A recent study made on the analyzer of the United States Bureau of Reclamation resulted in the substitution of a single-circuit 115-kv line for over 15 miles of 2-circuit line which had been previously thought necessary. Another saving resulted when it was determined that a synchronous condenser was not needed at an extension of the power system as originally planned.

Perhaps the greatest savings in power-system design, resulting from the use of the a-c network analyzer by the Bureau of Reclamation has been in power-system transient stability studies. Wherever practicable, and where the nature of the load justifies it, it is the policy of the Bureau of Reclamation to design its power systems to be stable for the most severe probable fault conditions. This becomes a determining factor because the Bureau's hydroelectric generation is usually at a long distance from the load centers.

Low generator transient reactance is an important factor in transient stability. However, due to their slow speed, hydroelectric generators normally do not have as low a reactance as steam-turbine generators, and special low reactances are obtained at extra cost. The a-c network analyzer is the best means available to evaluate this extra cost, which would be considerable for a plant such as Grand Coulee with an installed capacity of over 2,000,000 kva.

The value of rapid reclosing of circuit-breakers (12–20 cycle) on transient stability of a power system can be determined accurately in advance only by analyzer studies. Such studies, made in 1944 by the Bureau of Reclamation demonstrated that two 161-kv transmission lines, 140 miles long, with 20-cycle reclosing circuit breakers, were the practical equivalent of three lines without 20-cycle reclosing circuit breakers for the loads involved.[4] Subsequent operation through two complete years of this system, in lightning-active country, proved that the calculating-board studies were accurate. In the future, the important

---

Paper 49-164, recommended by the AIEE Computing Devices Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20–24, 1949. Manuscript submitted March 7, 1949; made available for printing April 29, 1949.

W. A. Morgan is in charge of the Power System Technical Section, Bureau of Reclamation, Denver, Colo.; F. S. Rothe is in the Analytical Division, General Electric Company, Schenectady, N. Y.; and J. J. Winsness is in the Switchgear Divisions, General Electric Company, Philadelphia, Pa.

The authors wish to acknowledge the important contribution from H. A. Peterson of the University of Wisconsin, formerly with the General Electric Company, to the general design of the analyzer, and from H. P. Kuehni of the General Electric Company to the design of the master instrument system.

load served by these two transmission lines will be supplied principally from a different direction as new hydroelectric generation sources are developed. At that time the two initial lines are all that are required from a power-flow or stability standpoint from the present source of generation even without dependence upon reclosing circuit breakers. Thus, the application of reclosing circuit breakers for the initial system saved building a 140-mile 161-kv line that would not be required in the ultimate system. It is Bureau of Reclamation practice, in the design of practically all its power systems of 115 kv and above, to utilize to the fullest extent the advantage of ultra-rapid reclosing circuit breakers in transient stability, with the result that the a-c network analyzer will help the Bureau to save many hundreds of miles of transmission lines.

For some important loads an interruption of any kind is a serious matter. Such loads could not tolerate the instability that would result if reclosing on a permanent fault occurred. Thus, in areas where the faults are less likely to be due to lightning and faults due to airplanes and similar hazards are more probable, rapidly reclosing circuit breakers may not be so essential. In these cases, the a-c network analyzer is used to investigate stability of the power system for the condition of faulting and clearing, without reclosing, the most heavily-loaded transmission line section. If rapid reclosing circuit breakers are used on such a system to restore the faulted line section to service as soon as possible, then the a-c network analyzer would be used to determine what must be done to the system to make it stable for the condition of reclosing on a permanent fault and clearing the faulted line a second time.

These are but a few specific examples that show how the a-c network analyzer installed at the Bureau of Reclamation already has paid for itself in design savings.

General Design Features

The best design of an a-c network analyzer should satisfy the requirements of accuracy, flexibility, and reliability at reasonable cost. The choice of the base electrical quantities of voltage, current, and frequency on which the per-unit representation of the power system is made is very important. A small current base and high impedance base result in freedom from heating and contact problems, allow the use of the very reliable telephone-type cords, plugs, and jacks, and permit a wide range in the operation of the units. The first analyzer employing a base current less than 1 ampere was built in 1938.[5] This analyzer used a base voltage of 50 volts, a base current of 50 milliamperes, and a base frequency of 480 cycles. This analyzer has been in continuous use since that date. During the war it was in operation 16 hours a day for two years; since the war it has been operating 60 hours a week until recently. This experience and the small amount of maintenance has shown that the choice of base quantities was sound, and that the design of the board was such as to make it very reliable. The stray capacitances associated with the high impedance base have not been found to be important in system studies. The new analyzer, shown in Figure 1, makes use of such techniques as careful arrangement of wiring and use of low-capacitance switches and relays to make this effect negligible. Therefore, the use of these same base quantities has been continued in the improved analyzer.

Table I shows the number, range, and ratings of the different types of circuit units in the Bureau of Reclamation analyzer. The ranges of units are such that, in the majority of cases, one unit will cover the range required. All of the impedance and transformer units are rated 1.25 per unit volts and 5 per unit current. Their thermal ratings are such that no damage will be done to them at 10 per unit current. Low flux densities and air-gap construction utilized in the reactors results in very little variation in impedance up to 10 per unit current and 1.25 per unit volts. The generator units are rated 2.5 per unit volts and have 2 per cent regulation at 10 per unit current. They are capable of delivering 20 per unit current with slightly higher regulation. These wide operating limits make it possible to handle most problems such as load and short-circuit studies with ease, without spending a considerable amount of time in setting up the system between narrow limits. For extreme conditions, it is possible to reduce the analyzer input voltage to $^1/_2$ or $^1/_4$ normal. With 25 per cent input voltage to the analyzer and 20 times normal current from a generator,

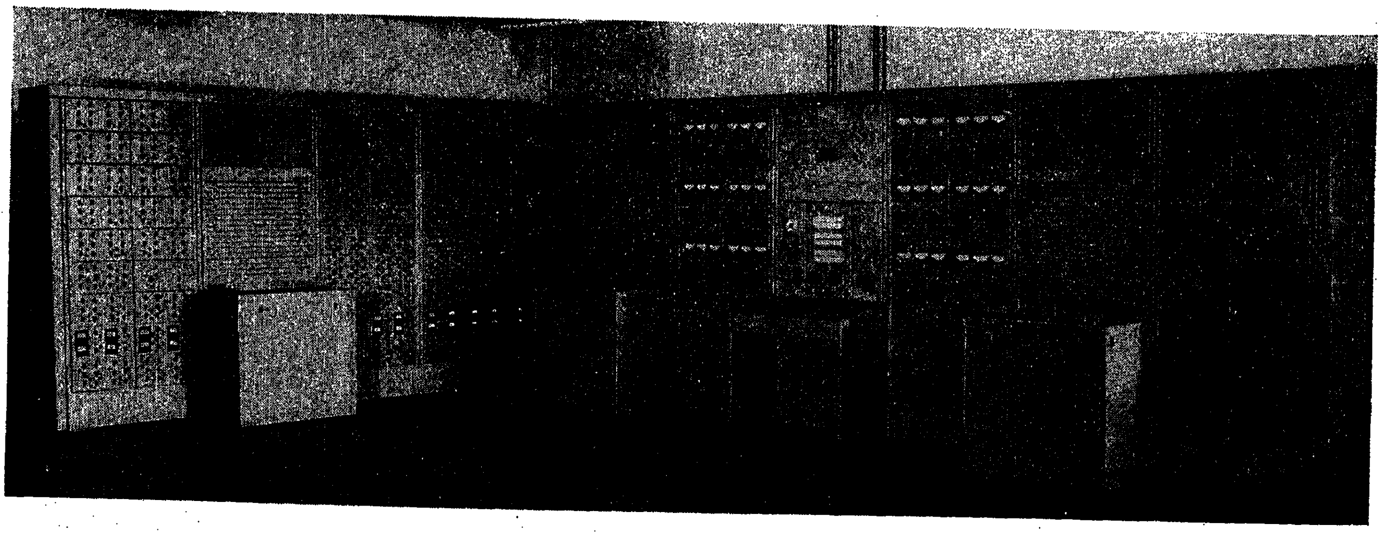


Figure 1. A typical modern a-c network analyzer

The center cabinet of the front section contains the master instruments and associated voltage, current, and push-button selector switches. Adjacent to and on each side of the instrument cabinet is a unit cabinet containing six generator units and six synchronous impedance units. Cabinets 2, 6, and 10 (numbered from left end) are plug-and-jack cabinets in which the terminals of circuit units in two or three adjacent cabinets are brought out for interconnection. The remaining cabinets are unit cabinets containing line, load, autotransformer, capacitor, and mutual transformer units. This analyzer has the front section and left wing with provisions for future addition of a right wing

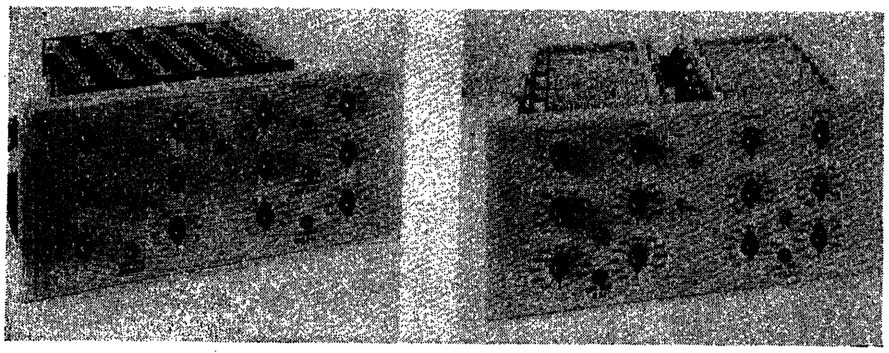


Figure 2. Capacitor-unit and line-unit drawout tray

Line unit with Pi-line switches

a fault current of 80 times the system base can be supplied from any generator unit. This large current range has proved to be very valuable in making system studies.

Tapped resistors, reactors, and capacitors are used in all units, except for the 0 to 0.1 per unit resistance in the load unit, in order to obtain definite fixed values of impedance. The accuracy on all taps down to 0.02 per unit at a specified current is ± 1 per cent, and ± 2 per cent at 0.01 and 0.02 per unit. The change in reactance of the reactors is less than ± 2 per cent over the full current range at any setting.

The impedance and transformer units are mounted on standard 8-inch high by 16-inch wide by 18-inch deep drawout trays. Figure 2 shows typical line-unit and capacitor-unit trays. Two line units or six capacitor units are mounted on one tray as shown. The generator unit is mounted on a double-height drawout tray, as shown in Figure 3. This standardization in tray size permits a standard unit cabinet size and maximum flexibility in arrangement of units and cabinets.

Load Units and Load Adjusters

The load units have individual voltmeters with manually-set pointers, and are connected to the system through individual autotransformers so that the voltage impressed on each unit can be kept constant regardless of changes in system voltage. This is especially helpful during the initial adjustment period, before the transmission line voltage opposite a given load substation has stabilized. The settings of the resistance and reactance values of the load units are calculated previously on the basis of one per unit voltage. If voltages other than one per unit prevail, then the loads would be different from what they are supposed to be, with no change in the load unit settings. The individual voltmeters allow the adjuster to monitor these load units and to keep the voltage at one per unit by adjustment of the one per cent taps of the autotransformer, without reference to the master instruments. It has been found that this feature saves considerable time in making load studies where it is desired to keep the system load constant during variations in the system design.[6]

Generator Units

Each generator unit consists of two selsyns, one for controlling phase angle and the other for controlling voltage magnitude. In addition, each generator unit has its own instrumentation, including a voltmeter, varmeter, and wattmeter, as shown in Figure 3. These instruments have red hand-set pointers that may be used to indicate the conditions under which the generator is expected to operate. A varmeter has been substituted for the ammeter usually found on generator units because watts and vars are of primary interest in adjusting a generator unit during a system study. In order to minimize the load imposed by the instruments on the generator and to decrease their effect on regulation, a current amplifier is used to feed the wattmeter and varmeter. The voltmeter can be connected either at the generator terminals or at any desired bus on the system.

Equivalent Pi-Line Units

Pi-line units have been found to be very useful in making system studies. It is desirable to be able to meter both ends of a pi-line unit and also, if possible, to use the capacitors and line unit individually. The equivalent pi-line unit consists of two standard capacitor units and one standard line unit, with a toggle switch on the line unit to connect a capacitor unit to each end of the line unit. Figure 4 shows the equivalent pi-line circuit. By plugging the capacitor-unit cords for units *300* and *301*, it is possible to read power flow at both ends of the line. Opening the toggle switch permits free and independent use of the line-unit and capacitor-unit sections in any manner desired. This permits having pi-line units without incurring restrictions in flexibility.

Instrument System

The master-instrument system is probably the most important part of a network analyzer. Figure 5 shows the master-instrument control panel, including the master instruments, scale selector switches, voltage and current selector switches, motor-generator-set control switches, reference phase shifter, and push-button selector panel. The master instruments include a voltmeter, ammeter, and wattmeter-varmeter of the light-beam-type, with 8-inch scales having 150 divisions. These instruments have an extremely fast rate of response and are well damped, making possible rapid reading. The burden imposed on the network by these instruments is minimized by the use of current and voltage feedback amplifiers. For the nominal

Table I. Number and Range of Circuit Units

Number	Units	Per Unit Range	Steps	Rating Per Unit Volts	 Per Unit Amperes
12	Generator unit	V 0—2.5	Continuous	2.5	10.0
		ϕ 0 ± 180 degrees	Continuous		
12	Synchronous impedance	R 0—0.11	0.001	1.25	5.0
	(Series $R + jX$)	X 0—1.11	0.001		
108	Line units	R 0—0.51	0.001	1.25	5.0
	(Series $R + jX$)	X 0—0.81	0.001		
32	Load units	R 0—16.1	Continuous	1.25	5.0
	(Series or parallel $R + jX$)	X 0—16.05	0.05		
32	Load unit autotransformer	V 0— ±15 per cent	1 per cent	1.25	5.0
42	Capacitor units (Susceptance)	0—1.1	0.01	1.25	
4	Large capacitor units (Susceptance)	0—50.0	1.0	1.25	
18	Autotransformer units	V 0— ±30.5 per cent	0.5 per cent	1.25	5.0
12	Mutual transformer units	1:1 Ratio		1.25	5.0

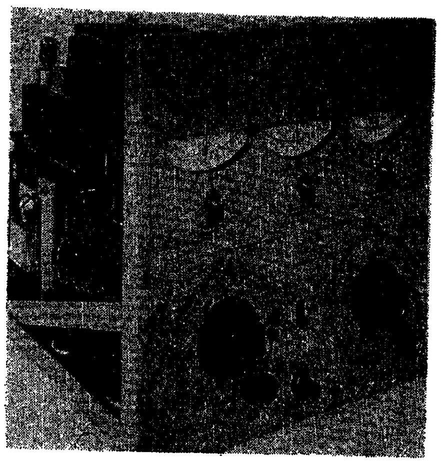


Figure 3. Generator unit

analyzer voltage and current of 50 volts and 50 milliamperes, a maximum potential drop of 0.01 volts (0.02 per cent) across the current shunts and a potential current of 0.1 milliamperes (0.2 per cent) was set as limits of instrument burden. The accuracy of the master-instrument system is within ± 0.5 per cent of full scale.

The scales on the master ammeter and wattmeter are arranged so that the digits appearing at the large scale divisions change with the scale multipliers. The current shunts of the instruments also are changed simultaneously by the knurled-handled scale-selector switch. Thus, the ammeter is direct reading in per unit for any of the six available current multipliers, and the wattmeter is made direct reading either in per unit, or on a 20-megavolt-ampere, 50-megavolt-ampere, or 100-megavolt-ampere base for any of the available current multipliers. This feature is a big improvement, as it allows the operator to change the multiplier readily, for successive metered points if he wishes, so as to get the reading at a favored location on the instrument scale and to have the power and reactive flows read directly in megawatts and megavars. The operator does not have to multiply mentally the reading by a factor to get the actual reading, or remember which of several scales to read.

The master instruments may be connected to meter any circuit selected by means of a 3-column keyboard of numbered push buttons, similar to that of an adding machine. A special master control switch allows a given circuit to be selected by the push buttons while the previous circuit, indicated by the numbered lights adjacent to the push-buttons, is still being metered. This saves time as, while the recorder is putting down the previous reading and the instruments are kept on the previous circuit in case a question should arise, the operator can refer to the plugging diagram, select the next circuit he wishes to meter, and push down the corresponding selector push buttons. Then, when the recorder is ready, the operator pushes the master control switch which connects the instruments to the new circuit and lights the adjacent numbered lights corresponding to the numbered push-buttons, and the operator announces to the recorder which circuit is being metered. While the recorder is locating the new circuit on the diagram, the operator has selected the proper instrument scale to use and is ready to announce the readings.

A special connection of the metering circuit allows the watt and var measurements to be of the proper sign when looking away from a bus, whether the given unit is metered from its polarity or nonpolarity jack plug end. This feature permits faster recording and simplifies checking of power-flow readings around a bus as the study progresses. Previously, the operator has had to keep in mind whether he was metering the polarity or nonpolarity jack plug end of a unit and mentally reverse the sign of the watts or vars from that indicated by the instruments and switches, when appropriate.

The Bureau of Reclamation has taken full advantage of this feature in originating a new method of indicating polarity sign of watt and var readings on power-flow diagrams. This is illustrated in Figure 6. The arrows are made up initially as part of the basic single-line diagram and are for reference only, to show the direction the instruments were looking when the readings were taken. The plus and minus signs in front of the watts (above the line) and vars (below the line) are then the actual signs indicated to be used by the master instruments and polarity switches, referred to the reference arrow direction, which is always away

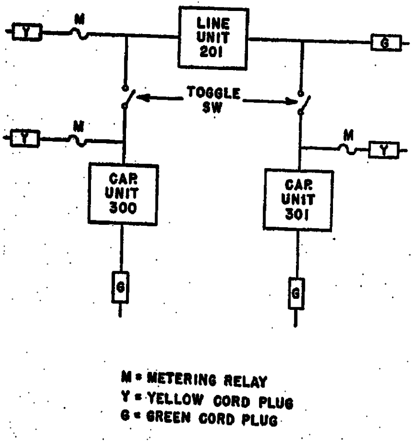


Figure 4. Schematic diagram of equivalent pi-line unit

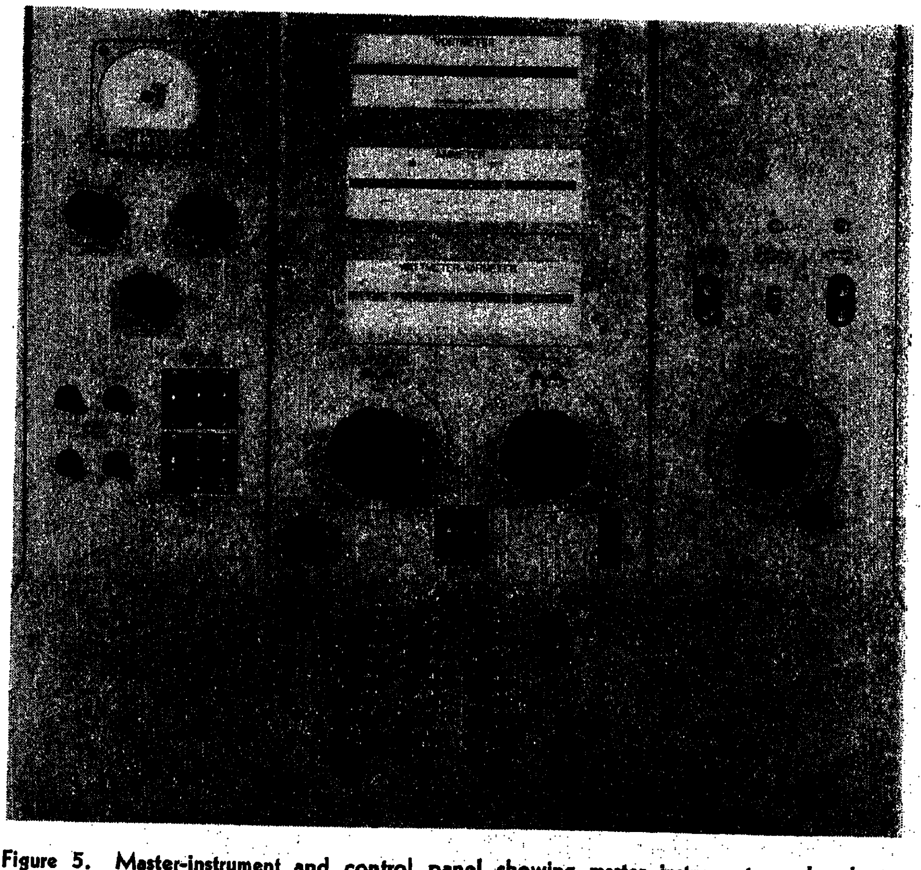


Figure 5. Master-instrument and control panel showing master instruments, scale-selector switches, metering switches, push-button keyboard, and other controls

from the bus. An analyzer unit with jack plug ends on two different busses would read plus power at one end and minus power at the other end, corresponding to the flow of power out of one bus and into the second bus. By having the plus and minus signs in front of the watt and var readings, the recorder readily can add algebraically the power and reactive-power flows over the circuits connecting to each bus to see that they total zero. This may be done conveniently by a third person without delaying the recording process. These checks make it possible to correct errors in reading or recording before conditions on the analyzer have been changed.

Construction and Wiring

Each of the circuit units terminates in plugs in one of the plug and jack cabinets. Because of the large number of circuit units in the analyzer, precautions were taken to minimize the length of interconnecting wires. There is a plug and jack cabinet for every three unit cabinets, with provisions made for interconnecting some of the jacks on each of the cabinets as required by the system connection diagram. The metering bus for each plug and jack cabinet is further subdivided into two sections; only one section is connected to the instrument system at any time. The wiring is carried in a 9-inch high subbase of the analyzer, and all wires are spaced on 1/2-inch centers and away from the steel to minimize the stray effects. This construction permits the complete wiring and testing of the equipment in the factory. The analyzer is built and shipped in sections comprising several cabinets, and relatively few interconnections are required at installation.

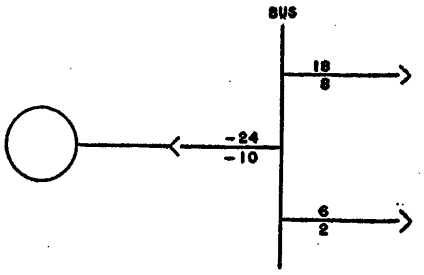


Figure 6. Example of method of representing polarity of watts and vars on a power-flow diagram

Summary

This type of a-c network analyzer with its low volt-ampere base has proved to be of reliable design over a number of years of actual operating experience. The improved analyzer described in this paper incorporates many new features, including direct-reading master instruments, a fuller complement of generator instruments, push-button selection for meter readings, and complete factory assembly and testing. These features should further increase the usefulness of this tool in power-system design.

References

1. The M.I.T. Network Analyzer, H. L. Hazen, O. R. Schurig, M. F. Gardner. *AIEE Transactions*, volume 49, July 1930, pages 1102–14.

2. Use of A-C Network Calculator in Planning and Operating Electric Power Systems, P. O. Bobo, H. A. Travers, E. E. George. *Report 328*, International Conference on Large High-Voltage Electric Systems (Paris, France), June 24–July 3, 1948.

3. Analyzers, C. Concordia, H. A. Peterson. *General Electric Review* (Schenectady, N. Y.), September 1945, pages 29–37.

4. Power System Stability Requirements Determined by A-C Calculating Board Study, and Operating Performance Proved by Staged Fault Tests, W. A. Morgan, Byron Evans. *Report 319*, International Conference on Large High-Voltage Electric Systems (Paris, France), June 24–July 3, 1948.

5. A New A-C Network Analyzer, H. P. Kuehni, R. G. Lorraine. *Electrical Engineering* (*AIEE Transactions*), volume 57, February 1938, pages 67–73.

6. Auxiliary Equipment Facilitates Network Calculator Operation, L. B. LeVesconte. *Electric Light and Power* (Chicago, Ill.), volume 24, October 1946, page 50.

Discussion

R. B. Squires (Westinghouse Electric Corporation, East Pittsburgh, Pa.): The author's paper is valuable primarily because it gives definite examples of how a large power system can benefit through the use of a network calculator or analyzer. Savings in system design such as is given in the paper have demonstrated the value of the network calculator to an increasing number of utility groups. This has resulted in the installation of a number of calculators throughout the country.

The increase in facilities has been especially rapid since the end of the war. Six network calculators or analyzers have been installed in the last three years, an increase of about 30 per cent over the total prewar figure.

Although the peak has probably passed, the average use of each installation continues at a higher level than before the war, demonstrating that the use expands as more companies have easy access to these devices.

Table I of the discussion has been prepared to summarize the various installations and show their essential features. There is a possibility that on some of the calculators additional facilities have been installed with which we are not familiar.

The network calculators are listed in Table I in the order of their original installation. For example, the second installation was at Westinghouse in East Pittsburgh in 1929. This was reinstalled at Purdue University, with some additions, in 1942. The present Westinghouse calculator dates from that year.

The power supply frequency is usually between 400 and 500 cycles, depending on the design of the manufacturer. For instance, the unit described in this paper uses 480 cycles; the units with which the writer is most familiar use 440 cycles.

The remaining columns show the number of units of each type, such as generators, instruments per generator, load adjusters, load adjuster voltmeters, and how the circuits are selected and metered.

The units listed as pi-line circuits also can be used as regular lines in all cases. The capacitor units listed are those in addition to the capacitors used with the pi-lines. The latter are equal in number to twice the pi-lines, but are not listed in the table.

As can be seen from the table, the majority of the features presented as new in this paper have been incorporated in all calculators manufactured during the past several years. The owners of the more modern equipments appreciate from experience the value of these features.

The network analyzer described in this paper represents an interesting adaptation of up-to-date features, (such as direct reading master instruments, more generator instruments, push button metering, and load adjusting voltmeters) to their high impedance level design. It has been the experience of the writer over the past several years that these features have been great time savers and conveniences on all the modern network calculators. The Bureau of Reclamation will no doubt find them of equal and continuing use in the future.

Edward W. Kimbark (Northwestern University, Evanston, Ill.): The network analyzer described in the paper differs from the one built by the same manufacturer in 1937 with respect to several rather minor details. (Incidentally, the date 1938 given in the present paper is not correct. The analyzer was in full operation in Schenectady by June 1937.) The paper would be more valuable to persons familiar with the earlier models of analyzers if it had specifically listed the features of the new model differing from the 1937 model. These features, as nearly as I can gather them from the paper, are the following:

1. Addition of load adjusters (autotransformers) and voltmeters to the load units.

2. Pi-lines.

3. Decentralization of cord and jack panels.

4. Push-button selector switch of adding-machine style and relays for connecting master instruments to the selected network unit.

5. Automatic change of ammeter and wattmeter scales when range-selector switch is moved.

6. Light-colored paint.

7. Addition of current amplifier to the watt-varmeter on each generator unit.

Table I. A-C Network Calculators Installed to Date

Owner	Year Installed	Frequency Cycles	Generator Units	Generator Imp. (Z)	Inst. Per Generator	Circ. Select.	Lines R + JX Series	Equivalent Pi-Lines	Load Units	Load Adjusters	Load Adjuster Voltmeters	Special Circuits	Capacitor Units	Auto Transformer	Mutual Transformer	Total Circ. Except Load Adjuster
Massachusetts Inst. Technology	29	60	16	0	3	R	80	0	40	0	0	6	55	12	0	209
Purdue University*	42	440	16	0	3	R	160	0	40	40	0	38	60	36	33	383
Pennsylvania Railroad	32	440	6	0	3	S	168	0	40	0	0	40	12	14	16	296
Commonwealth Edison Company	32	440	6	0	3	S	72	0	48	0	0	12	24	18	6	186
General Electric Company	37	480	12	0	2	S	150	0	50	0	0	2	64	20	15	313
Public Service Electric and Gas Co.	38	480	8	0	2	S	85	0	30	0	0	0	18	10	12	163
Tennessee Valley Authority	38	440	18	0	3	S	120	0	44	24	0	8	34	24	22	270
Bonneville Power Administration	39	480	18	0	2	R	130	0	70	0	0	12	60	20	16	326
Sao Paulo Tramway Light and Power Company	40	440	6	0	3	S	32	0	16	16	0	0	12	6	12	98
Potomac Electric Power Company	41	440	6	0	3	S	52	0	18	18	0	0	12	6	8	120
Hydro Electric Power Commission	41	440	15	0	3	R	98	0	32	32	0	49	36	18	11	259
Public Service Co. of Oklahoma	41	60	7	0	2	S	87	0	30	0	0	21	26	14	0	185
Westinghouse Electric Corporation	42	440	22	22	4	R	152	24	48	48	48	0	48	36	36	384
Illinois Institute of Technology	45	440	12	12	3	R	100	0	40	40	40	0	36	18	18	236
Iowa State College	46	10^4	4	4	3	R	0	24	12	12	0	0	12	4	4	64
Texas A and M College	47	440	18	18	3	R	134	16	56	56	56	0	54	24	24	344
City of Los Angeles	47	440	18	18	3	R	120	0	40	40	0	0	36	16	18	266
University of Kansas	47	60	8	0	2	S	56	0	32	32	0	11	12	8	6	133
Associated Electrical Industries, Ltd.	47	500	12	0	2	R	120	0	30	0	0	4	60	36	12	274
Georgia School of Technology	47	440	18	18	3	R	88	72	60	60	0	0	24	24	18	322
Pacific Gas and Electric Company	48	440	14	14	3	R	132	32	48	48	0	0	48	24	12	324
Consolidated Gas, Electric Light and Power Co. of Baltimore	48	440	16	16	3	R	122	0	44	44	44	0	10	20	12	240
United States Bureau of Reclamation	48	480	12	12	3	R	87	21	32	32	32	0	4	18	12	240

S = Circuit selection by switches or plugs

R = Circuit selection by remote selector & relays

* First installed in 1929, reinstalled in 1942

8. New convention on relative signs of watts and vars at the two ends of a circuit element.

Will the authors please correct me if I have omitted any important changes from this list?

Actually features 1 to 6 are not new, having been included previously in analyzers built by another manufacturer. Thus the present paper offers very little that is really new in network analyzer design or operation.

A network analyzer incorporating at least one important novel feature has been designed by Professor E. B. Phillips and is in operation at the University of Kansas. The novel feature is that impedances set up on the analyzer are shifted by a constant angle of $\cos^{-1} 0.85$ from the corresponding impedances of the power system represented. This feature enables reactors of relatively low Q to be used to represent pure reactance, thus making possible the use of small 60-cycle reactors. The use of 60 cycles eliminates the need of a motor-generator set, which is one of the expensive components of conventional network analyzers. (The expense arises not only from the cost of the motor-generator set itself but also from the fact that, because of its noise, the set must be installed in a separate room and conduits must be installed connecting the two rooms.) It is to be hoped that the appropriate AIEE technical committee will prevail upon Professor Phillips to present to the Institute a complete technical description of his new and very carefully worked-out design.

More innovations such as those on the Phillips analyzer are needed to decrease the cost of network analyzers enough so that every power company can afford to have one. However, even with the present costs, power companies cannot afford not to use network analyzers for studying important changes in their systems. The authors perform a valuable service by citing new examples of possible savings in construction costs without sacrifice of reliability. They also present some interesting thoughts on the pros and cons of rapid-reclosing circuit breakers where the utmost reliability is sought.

Eric T. B. Gross (Illinois Institute of Technology, Chicago, Ill.) This paper is a most valuable contribution since it brings us up to date by the description of a modern analyzer, a tool which simplifies greatly most of the work of the power systems engineers.

It is of interest that in this improved a-c network analyzer nearly all the modifications are incorporated which have been developed in recent years by different users of such calculating boards at various locations. At the Illinois Institute of Technology, we have found the recording table (described in reference 6 of the paper) of great advantage since the work is speeded up considerably, possible errors are checked, and the operating engineer is able to visualize quickly how kilowatts and kilovars are distributed in the whole system. This recording table is of the most help in load studies, and I would say that 20 to 25 per cent more work can be done in the same time with the aid of this tool. I understand that similar recording tables now are in use at some other institutions.

F. S. Rothe: The authors wish to thank the discussors of this paper for their remarks. There is general agreement that the improvements in network analyzers as described in the paper will benefit a large number of users through increased efficiency of operation.

There was criticism of the paper because some of the features described were not new. However, we believe it is fair to say that the features described in general represent definite improvements over existing designs. The method described in the paper for reading megawatts and megavars with the correct sign directly on the master instruments is an example of this.

In connection with the novel analyzer of Professor Phillips, Professor Kimbark cites the saving of a motor generator set as

Table II

Owner	Year Installed	Frequency	Generator Units	Low $\frac{R}{X}$ Units	Pi Line Units	Line Units	Load Units	Load Adjusters	Load Adjuster Voltmeters	Capacitor Units	Large Capacitor Units	Auto-transformer	Mutual Transformer
General Electric Company Number 1	1937	480	12	2	0	150	50	0	0	60	0	20	15
Public Service Electric and Gas Company of New Jersey	1938	480	10	0	0	121	40	10	0	30	0	18	22
Bonneville Power Administration	1939	480	18	12	0	130	70	0	0	60	0	20	16
Bureau of Reclamation	1948	480	12	12	21	87	32	32	32	42	4	18	12
General Electric Company Number 2	1949	480	12	12	50	96	56	56	56	100	4	16	15
University of California	1949	480	6	6	0	50	16	0	0	35	0	8	4
India Institute of Science		480	16	16	24	76	50	50	50	48	0	32	8

eliminating an expensive component from the analyzer. We do not find this to be the case for the low volt ampere base analyzer described in the paper, since the cost of the set, including wiring, is but a small fraction of the total cost of the analyzer.

There are several additions which should be made to Mr. Squires' list of a-c network analyzers. In 1948 the Electricité de France installed an analyzer in Paris and there have been references in the literature to a-c network analyzers in Russia. An up-to-date list of analyzers built by the General Electric Company is given in Table II of this discussion.

General Theory, and Experimental Confirmation, of the Moving Coil Fluxmeter

THOMAS JAMES HIGGINS
MEMBER AIEE

GLENN ROBERTSON
STUDENT MEMBER AIEE

THE moving coil fluxmeter is an instrument for the measurement of magnetic flux. Essentially, the mechanism is that of a D'Arsonval galvanometer so designed that the net restoring torque due to the suspension (if used) and the spiral leads to the moving coil is held to a minimum. Usually, the search coil connected to the fluxmeter is of low resistance. The combination of minimum restoring torque and low search-coil resistance results in a preferred operating characteristic: namely, if under certain conditions the flux ϕ linking the N-turn search coil experiences an incremental change $\Delta\phi$, the deflection D of the pointer of the fluxmeter is proportional to the corresponding incremental change $\Delta\lambda$ in the flux linkages λ associated with the search coil.*

The theory of the fluxmeter has been developed in various degrees of completeness by several writers.[1-9] Study of their papers, epitomized in Appendix I, reveals that in the course of formulating a differential equation which approximately characterizes the deflection D, each writer introduces certain simplifying assumptions. For the most part these assumptions are tantamount to discarding, as negligible by comparison with those remaining, certain terms in the general differential equation which accurately characterizes the deflection D. The approximate differential equation stemming from these assumptions, when solved subject to a prescribed simple set of boundary conditions, has the solution $D=\Delta\lambda/C$; or equivalently

$$\Delta\lambda = CD \tag{1}$$

where C is a constant, fixed by the design of the instrument.

If, as is commonly the case in practice, the same flux ϕ links each of the N_{sc} turns of the search coil, $\Delta\lambda = N_{sc}\Delta\phi$. Substituting this expression in equation 1 and solving for $\Delta\phi$ gives

$$\Delta\phi = CD/N_{sc} \tag{2}$$

One or the other of equations 1 or 2 is the equation implied by the information printed on the scale of a commercial fluxmeter or cited in accounts of the fluxmeter contained in various reference sources: textbooks on electrical measurements or on principles of electrical engineering, handbooks of electrical engineering, instruction manuals for fluxmeters, and elsewhere. In practice, however, it frequently occurs that the actual circuit conditions are such that the terms discarded in formulating the approximate differential equation of which equation 1 is the solution are not negligible. In consequence, calculation by equations 1 or 2 is not valid; depending on the conditions at hand, the calculated values will be either smaller or larger than the actual existing values. Yet accounts of the fluxmeter to be found in available reference sources do not cite the restrictions under which equations 1 and 2 are valid (except that some instruction manuals specify the maximum allowable value of search-coil resistance, a bit of information that often goes unheeded); nor, more generally, do these reference sources discuss how a fluxmeter can be used for accurate measurement under any set of circuit conditions that are likely to occur in practice.

Now, if the general equation relating $\Delta\lambda$ to D were known, analysis of this equation would enable:

1. Determination of the correct use of the fluxmeter under any set of circuit conditions that occur in practice.
2. Establishment of various approximate equations especially convenient for calculation in those situations that most often arise in practice and of the corresponding restrictions limiting use of these approximate equations.
3. Estimate of the error to be expected from use of a given approximate equation.
4. Ascertainment of other factors of interest and value.

Obviously, this information is precisely that which is desired by one who has occasion to make extensive use of the fluxmeter in practice. It is, therefore, the purpose of this paper to provide this desired information for both the unshunted (single-range) and the shunted (multiple-range) fluxmeter.

It happens that the study of each of these two types of fluxmeter falls naturally into three parts:

1. Derivation of the general equation relating $\Delta\lambda$ to D.
2. Deduction from this equation of useful approximate equations, of the corresponding restrictions limiting their use, and of the approximate magnitude of inherent error.
3. Advance of experimental data confirming the correctness of this theory.

---

* This is actually the case, although the information printed on the scale of a commercial fluxmeter usually results in the inexperienced user inferring that deflection is proportional to the change in flux $\Delta\phi$. Again, some textbook accounts of the fluxmeter state, incorrectly, that deflection is proportional to change in flux. Note, however, equation 16. This confusion stems primarily from the fact that, in the sources mentioned, discussion is based on a 1-turn search coil, for which $\Delta\phi$ is numerically equal to $\Delta\lambda$.

Paper 49-168, recommended by the AIEE Basic Sciences Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20–24, 1949. Manuscript submitted January 14, 1949; made available for printing May 6, 1949.

THOMAS JAMES HIGGINS is Professor of Electrical Engineering, University of Wisconsin, Madison, Wis. GLENN ROBERTSON is an instructor at Illinois Institute of Technology, Chicago, Ill.

This paper is based upon a thesis supervised by Professor Higgins and submitted by Mr. Robertson to the faculty of Illinois Institute of Technology in January, 1949, in partial fulfillment of the requirements for the degree of Master of Science in Electrical Engineering.

Derivation of the General Equation for the Unshunted Fluxmeter

In the circuit of Figure 1 let e_{sc} be the (instantaneous) voltage generated in the search coil, e_f the voltage generated in the moving coil of the fluxmeter, i the circuit current, R the total resistance, and L the total inductance of the circuit. Then, by Kirchhoff's voltage law

$$e_{sc}=iR+Ldi/dt-e_f \qquad (3)$$

Inasmuch as the fluxmeter is a D'Arsonval galvanometer (with particularly chosen design parameters) its moving coil rotates in a radial magnetic field. Let B be the magnitude of the radial flux density, h and b the vertical height and horizontal breadth of the rectangular moving coil of area $A=hb$ and of N turns, θ the (instantaneous) angular position of the plane of the coil measured from a conveniently chosen reference position $\theta=0$, $w=d\theta/dt$ the angular velocity of the coil, and $v=bw/2$ the linear velocity of the vertical side of the coil angularly located at θ (the other vertical side is located at $\theta+\pi$). Then the voltage generated in one turn of this vertical side of the fluxmeter coil is $-Bhv=-Bhbw/2=-(BA/2)(d\theta/dt)$; whence the voltage generated in the coil is $e_f=-BAN(d\theta/dt)=-Gd\theta/dt$, where $G=BAN$ is a design constant commonly termed the "winding area."

The voltage generated in the search coil is $e_{sc}=d\lambda/dt$, where λ is the negative of the instantaneous flux linkages associated with the search coil.

Substituting for e_{sc} and e_f in equation 3 yields

$$d\lambda/dt=iR+Ldi/dt+Gd\theta/dt \qquad (4)$$

The electrodynamic force acting on a vertical side of one turn of the fluxmeter coil is Bhi; whence the total electrodynamic torque t_e acting to increase θ is $t_e=(Bhi)(bN)=Gi$. The torques acting to prevent increase of θ are: the inertia torque $t_i=Jd^2\theta/dt^2$, the air friction torque $t_f=k_fd\theta/dt$, the electromagnetic checking torque due to eddy currents induced in the metal frame (if any) on which the coil is wound, $t_c=k_cd\theta/dt$, and the restoring torque $t_r=U\theta$, which expression indicates that the reference position $\theta=0$ of the plane of the coil has been chosen to coincide with the position of zero restoring torque—a choice which imposes no limitation on the generality of treatment, yet simplifies the subsequent analysis. Herein, J is the polar moment of inertia of the coil about its vertical axis; k_f and k_c are damping constants (that in moving-coil instruments the friction and electromagnetic checking torques are essentially proportional to the angular velocity of the moving coil has been verified by numerous experiments.[10]); U is the sum of the torsion constant of the suspension if used and the effective spring constant of the spiral or other type of leads to the moving coil.

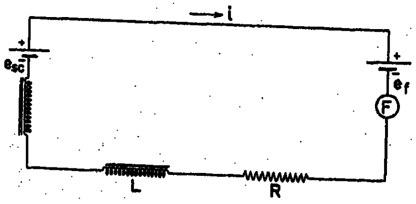


Figure 1. Circuit of unshunted fluxmeter

Equating driving torque to the sum of the opposing torques yields the general differential equation characterizing the mechanical motion of the fluxmeter coil, namely

$$Gi=Jd^2\theta/dt^2+Kd\theta/dt+U\theta \qquad (5)$$

where $K=(k_f+k_c)$.

Solving equation 5 for i, substituting the resulting expression in the term iR of equation 4, and collecting like terms gives

$$d\lambda/dt=(JR/G)d^2\theta/dt^2+[(RK/G)+G]d\theta/dt+Ldi/dt+(UR/G)\theta \qquad (6)$$

Integrating each side of equation 6 between $t=t_1$ and $t=t_2$, where t_1 and t_2 are arbitrary except for the condition $t_2>t_1\geq 0$, yields

$$[\lambda]_{t_1}^{t_2}=(JR/G)[d\theta/dt]_{t_1}^{t_2}+[(RK/G)+G][\theta]_{t_1}^{t_2}+L[i]_{t_1}^{t_2}+(UR/G)\int_{t_1}^{t_2}\theta\,dt \qquad (7)$$

Designating the values of λ, i, θ, and $w=d\theta/dt$ at $t=t_1$ and $t=t_2$ by corresponding subscripts and evaluating equation 7 yields

$$\lambda_2-\lambda_1=(JR/G)(w_2-w_1)+[(RK/G)+G](\theta_3-\theta_1)+L(i_2-i_1)+(UR/G)\int_{t_1}^{t_2}\theta\,dt \qquad (8)$$

Equation 8 is the desired general equation relating change in flux linkages associated with the search coil to the corresponding change in coil position.

Approximations for the Conditions Commonly Encountered in Practice

At $t_1=0$, with the fluxmeter coil at rest ($w_1=0$) at $\theta=\theta_1$, and with zero current ($i_1=0$) through it, let a quick change in the flux linkages associated with the search coil be initiated. The driving torque stemming from the generated current sets the coil in motion. Let $t_2=T$ be the time at which it acquires maximum angular displacement. At this instant $\theta=\theta_2$, $w_2=0$, and $i=i_2$. For the stated boundary conditions, equation 8 reduces to

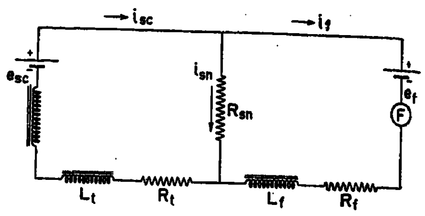


Figure 2. Circuit of shunted fluxmeter

$$\lambda_2-\lambda_1=[(RK/G)+G](\theta_2-\theta_1)+Li_2+(UR/G)\int_0^T\theta\,dt \qquad (9)$$

It is commonly the case, in practice, that L is small in comparison with R; that is, the circuit is substantially noninductive.* In such case the circuit current is substantially in phase with the net generated voltage in the circuit. Now the term "quick" implies that the total change in flux linkages has been completed before maximum displacement is attained. If so, at $t=T$ the flux through the search coil is not changing, whence the generated voltage in the search coil is zero. Again, at $t=T$ the fluxmeter coil is stationary ($w_2=0$); whence the generated voltage in the coil is zero, and, as the circuit current is substantially in phase with the net generated voltage, $i_2\cong 0$. Accordingly, for $R>>L$ equation 9 reduces to

$$\lambda_2-\lambda_1=[(RK/G)+G](\theta_2-\theta_1)+(UR/G)\int_0^T\theta\,dt \qquad (10)$$

Thus, unless L is commensurate with R—a case usually encountered in practice only when heavy iron-cored inductors are in series with the fluxmeter—the magnitude of a quick change in flux linkages associated with the search coil is to be calculated, in general, from equation 10. It is to be noted that the restriction to a quick change in flux linkages imposes no essential limitation, for, as discussed below, it is only quick changes that actually are utilized in practice.

Evaluation of the Integral Term in Equation 10

The integral in equation 10 cannot be evaluated explicitly, for θ is an unknown function of t. However, an upper limit to its actual value is established easily by use of a well-known equality in the elementary calculus

$$\left|\int_a^b f(x)dx\right|\leqq\int_a^b|f(x)|dx\leqq|f_m(x)|(b-a) \qquad (11)$$

---

* For detailed discussion of the essential meaning and implications of this statement, see Appendix II.

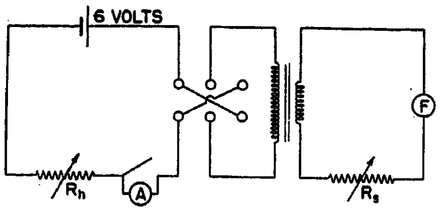


Figure 3. Circuit for studying variation of deflection with circuit resistance

(R_f and R_{sc}) in series with R_s is not shown

wherein $b>a$, and $|f_m(x)|$ designates the maximum magnitude of $f(x)$ in the interval $a \leqq x \leqq b$. Accordingly, it follows from equation 11 that

$$(UR/G)\left|\int_0^T \theta\, dt\right| \leqq (UR/G)T|\theta_m| \qquad (12)$$

wherein $|\theta_m|$ designates the maximum magnitude of θ over the time interval $0 \leqq t \leqq T$.

Obviously, $|\theta_m| \geq |\theta_2-\theta_1|$. Usually a fluxmeter is so adjusted and so used in practice that $\theta_1=0$. In this case, $|\theta_m| \equiv |\theta_2|$.

The Case When Both $R>>L$ and $[(RK/G) + G]>> URT/G$

The magnitude of the first term of the right-hand member of equation 10 is $[(RK/G)+G]|\theta_2-\theta_1|$. The magnitude of the second term is, by equation 12, less than or equal to $(URT/G)|\theta_m|$. Accordingly, it follows that if

$$[(RK/G)+G]|\theta_2-\theta_1| >> (URT/G)|\theta_m| \qquad (13)$$

equation 10 is well approximated by

$$\lambda_2-\lambda_1 = [(RK/G)+G](\theta_2-\theta_1) \qquad (14)$$

Now, it is to be noted that the right-hand side of equation 13 is directly proportional to R, whereas the left-hand side is positively biased by the constant term G. Accordingly, it follows: that for R sufficiently near zero, the right-hand side is very small by comparison with the left-hand side; that as R increases from zero, the right-hand side increases faster than does the left-hand side, thus weakening the inequality; and that possibly (the actual variation depends on the relative values of the other parameters) for sufficiently large R, the two sides become commensurate and the inequality no longer holds.

Accordingly, whether or not this last possibility occurs, there is a range of R for which equation 13 holds. Further, it follows from equation 13 that the upper limit of this range increases as G increases in magnitude and U decreases in magnitude. Now the gain through increasing G, say through using more turns on the fluxmeter coil, tends to be offset by the increased value of T resulting from the more sluggish action of the moving coil because of the increased moment of inertia J. On the other hand, decreasing U also tends to decrease T because of decrease in the restoring torque opposing motion, and decrease of T also tends to extend the maximum value of R for which equation 13 is applicable.

Now, equation 14 is a preferred operating characteristic for a commercial fluxmeter. For if a given fluxmeter is always used with search coils having a specified resistance, the circuit resistance R is substantially constant, and it follows from equation 14 that $(\lambda_2-\lambda_1)$ is of constant proportionality to $(\theta_2-\theta_1)$; whence the scale of the fluxmeter can be calibrated directly in terms of $(\lambda_2-\lambda_1)$. To attain this desired operating characteristic, a commercial fluxmeter is so designed that the moving coil is made as light as is feasible, the winding area as large as is feasible, and the restoring torque is reduced to a minimum. Then G, U, and T are of such magnitudes that the maximum value of R for which equation 13 holds is several times greater than the least value to which R can actually be reduced when using the instrument, namely, R equal to the internal resistance of the fluxmeter. Inasmuch as the bracketed term in equation 14 is positive, it follows that

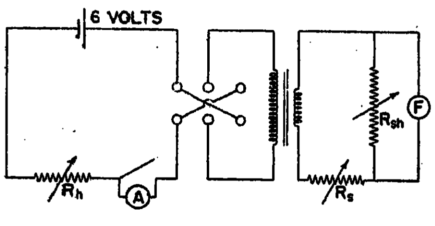


Figure 4. Circuit for studying variation of deflection with shunting resistance

R_f and R_{sc} are not shown

$$|\lambda_2-\lambda_1| = [(RK/G)+G]|\theta_2-\theta_1| \qquad (15)$$

The bracketed term in equation 15 is a constant, of specific value determined by the pertinent values of R, K, and G. Thus, it is convenient to write

$$\Delta\lambda = C'D \qquad (16)$$

wherein: $\Delta\lambda=|\lambda_2-\lambda_1|$ is the magnitude of the change in flux linkages; $D=|\theta_2-\theta_1|$ is the magnitude of the angular deflection of the moving coil, expressed in scale divisions of angular deflection of the fluxmeter pointer; and

$$C' = [(RK/G)+G] \qquad (17)$$

flux linkages/scale division of pointer deflection, is the "working constant" of the instrument.

Accordingly, it follows from equations 16 and 17 that a fluxmeter can be employed in a circuit containing arbitrary total resistance R, and that the change in flux linkages $\Delta\lambda$ associated with the search coil can be calculated with accuracy from equation 16, provided that the corresponding value of C' is known or can be determined, that the specified boundary conditions $w_1=w_2=i_1=0$ are observed, and that the restrictions $R>>L$ and $[(RK/G)+G]D>>(URT/G)|\theta_m|$ are satisfied. Now, as mentioned above, the fluxmeter is usually so adjusted and so used that $\theta_1=0$, whence $|\theta_m| \equiv |\theta_2| \equiv D$. If so, the latter restriction reduces to $[(RK/G)+G]>>(URT/G)$.

Determination of Flux

If $\Delta\lambda$ is written as $\Delta\lambda=N\Delta\phi$ where $\Delta\phi$ is the change in a flux ϕ linking each of the N_{sc} turns of a search coil, equation 16 can be written in the form $\Delta\phi=C'D/N_{sc}$. (This equation implies not only that the same flux ϕ links each of the N_{sc} turns but, further, that the diameter of the wire comprising the search coil is negligible by comparison with the diameter of the cross section of the search coil; whence essentially no flux passes through the wire of the coil. Though these conditions commonly are satisfied in a well-designed search coil, they often are not satisfied if a search coil is constructed by one lacking knowledge of the principles involved.) Obviously, if the change in flux is produced by reversal of a flux ϕ from ϕ to $-\phi$, then $\Delta\phi=2\phi$, whence $\phi=C'D/2N_{sc}$. Again, if the flux is changed from 0 to ϕ, or from ϕ to zero, then $\Delta\phi=\phi$, whence $\phi=C'D/N_{sc}$.

The Permissible Value of R

It obviously is desirable, for facility of general use, that the scale of a fluxmeter be calibrated directly in terms of the desired quantity $\Delta\lambda$. Inspection of equations 16 and 17 indicates that this can be done, providing R is held constant at

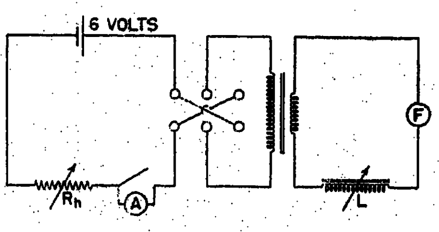


Figure 5. Circuit for studying variation of deflection with circuit inductance

(R_f+R_{sc}) is not shown

some convenient fixed value, say R_m. If R is not constant, calibration still can be essentially accomplished, provided R is allowed to vary only within limits such that $[(RK/G)+G] \cong [(R_mK/G)+G]$. As, in practice, the total resistances of different circuits tend to vary because of differences in the resistances of the leads and search coils utilized, fluxmeters are designed on this latter basis.

Thus, as purchased, the fluxmeter is direct reading for $\Delta\lambda$ and, thus, for $N_{sc}\Delta\phi$, providing it is used in a circuit where R is as specified by the manufacturer. (Usually, this specification is stated in the cover or on the scale of the instrument.) Thus, in Rawson fluxmeters the manufacturer specifies that the resistance of the search coil and associated leads shall not be greater than one ohm. This implies a permissible variation in R over the range $R_f \leqq R \leqq (R_f+1)$, where R_f is the internal resistance of the fluxmeter. Similarly, the early model of Sensitive Research Instrument fluxmeter has a value of R_f of about the same magnitude as that of the Rawson instrument, and the same restriction is stated.

Obviously, then, if R differs from the value R_m for which the scale is calibrated, the user must determine the appropriate value of C', for example, from a calibration curve prepared as discussed in the following section. This restriction on the permissible value of R if the fluxmeter is to be direct reading is an important point in the correct use of a fluxmeter; it is not emphasized or clearly stated in most textbook discussions of the instrument, and is frequently violated in practice if the user is without knowledge of the restriction. Commonly, this violation occurs through use of search coils comprised of many turns of fine wire, and thus of high resistance.

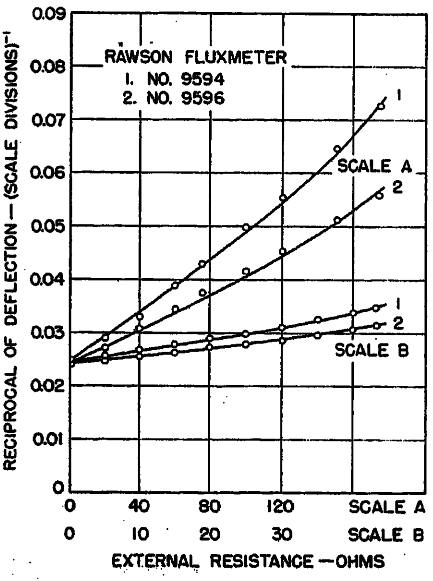


Figure 6. D^{-1} **versus** R_{ex}: **Rawson fluxmeters**

Determination of C'

For $R=R_m$, equation 17 can be written in the form

$$\Delta\lambda = CD \tag{18}$$

where

$$C=[(R_mK/G)+G] \tag{19}$$

Commonly, fluxmeters are designed so that, as received new from the manufacturer, the value of C is

$$\begin{aligned} C &= 10^4 \text{ maxwell-turns/scale division} \\ &\qquad \text{(centimeter-gram-second units)} \\ &= 10^{-4} \text{ weber-turns/scale division} \\ &\qquad \text{(meter-kilogram-second units)} \end{aligned} \tag{20}$$

Manufacturers print this value of C (usually in centimeter-gram-second units) on the scale of the instrument. However, it commonly happens that with use, or through repair, unless done by the manufacturer, the characteristics of the fluxmeter change somewhat, thus necessitating redetermination of C. Accordingly, when using a fluxmeter it is wise to ascertain whether or not such a redetermined value is to be used; if so, it is commonly to be found recorded in the cover of the instrument.

Occasionally a fluxmeter must be used in a circuit where R is appreciably larger than R_m. For such use it is convenient to have available a calibration curve of C' versus R or R_{ex}. The co-ordinates for such a curve can be obtained in several ways. Thus, for various values of R or R_{ex} the deflections corresponding to a known fixed change in flux linkages $\Delta\lambda$ can be noted and the values of C' calculated from equation 17. The fixed value of flux linkages can be ascertained by initial measurement with $R=R_m$, whence $C'=C$, and $\Delta\lambda$ can be calculated from equation 18; or else a fixed known value can be obtained by reversing a known current passed through a standard mutual inductor or a standard solenoid; or still other artifices can be employed. Again, the values of (K/G) and G can be determined experimentally as discussed in following paragraphs, and values of C', corresponding to various assumed values of R, can be calculated from equation 17.

Extending the Range of a Fluxmeter

The possibility of easy determination of C' enables use of a fluxmeter in a circuit where the value of R is substantially greater than R_m: for example, in a circuit where the search coil, wound of many turns of fine wire for use in mapping a weak magnetic field, is of high resistance. In this case, it follows from equations 17 and 19 that $C'>C$. If so, it follows from equation 16 that a greater change in flux linkages $\Delta\lambda$ is required to produce a given deflection D, now that $C'>C$ is the working constant of the instrument. In turn, this analysis suggests that the range of a fluxmeter can be extended by inserting resistance in series with the fluxmeter. However, this method of extending the range of a fluxmeter is less preferable than that of shunting the fluxmeter coil with a low resistance, for, as is shown below, shunting a fluxmeter, rather than inserting series resistance, enables a greater deflection for the same change in flux linkages, and hence provides greater sensitivity of measurement. In consequence, commercial multiple-range fluxmeters are multiranged by use of shunts.

Incorrect Use of Equations 16 and 18

In conclusion, it is to be noted that use of equations 16 and 18 is subject to the restrictions: $w_2=w_1=i_1=0$, $R>>L$, $[(RK/G)+G]|\theta_2-\theta_1|>>|\theta_m|URT/G$. If an of these conditions are substantially departed from, correspondingly modified equations must be used. As a particular example, a student inexperienced in the use of a fluxmeter, having obtained a deflection by reversal of a flux ϕ, and being desirous of obtaining a check through re-reversing the flux to its original value and direction, often does so while the pointer is returning towards its initial position with appreciable velocity

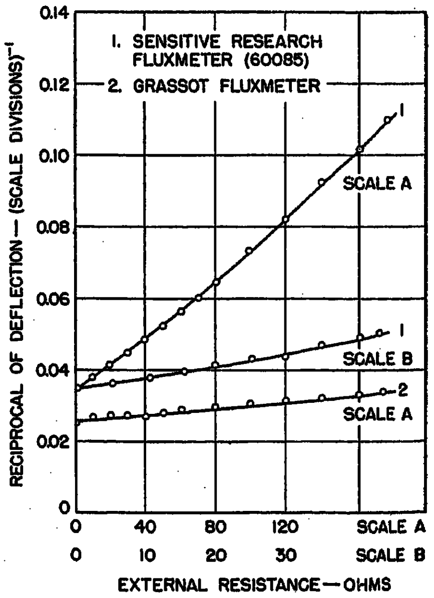


Figure 7. D^{-1} **versus** R_{ex}: **Grassot and Sensitive Research Instrument fluxmeters**

w. At the instant t_1 of re-reversal, however, $w_1>0$; whence it follows from equation 8 that, because of the resulting smaller values of $(JR/G)(w_2-w_1)$ a given change in flux linkages $(\lambda_2-\lambda_1)$ results in a greater value of $(\theta_2-\theta_1)$, and hence a greater pointer deflection D, than would have resulted if w_1 were equal to zero. In consequence, if equation 16 or 18 is, incorrectly, used to calculate $\Delta\lambda$, the larger value of D results in a calculated value of $\Delta\lambda$ greater than the actual value. Obviously, the magnitude of error increases as w_1 increases. In consequence, if through damage and poor repair a fluxmeter coil tends to return rather rapidly towards its initial position, a substantial error easily can result if the fluxmeter is so used.

Derivation of the General Equation for the Shunted Fluxmeter

In the circuit of Figure 2 let $e_{sc}=d\lambda/dt$ be the voltage generated in the search coil, $e_f=-Gd\theta/dt$ the voltage generated in the moving coil of the fluxmeter, i_{sc} the current through the search coil, i_{sh} the current through the shunt, i_f the current through the fluxmeter, L_t and $R_t=(R_{sc}+R_s)$ the total inductance and total resistance of the branch containing the search coil, R_{sh} the resistance, including leads of the shunt, and L_f and R_f the total inductance and total resistance of the branch containing the fluxmeter coil.

Applying Kirchhoff's voltage law to the right-hand loop of Figure 2 yields

$$d\lambda/dt=(i_{sh}+i_f)R_t+L_t d(i_{sh}+i_f)/dt+i_{sh}R_{sh} \quad (21)$$

Applying Kirchhoff's voltage law around the outside of the circuit of Figure 2 yields

$$d\lambda/dt=(i_{ts}+i_f)R_t+L_t d(i_{sh}+i_f)/dt+i_f R_f+L_f(di_f/dt)+Gd\theta/dt \quad (22)$$

By analogy with equation 5, the differential equation characterizing the mechanical motion of the fluxmeter coil is

$$Gi_f=Jd^2\theta/dt^2+Kd\theta/dt+U\theta \quad (23)$$

Subtracting equation 21 from equation 22 and solving for i_{sh} yields

$$i_{sh}=R_{sh}^{-1}[i_f R_f+L_f di_f/dt+Gd\theta/dt] \quad (24)$$

Solving equation 23 for i_f and substituting in equation 24 yields

$$i_{sh}=R_{sh}^{-1}G^{-1}[R_f Jd^2\theta/dt^2+R_f Kd\theta/dt+R_f U\theta]+R_{sh}^{-1}L_f di_f/dt+R_{sh}^{-1}Gd\theta/dt \quad (25)$$

Integrating equation 21 between t_1 and t_2 gives

$$\int_{t_1}^{t_2}d\lambda=(R_t+R_{sh})\int_{t_1}^{t_2}i_{sh}dt+R_t\times\int_{t_1}^{t_2}i_f dt+L_t[i_{sh}+i_r]_{t_1}^{t_2} \quad (26)$$

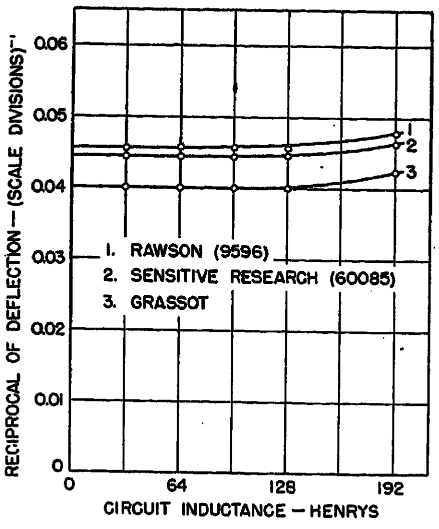


Figure 8. D^{-1} versus circuit inductance L

Substituting from equations 23 and 25 in equation 26 as indicated, performing the indicated integration, and collecting coefficients of like terms yields

$$\lambda_2-\lambda_1=[R_t+R_f(1+R_tR_{sh}^{-1})]JG^{-1}(w_2-w_1)+\{[R_t+R_f(1+R_tR_{sh}^{-1})]KG^{-1}+(1+R_tR_{sh}^{-1})G\}(\theta_2-\theta_1)+(1+R_tR_{sh}^{-1})L_f(i_{f2}-i_{f1})+L_t(i_{sh2}+i_{f2}-i_{sh1}-i_{f1})+[R_t+R_f(1+R_tR_{sh}^{-1})]\times UG^{-1}\int_{t_1}^{t_2}\theta dt \quad (27)$$

Equation 27 can be recast in a form more convenient to analyze; namely

$$\lambda_2-\lambda_1=(R_p+R_f)R_tR_p^{-1}JG^{-1}(w_2-w_1)+[(R_p+R_f)KG^{-1}+G]R_tR_p^{-1}(\theta_2-\theta_1)+R_tR_p^{-1}L_f(i_{f2}-i_{f1})+L_t(i_{sh2}+i_{f2}-i_{sh1}-i_{f1})+(R_p+R_f)R_tR_p^{-1}UG^{-1}\times\int_{t_2}^{t_1}\theta dt \quad (28)$$

where $R_p=R_tR_{sh}/(R_t+R_{sh})$.

Equation 28 is the desired general equation relating change in flux linkages associated with the search coil to the corresponding change in coil position.

The General Equation for the Conditions Usually Encountered in Practice

At $t_1=0$, with the fluxmeter coil at rest ($w_1=0$) at $\theta=\theta_1$ and with zero current ($i_1=0$) through it, let a quick change in the flux linkages associated with the search coil be initiated. Let $t_2=T$ be the time at which the fluxmeter pointer attains maximum deflection. At this instant $\theta=\theta_2$, $w_2=0$ and $i=i_2$.

Under these conditions equation 28 yields

$$\lambda_2-\lambda_1=[(R_p+R_f)KG^{-1}+G]R_tR_p^{-1}(\theta_2-\theta_1)+[R_tR_p^{-1}L_f i_{f2}+L_t(i_{sh2}+i_{f2})]+(R_p+R_f)R_tR_p^{-1}UG^{-1}\int_0^T\theta dt \quad (29)$$

As discussed previously, it is commonly true in practice that L_t and L_f are small against the corresponding values of R_t and R_f; whence, as before, $i_{sh2}=i_{f2}\cong 0$. In such case the second term of the right-hand member of equation 29 vanishes. Again, in fashion analogous to the evaluation of the integral term of equation 10, it is to be deduced that the magnitude of the integral term is small by comparison with the magnitude of the first term for a range of (R_p+R_f) such that

$$[(R_p+R_f)KG^{-1}+G]|\theta_2-\theta_1|>>(R_p+R_f)UTG^{-1}|\theta_m| \quad (30)$$

This inequality is the same as the inequality of equation 13, except that R now is replaced by (R_p+R_f). Accordingly, the analysis and remarks developed previously for the unshunted fluxmeter are applicable in obvious fashion to the shunted fluxmeter under discussion here.

In particular, it follows that there is a range of (R_p+R_f) for which the inequality of equation 30 is valid, and that over this range equation 29 is well approximated by

$$\lambda_2-\lambda_1=[(R_p+R_f)KG^{-1}+G]R_tR_p^{-1}(\theta_2-\theta_1) \quad (31)$$

subject to the stated boundary conditions and the restrictions $R_t>>L_t$ and $R_f>>L_f$.

The coefficient of $(\theta_2-\theta_1)$ in equation 31 is a constant of specific value determined by the pertinent values of (R_p+R_f), R_t, K, and G. Thus, again it is convenient to write equation 31 in the form

$$\Delta\lambda=C'D \quad (32)$$

where now

$$C'=[(R_p+R_f)KG^{-1}+G]R_tR_p^{-1} \quad (33)$$

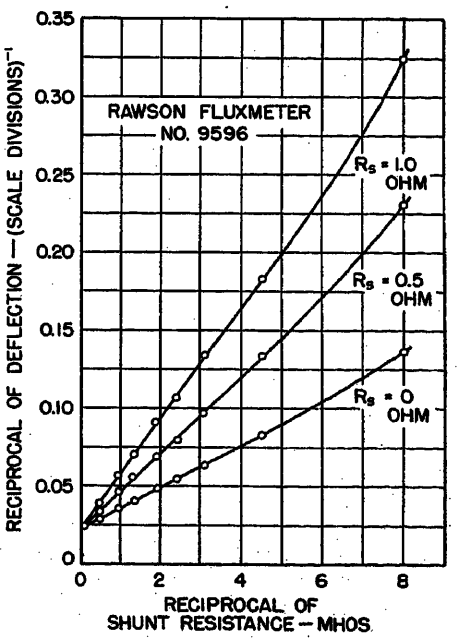


Figure 9. D^{-1} versus R_{sh}^{-1}: Rawson fluxmeter

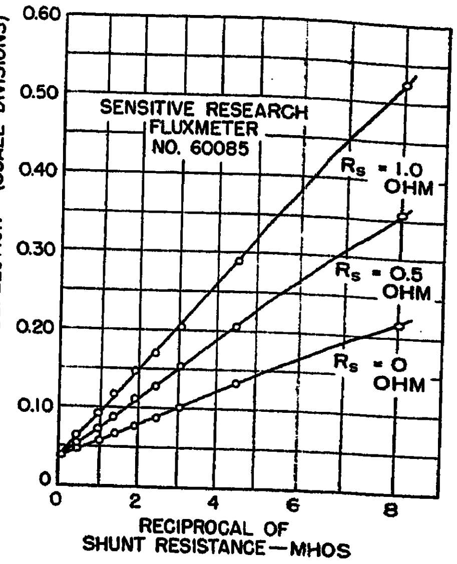


Figure 10. D^{-1} versus R_{sh}^{-1}: Sensitive Research Instrument fluxmeter

Thus, for a given fluxmeter in a given circuit, the coefficient C' in equation 32 is fixed in value. Hence, providing the same precautions are observed as discussed at length for the unshunted fluxmeter, the scale of the instrument can be calibrated in terms of the changes in flux linkages associated with the search coil $\Delta\lambda$. Moreover, as the value of C' depends on the assigned value of $R_p = R_{sh}R_t/(R_{sh}+R_t)$, multiple-range instruments can be obtained by use of several different fixed values of R_{sh}.

Experimental Apparatus and Procedure

To obtain experimental confirmation of the correctness of the theory developed above and to obtain quantitative measure of the error to be expected from using the approximate equations 16 and 32, investigation was made of the effects of (1) increasing the resistance external to an unshunted fluxmeter, (2) decreasing the shunting resistance of a shunted fluxmeter, and (3) increasing the inductance external to an unshunted fluxmeter.

1. Figure 3 illustrates the circuit used to investigate the increase of resistance in the circuit of an unshunted fluxmeter. A 6-volt battery supplies current to the 485-turn winding of an iron-cored solenoid (approximately 8 inches long and 1.5 inches in diameter) through a double-pole double-throw reversing switch, ammeter, and carbon-plate rheostat. A flat circular 8-turn search coil, wound on and coaxially with the solenoid, is connected to the fluxmeter through a precision decade resistance box R_s. Runs 1 to 4 were made on four instruments: two Rawson model 504 fluxmeters, a Sensitive Research Instrument Corporation model *F* fluxmeter, and an imported Grassot fluxmeter. The procedure of each run was the same. R_s was set at zero ohms and R_h adjusted to that value which resulted in approximately half full-scale deflection when the double-pole switch was closed. Then, with the current held constant, R_s was increased in suitable increments from 0 to 200 ohms. At each value of R_s, the switch *S* was closed, the deflection *D* noted, the switch opened, and the fluxmeter pointer brought to rest; this same cycle was repeated twice more, and the three deflections, which were nearly identical, were averaged. At the end of the run, the circuit leads were disconnected from the fluxmeter and connected to a precision 4-decade d-c resistance bridge; the decade resistance box R_s was set in turn to each of the values used during the run, and the total external-circuit resistance R_{ex} was measured. The resistance measured when the decade box is set at zero ohms yields the resistance of the search coil plus circuit leads. Table I comprises a typical set of data—that of run 1.

2. Figure 4 illustrates the circuit used to investigate the effect of decreasing the shunting resistance R_{sh} of a shunted fluxmeter. The circuit derives from that of Figure 3 through the addition of a shunting precision decade resistance box R_{sh}. Runs 5 to 8 were made on four fluxmeters, one of the Rawson instruments noted above being replaced by a General Electric catalog number *32C24764* fluxmeter. Three sets of data were taken for each instrument. For fixed values of R_s equal, in turn, to 0, 0.5, and 1 ohm, R_{sh} was decreased in suitable decrements for infinity (open circuit) to a value yielding a pointer deflection of only several divisions. Data were taken as described above. Finally, the total resistance R_t of the series combination of search coil, resistance box, and leads and the resistance R_{sh} of the shunting resistance box and its leads, were accurately measured with the resistance bridge. Table II comprises a typical set of data—that of Run 5.

3. Figure 5 illustrates the circuit used to investigate the effect of increasing the total inductance *L* relative to the total circuit resistance *R* of an unshunted fluxmeter. The circuit is that of Figure 3 with the precision decade resistance box R_s replaced by a variable heavy iron-cored inductor. Runs 9 to 11 were made on the three fluxmeters used in runs 2, 3, and 4. For a fixed circuit resistance of 26 ohms external to the fluxmeter, *L* was increased in suitable increments from zero to the maximum obtainable value. Data were taken as previously described. The utilized values of *L* then were calculated from measured values of 60-cycle impedance and resistance. Table III comprises the data of runs 9 to 11.

Experimental Confirmation of the Theory of the Unshunted Fluxmeter

If C' from equation 17 is substituted in equation 16 and the resulting expression solved for $D^{-1}(\equiv 1/D)$ we obtain

$$D^{-1} = (K/G\Delta\lambda)R + G/\Delta\lambda \tag{34}$$

Inasmuch as $R = (R_{ex}+R_f)$, equation 34 can be recast in the form

$$D^{-1} = (K/G\Delta\lambda)R_{ex} + (KG^{-1}R_f + G)/\Delta\lambda \tag{35}$$

In equation 34 let $\Delta\lambda$ be of fixed value and the equation be interpreted as expressing D^{-1} as a function of *R*. Then equation 34 is that of a straight line, where $(K/G\Delta\lambda)$ is the slope and $(G/\Delta\lambda)$ is the ordinate intercept. Obviously, equation 35 can be similarly analyzed.

Accordingly, if for $R >> L$ and a fixed value of $\Delta\lambda$, *D* is observed as a function of *R*, and if, from these data, D^{-1} is plotted as a function of *R* or R_{ex}, it is to be expected that the resulting curve will be a straight line over that range of *R* for which equations 34 and 35 are valid, namely, the range of *R* for which equation 13 holds.

The curves of Figures 6 and 7, plotted from the data of runs 1 to 4, are precisely such curves. It is to be noted that the

Table I. Data of Run 1 on the Grassot Fluxmeter

R_s	D	D^{-1}	R_s	D	D^{-1}
0.590	38.5	2.60 × 10^{-2}	60.6	35.2	2.84 × 10^{-2}
5.580	38.0	2.63	65.6	35.0	2.86
10.6	37.9	2.64	75.6	43.3	2.92
15.6	37.8	2.65	90.6	33.9	2.95
20.6	37.2	2.69	100.6	33.0	3.03
25.6	37.0	2.70	120.6	32.0	3.12
35.6	36.3	2.76	135.6	31.3	3.20
40.6	36.1	2.77	150.6	30.2	3.31
45.6	36.0	2.78	175.6	29.5	3.39
55.6	35.4	2.80	200.6	28.8	3.57

curves for the Rawson and Sensitive Research fluxmeters are linear over essentially the same range of external circuit resistance—about 45 ohms—whereas the Grassot fluxmeter is linear up to about 180 ohms. The greater range of linearity of the Grassot fluxmeter is due to the very delicate system of suspension and spiral connections employed by the moving coil. In consequence, the Grassot fluxmeter has a much smaller value of U. In turn, this smaller value of U enables satisfaction of equation 13 over a range of R several times greater than that of the other instruments.

It is to be noted that beyond the range of linearity the curves of Figures 6 and 7 bend upwards, thus indicating that for a given value of R the value of D^{-1} is greater than would have been obtained if the linear law had persisted over these values of R. This also is to be expected, for the linear characteristic is that which stems from equation 10 when the integral term can be neglected. If it cannot be neglected, it follows that for a given change in flux linkages $(\lambda_2-\lambda_1)$ at a given value of R, the first term of the right-hand member of equation 10 and, therefore, the actual value of $(\theta_2-\theta_1)$, must be smaller than it would be if the integral term were absent. Accordingly, the actual value of $D\equiv|\theta_2-\theta_1|$ will be less and, therefore, the actual value of D^{-1} greater than that calculated on the basis of the linear characteristic stemming from neglect of the integral term. This is precisely what the curves of Figures 6 and 7 evidence.

The curves of Figure 8 are plotted from the data of runs 9 to 11, as tabulated in Table III. In these runs R and $\Delta\lambda$ are held constant; D is determined for increasingly large values of L. Figure 8 evidences that, as L increases, D^{-1} increases; equivalently, as L increases, D decreases—which is to be expected, for, in equation 10, the integral term can be neglected—in which case $(\theta_2-\theta_1)$ will be constant for a constant change in flux linkages $(\lambda_2-\lambda_1)$—only if equation 13 is satisfied. In the experimental work, $\theta_1=0$; whence $|\theta_2-\theta_1|=|\theta_2|\equiv|\theta_m|$ and the inequality equation 13 reduces to $[(RK/G)+G]>>URT/G$. As L increases, however, the motion of the moving coil becomes increasingly more sluggish because of the slower rate of increase of the induced current in the circuit and, as was actually found in the experimental work, T increases substantially with L. Thus, the right-hand side of the inequality increases with L, and the left-hand side is constant; the inequality weakens as L increases. Eventually a value of L is reached for which the integral term in equation 10 cannot be neglected. Since this term increases in magnitude relative to the preceding term as L increases, since $(\lambda_2-\lambda_1)$ is constant, and since the coefficient $[(RK/G)+G]$ is constant, it follows that $(\theta_2-\theta_1)$ must decrease as L increases. Thus, $|\theta_2-\theta_1|=D^{-1}$ increases with L—in precise agreement with the curves of Figure 8. As before, the slower increase of D^{-1} manifest for the Grassot fluxmeter stems from its value of U being much smaller than those of the other two fluxmeters.

Table II. Data of Run 5 on the Grassot Fluxmeter

R_{sh}	R_{sh}^{-1}	D			D^{-1}		
		$R_s=0$	$R_s=0.5$	$R_s=1$	$R_s=0$	$R_s=0.5$	$R_s=1$
∞	0.000	44.0	44.0	44.0	2.27×10^{-2}	2.27×10^{-2}	2.27×10^{-2}
10.02	0.100	41.9	40.0	38.0	2.49	2.50	2.63
2.02	0.495	33.7	28.0	24.0	2.96	3.57	4.16
1.02	0.980	27.5	21.0	17.0	3.64	4.75	5.88
0.72	1.385	24.0	17.1	13.4	4.16	5.85	7.45
0.52	1.915	20.1	13.9	11.0	4.98	7.20	9.10
0.42	2.470	18.0	12.0	9.0	5.55	8.33	11.10
0.32	3.090	15.1	9.6	7.4	6.62	10.40	13.50
0.22	4.500	11.8	7.0	5.4	8.47	14.30	18.50
0.13	8.000	7.1	4.1	2.8	14.10	24.40	35.70

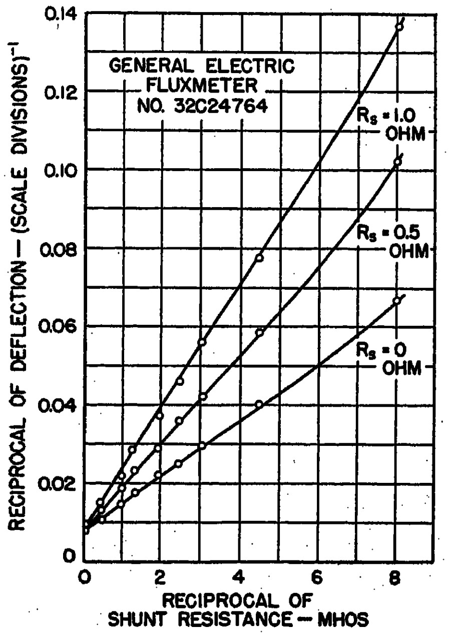


Figure 11. D^{-1} versus R_{sh}^{-1}: General Electric fluxmeter

Obviously, Figure 8 confirms Golding's[6] conjecture that a substantial value of inductance can be inserted in a fluxmeter circuit without appreciable change in the working constant of the fluxmeter.

Experimental Confirmation of the Theory of the Shunted Fluxmeter

If C' from equation 33 is substituted in equation 32, R_p^{-1} replaced by its value $(R_t+R_{sh})/R_tR_{sh}$, and the resulting expression solved for D^{-1}, we obtain

$$D^{-1}=[(1/\Delta\lambda)(R_fKG^{-1}+G)R_t]R_{sh}^{-1}+ (1/\Delta\lambda)[(R_t+R_f)KG^{-1}+G] \quad (34)$$

In equation 34 let $\Delta\lambda$ and R_t be of fixed value and let the equation be interpreted as expressing D^{-1} as a function of R_{sh}^{-1}. Then equation 34 is that of a straight line where $(1/\Delta\lambda)(R_fKG^{-1}+G)R_t$ is the slope and $(1/\Delta\lambda)[(R_t+R_f)KG^{-1}+G]$ is the ordinate intercept. Accordingly, if for $R_t >> L_t$ and $R_f >> L_f$ and for fixed values of $\Delta\lambda$ and R_t, D is observed as a function of R_{sh}^{-1}, it is to be expected that the resulting curve will be a straight line over the range of R_{sh}^{-1} for which equations 32 and 34 are valid, namely, the range of R_{sh}^{-1} for which equation 30 holds.

The curves[*] of Figures 9 to 12, plotted from the data of runs 5 to 8, are precisely such curves. The curves bend upwards beyond the linear range, for reason analogous to that discussed previously in accounting for the corresponding phenomenon in the curves of Figures 6 and 7. The increase, as R_s increases, of the slopes of the linear portions of the three curves of a given figure stems from the fact, evidenced in equation 34, that the slope is

---

[*] In examining these curves, it is to be recalled that R_s is the resistance of only the variable decade resistor R_s of Figure 4.

Table III. Data for Runs 9 to 11

L Henrys	R_{ex}	Rawson D	Rawson D^{-1}	Sensitive Research D	Sensitive Research D^{-1}	Grassot D	Grassot D^{-1}
0	26	22	4.55×10^{-2}	22.5	4.45×10^{-2}	25.0	4.00×10^{-2}
32	26	22	4.55	22.5	4.45	25.0	4.00
64	26	22	4.55	22.5	4.45	25.0	4.00
96	26	22	4.55	22.5	4.45	25.0	4.00
128	26	22	4.55	22.5	4.45	25.0	4.00
192	26	21	4.76	21.0	4.65	23.5	4.25

Table IV. Co-ordinates for Figures 13 and 14 for Grassot Fluxmeter

R_{ex}	R	D	C'/C	R_{ex}	R	D	C'/C
0	100	38.5	1.00	50	150	35.8	1.08
10.0	110	37.9	1.02	80	180	34.2	1.13
20.0	120	37.4	1.03	120	220	32.1	1.20
30.0	130	36.8	1.05	160	260	30.0	1.28
40.0	140	36.2	1.06	200	300	27.9	1.38

directly proportional to R_t, increasing with R_t and thus with R_s, since $R_t=(R_s+R_{sc})$. A corresponding increase in the ordinate intercept $(1/\Delta\lambda)[(R_t+R_f)\times KG^{-1}+G]$ actually occurs as R_s increases from 0 to 0.5 to 1 ohm, but because of the relative magnitudes of the parameters in this expression, the increases are too small to be detected on the curves. Accordingly, all three curves appear to have the same ordinate intercept.

Determination of C'

The various excellent agreements between theory and experiment as previously set forth substantiate in general the correctness of the theory developed for the unshunted and the shunted fluxmeters. In particular, it follows that a fluxmeter can be used for accurate measurement in a circuit where the resistance external to the fluxmeter is any arbitrary value within the linear range of the instrument, providing the corresponding value of C' is used in working equation 16 or 32.

For an unshunted fluxmeter it follows from equation 16 that, if $\Delta\lambda$ is of constant value, then $C'D'=CD_C$, or, equivalently

$$(C'/C)=(D')^{-1}/D_C^{-1} \qquad (35)$$

where C' and $(D')^{-1}$ are corresponding values for an arbitrary value of total circuit resistance R, and C and D_C^{-1} are corresponding values for $R=R_m$. R_m and C are stated by the manufacturer; D_C^{-1} and $(D')^{-1}$ can be obtained from a constant $\Delta\lambda$ curve of D^{-1} versus R or a constant $\Delta\lambda$ curve of D^{-1} versus R_{ex}, as in Figures 6 and 7. If, with values so obtained, the corresponding values of C' versus R or R_{ex} are plotted, the resulting calibration curve furnishes ready determination of the working constant C' for any value of R or R_{ex} that can be utilized in practice.

For convenience of use it usually is preferable to plot a calibration curve of C'/C versus $R_{ex}=R-R_f$. The curves of Figure 13 are precisely such curves. These curves are plotted from data typified by that of Table IV for the Grassot fluxmeter. The values therein are calculated, as just outlined, from smoothed data taken from the pertinent curve of Figure 6. For each of the three fluxmeters, $C'=C$ when $R=R_m=R_f$, for the manufacturer's injunction that the external resistance R_{ex} be kept below one ohm implies that the working constant is C when $R_{ex}=0$. Accordingly, a value of C'/C corresponding to a given value of R_{ex} is to be calculated by dividing the corresponding value of D^{-1} by the value of $D^{-1}\equiv D_C^{-1}$ for $R_{ex}=0$.

Figure 14 comprises a calibration curve of C'/C versus R, and is included for use with the material which follows.

The linearity of the curves of Figures 13 and 14 is to be expected, for over the linear range of R, the constant C' is defined by equation 17. This can be written in the form

$$(C'/C)=(K/GC)R+(G/C) \qquad (36)$$

Since $R=R_{ex}+R_f$ equation 36 can be written in the form

$$(C'/C)=(K/GC)R_{ex}+(K/GC)R_f+(G/C) \qquad (37)$$

For the fluxmeters under discussion, $R_m=R_f$. Accordingly, it follows from equation 19 that equation 37 can be written in the form

$$C'/C=(K/GC)R_{ex}+1 \qquad (38)$$

If equation 36 be interpreted as expressing C'/C as a function of R, it is the equation of a straight line of slope K/GC and ordinate intercept G/C. Similarly, equation 38 is that of a straight line of slope K/GC and ordinate intercept one. Accordingly, if, over the linear range of R, experimentally determined values of C'/C be plotted as a function of R or R_{ex}, it is to be expected that the resulting curve will be a straight line—as in Figures 13 and 14.

The values of C for the fluxmeters of Figures 13 and 14 are encompassed in Table V. As purchased new, each fluxmeter had a working constant of $C=10^4$ maxwell-turns. But because of student use, with resulting damage and repair in the school's instrument shop, the present values of C are as tabulated. These values were determined by use of a Cenco Standard Variable Magnetic Inductor. Interestingly, the C of the Grassot instrument, maintained as the laboratory standard, has not changed appreciably since purchase several decades ago. This indicates that if a fluxmeter has been used only by those experienced and careful in use of it, C can be taken as the new value; it is not necessary to make a separate determination of C prior to using the calibration curve of C'/C versus R or R_{ex}.

Determination of the Parameters of a Fluxmeter

As discussed immediately after equation 38, the curves of Figure 14 are straight lines of slope K/GC and ordinate intercept G/C. Accordingly, the values of K/GC and G/C can be scaled from the curves of Figure 14. From these values and the values of C in Table V, K/G, G, and K can be calculated. The corresponding numerical values are tabulated in Table V.

The units of the tabulated values are: R_f in ohms; C and G in maxwell-turns per scale-division; K/G in maxwell-turns per ohm scale-division; K in maxwell-turns^2 per ohm scale-division^2; and U_m in maxwell-turns^2 per ohm second scale-division.^{2}

To determine U we note that equation 10 can be written in the form

$$D^{-1}=(K/G\Delta\lambda)(R_{ex}+R_f)+(G/\Delta\lambda)+(URD^{-1}/G\Delta\lambda)\int_0^T\theta dt \qquad (39)$$

This equation is the over-all characteristic of the curves of Figures 6 and 7. Omission of the integral term yields equation 34, the equation of the linear portion of the curves of Figures 6 and 7, hence also the equation of the straight lines obtained by extending the linear portions. Accordingly, it follows that, for a given

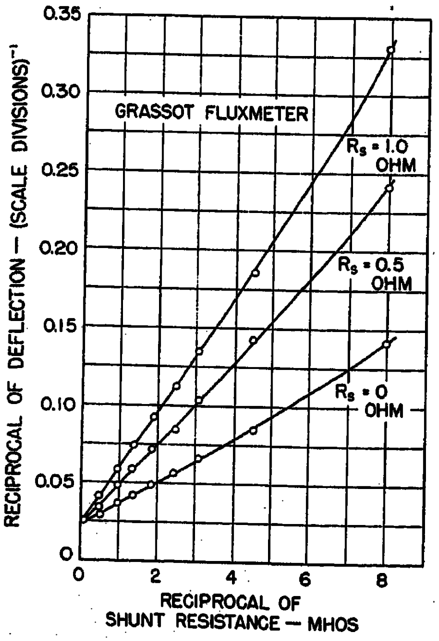


Figure 12. D^{-1} versus R_{sh}^{-1}: Grassot fluxmeter

value of R_{ex}, hence of $(R_{ex}+R_f)$, the integral term is the magnitude of the difference between the actual ordinate of the curve D^{-1} and the ordinate of the linear portion extended d^{-1}. Now as discussed previously, $\left|\int_0^T \theta dt\right| = T\left|(\theta_2-\theta_1)_m\right|$. All experimental data were taken for $\theta_1=0$, whence $\left|(\theta_2-\theta_1)_m\right| = \left|\theta_2\right| \equiv D$. Accordingly for the case at hand, $(D^{-1}-d^{-1}) \equiv (URD^{-1}/G\Delta\lambda)\int_0^T \theta dt \leqq URT/G\Delta\lambda$. In consequence, $(D^{-1}-d^{-1})G\Delta\lambda/RT \leqq U$, or since $\Delta\lambda = CD_C$

$$(D^{-1}-d_1^{-1})GCD_C/RT \leqq U \qquad (40)$$

Equation 40 yields a minimum value of U. Actual observation of θ as a function of t indicates that the two are approximately linearly related over a considerable portion of the deflection. In consequence, the actual value of U is approximately twice the minimum value of equation 40. But although equation 40 thus does not yield the actual value of U, it furnishes information sufficient to ascertain the order of magnitude of U for the fluxmeters of Table V and also the relative sizes of the parameters of the three instruments—all that is desired for the purpose at hand.

Minimum values of U, calculated from equation 40, are tabulated in Table V. The necessary values of the ordinates have been scaled from the curves of Figures 6 and 7 for $R_{ex}=200$. Calculation is typified by that for the Sensitive Research Instrument fluxmeter: $(D^{-1}-d^{-1}) = 1.44\times10^{-2}$, $U_m = [(1.44\times10^{-2})(8.9\times10^{-3})(1.13\times10^4)(28.8)/(211)\times(1)] = 1.97\times10^5$.

It is to be recalled that D_C is the deflection corresponding to $R_{ex}=0$, $R=R_{ex}+R_f$; and that $T=1$ second. If R is expressed in abohms, then $U_m = 1.97\times10^{-4}$ dyne-centimeter per scale division.

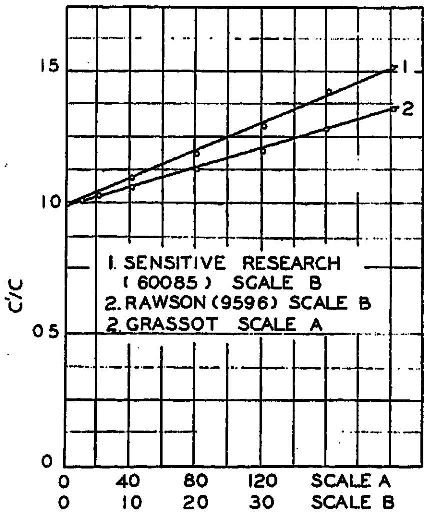


Figure 13. Calibration curves of C' versus external resistance R_{ex}

Table V. Parameters of Fluxmeters*

Fluxmeter	R_f	C	K/G	G	K	U_m
Rawson	16.5	1.08×10^4	83.5	8.7×10^3	72.7×10^4	0.57×10^5
Sensitive Research Instrument	11.0	1.13×10^4	107.0	8.9×10^3	95.0×10^4	1.94×10^5
Grassot	100.0	1.00×10^4	16.3	8.4×10^3	13.7×10^4	0.10×10^5

* See text for statement of units of tabulated values.

The small value of the U_m of the Grassot fluxmeter stems from the previously mentioned very delicate system of suspension and spiral connection employed with the moving coil.

Misstatements in Textbooks

The fact that some textbooks state, incorrectly, that the deflection of a fluxmeter is proportional to the change in flux, rather than—correctly—to the change in flux linkages, has been mentioned in the introduction. Again, in developing a theory of the fluxmeter, some textbooks assume that the "RI drop" can be neglected. Commonly, this term is considered negligible because the current is small, whence also the RI drop is small. The resulting analysis yields a working constant equivalent to that defined by equation 14 where $R=0$. As evidenced by Figure 14, however, the working constant of the Grassot fluxmeter, for example, would be 0.834×10^4 for $R=0$, as compared with the actual value of 10^4 for $R=R_f=100$ ohms; or, again, that of the Rawson fluxmeter would be 0.838×10^4 for $R=0$, as compared with the actual value of 1.08×10^4 for $R=R_f=16.5$ ohms.

Obviously, it is most desirable that a textbook development of the theory of the fluxmeter should retain the iR term of equation 4, obtain the actual linear-range working constant C' of equation 14, and therefrom stress the significance of maintaining the resistance of the fluxmeter circuit at substantially the manufacturer's value of R if his value of C is to be used as the working constant. As an example of the practical importance of such a discussion, it is to be noted that the working constant $C=10^4$ of the multiple-range fluxmeter manufactured by the Sensitive Research Instrument Corporation is based on using the fluxmeter with a special 15-ohm search coil. If some other search coil of small resistance is used, as may be necessary for a particular measurement, it is imperative that additional series resistance be added to make $R_{ex}=15$ ohms if $C=10^4$ is still to be the working constant. The necessity of adding the additional series resistance is obvious if the dependence of C' on R is known to the user, but otherwise the necessity is not evident.

Summary

1. An inclusive mathematical theory of the operation of the unshunted fluxmeter is developed. Previously published accounts of the theory of the unshunted fluxmeter are analyzed and found to be encompassed as special subcases of the theory developed in this paper.

2. From the general equation of operation a simple approximate equation is deduced which can be utilized for accurate calculation, provided the stated pertinent boundary conditions and restrictions are observed.

3. Experimental data are advanced which confirm the correctness of the general theory and make clear the necessity of observing the stated boundary conditions and restrictions.

4. Analysis and experimental confirmation analogous to that outlined in the three preceding paragraphs is advanced for the shunted fluxmeter.

5. Experiment reveals that a considerable amount of inductance can be inserted in series with a fluxmeter without substantially changing the working constant of the instrument.

6. Experiment indicated that a fluxmeter can be used in a search-coil circuit of arbitrary resistance up to, in general, several times its internal resistance, provided that the corresponding numerical value of the working constant C' is selected from the

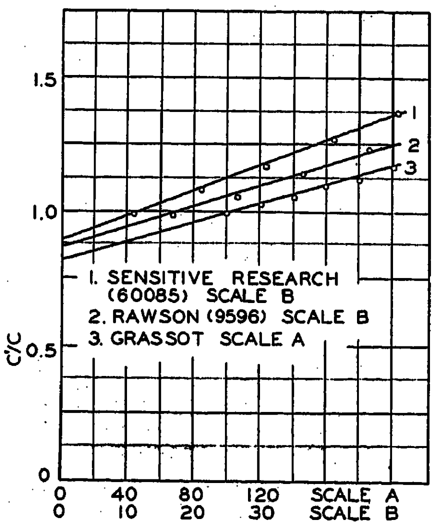


Figure 14. Calibration curves of C' versus total circuit resistance R

calibration curve of C' versus circuit resistance. A combined numerical-experimental procedure for effecting this calibration curve is advanced. This procedure is illustrated by effecting the calibration curves of several fluxmeters.

7. A procedure for determining the mechanical and electrical parameters of a fluxmeter is advanced and illustrated by calculation of the parameters of several different fluxmeters.

8. Several misstatements commonly found in reference sources are mentioned and suggestion advanced as to how to correct these inaccurate statements.

Appendix I

In 1902 Beattie[1] suggested that a pivot-type D'Arsonval galvanometer designed with minimum restoring torque and used in a circuit of low resistance could be employed for determination of the flux in an iron specimen on which a hysteresis loop was being run. Taking R, L, and U as zero enabled Beattie to deduce an equation relating D to Q, the total charge passed through the fluxmeter by the voltage generated in the search coil. From knowledge of Q the change in flux $\Delta\phi$ is then determined as when using a ballistic galvanometer for flux determination. Beattie constructed an instrument, tested it, and found experimental data to be confirmative of his working equation.

Thus, Beattie proposed the instrument now termed the pivot-type fluxmeter, but it is to be noted that he used it as a charge meter—hence his name of quantometer for the instrument. In 1904 Grassot[2] suggested that a suspension-type D'Arsonval galvanometer designed with minimum restoring torque and used in a circuit of low resistance could be used for the direct measurement of flux. Taking R, L, and U as zero yielded a working constant $C'=G$, to be obtained from equation 17 by setting $R=0$. As deflection is directly proportional to change in "flux," Grassot termed the instrument a fluxmeter. Grassot constructed an instrument, tested it, and found experimental data to be confirmative of his working equation.

Interestingly, modern suspension-type fluxmeters (termed Grassot fluxmeters) differ little from Grassot's original instrument. The essential advantage of Grassot's instrument, as compared with Beattie's, was the use of a suspension, rather than pivots, for supporting the moving coil. This construction eliminates the pivot friction. In Beattie's instrument the pivot friction was considerable, thus necessitating —as Beattie found—a correction proportional to deflection.

The first approach to a comprehensive theory of the unshunted fluxmeter appears to be that advanced by Darmois and Ribaud in 1924.[3] They worked out the theory of the use of the fluxmeter for measuring either time or flux and for the case of constant or exponential voltage applied in series with the fluxmeter. Although U still is taken as zero, the effect of pivot friction is included. Pivot friction introduces essentially a Coulomb friction force: that is, the pivot friction torque is a constant F, independent of the angular position of the moving coil. Pivot friction torque was neglected in formulating the theory of this paper as it is negligible in modern fluxmeters. However, it is easily introduced. Thus adding the opposing pivot friction torque F to the right-hand side of equation 5 results in an additional term FR/G in equation 6, an additional term $FR(t_2-t_1)/G$ in equations 7 and 8, and an additional term FRT/G in equations 9 and 10. If in this resulting equation 10, the integral term is taken as zero through assuming $U=0$, we obtain Darmois and Ribaud's working equation. In modern instruments, however, it is the restoring torque and not the pivot torque which results in departure from linear operation—except, of course, in a pivot-type fluxmeter where long continued use or careless handling has resulted in deterioration of the jewels or pivots.

Table VI. Maximum and Final Deflections in Run 9

L Henrys	D_m	D_f
0	22	22
32	25	22
64	26	22
96	27	22
128	26	22
192	25	21

Darmois and Ribaud effect, for certain conditions, the equation for the deflection as a function of time. In so doing they find that if $L>JR^2/4G$, the motion of the moving coil in taking up its final position is a highly damped oscillation. This phenomenon was observed during the experimental work of runs 9, 10, and 11. As indicated by the data in Table VI for the Rawson fluxmeter, for values of L other than $L=0$, the pointer swung up to a maximum deflection, then dropped back several divisions to the final steady-state value. It may be noted that as L increases from 0 to larger values, R being constant, the inequality $L>(JR^2/4G)$ strengthens. Correspondingly, the degree of oscillation increases at first; whence the greater difference between D_m and D_f with increase of L, as tabulated in Table VI. But as L is increased, the effect of the integral term in equation 8, neglected in Darmois and Ribaud's analysis, also increases. The effect of this term is such that at a certain value of L the value of D_m is a maximum and thereafter decreases as L increases—as is manifest in Table VI.

The effect of restoring torque was first considered by Surugue.[4] In 1935 he worked out a theory wherein F, L, and U are taken as zero. The resulting working constant is identical with equation 17. The effect of restoring torque is then introduced through use of an additional semiempirically-determined factor multiplying C'.

In reviewing the theory pertinent to use of the fluxmeter as a device for the integration of current or voltage over a long period of time, Dunn[5] (1939) points out that neglect of the integral term of equation 10 introduces a substantial error in the measured quantity; he names this term the "drift error" because after deflection the pointer drifts back towards zero under the influence of $U\neq0$, and finds its maximum value to be URT/G—in agreement with the value found in the more comprehensive discussion of this paper. This reference contains statement of experimental methods, different from those used herein, for obtaining the parameters of a fluxmeter.

In 1933 Golding[6,7] advanced theory for both the unshunted and the shunted fluxmeter. In the first instance R, L, F, and U are taken as zero, whence the working constant obtained is $C'=G$, to be obtained from equation 17 by setting $R=0$. Corresponding approximate treatment of the shunted fluxmeter results in a working constant $C'=G(R_{sh}+R_t)/R_{sh}=GR_tR_p^{-1}$. This differs from the accurate working constant of equation 33 in that the first term of the latter is missing—a consequence of approximations introduced in effecting the theory. Golding's theory is encompassed in his book,[12] and in a survey paper by Miller.[8]

A more accurate theory of the shunted fluxmeter was worked out by Tagg[9] in 1945, in the course of formulating the general theory of the time–deflection characteristics of moving-coil instruments. Taking F and U as zero yields working constants for the unshunted and shunted fluxmeters which are identical with those of equations 17 and 33.

To conclude, although the following are not concerned with general theory, they encompass information on fluxmeters which is of particular interest.

1. Haworth[13] has described use of a fluxmeter with feedback applied through a phototube which results in counterbalance of the restoring torque, thus eliminating the drift toward zero after attainment of deflection.

2. As evidenced by the curves of Figures 6, 7, 13, and 14, the sensitivity of a moving-coil fluxmeter decreases as circuit resistance increases. In consequence, if the search-coil resistance is high (as may be the case in mapping rapidly-varying weak magnetic fields), the deflection may be too small for accurate measurement. Dicke[14] has described an electronic fluxmeter which possesses among other advantages both zero drift and the possibility of use of search coils of resistance up to 10,000 ohms.

3. The remarks of Salisbury[15] on calibration of moving-coil fluxmeters are of general interest and utility.

Appendix II

The statement "L small in comparison to R" and the corresponding symbolism "$R>>L$" imply, of course, that the numerical value of L is small by comparison with numerical magnitude of L/R. The effect of increasing the ratio L/R of the two magnitudes is to be analyzed as follows. If L is essentially zero, the circuit is purely resistive; at $t=t_2$ the net generated voltage in the search coil and fluxmeter coil is zero; inasmuch as no generated voltage due to self inductance exists ($L\cong0$), the iR drop also must be zero (the current is "in phase" with the net generated voltage); whence at the instant $t=t_2=T$ of maximum displacement, $\theta=\theta_m=\theta_2$, the current $i=i_2=0$, and thus $Li_2=0$.

If L is a value other than essentially zero, but is yet of such magnitude that the fluxmeter does not overshoot the "steady-state deflection" (defined below), it still is true that, at $t_2=T$, the net generated voltage in the circuit is zero, but it does not now follow that $i_2=0$. For inasmuch as the circuit is not purely resistive, an electromotive force generated by change of current in the self-inductance of the circuit can support an equal i_2R drop at $t=t_2$. All that now can be said is that at $t=t_2$ the combined mechanical and electrical torque is zero. However, it is obviously true that, if L does not differ too much from zero (whence for arbitrary but fixed R, the ratio L/R is still small), it is to be expected that circuit conditions at $t=t_2$ are approximated by those holding for $L=0$, whence i_2 and thus Li_2 are substantially zero.

However, in a well-adjusted fluxmeter, the drift is extremely slow, whence the reading is taken at steady-state deflection. Accordingly, it must be that in such a fluxmeter $i_2=0$. (Otherwise an electromagnetic torque exists and steady-state deflection is not the case) Accordingly, if $t_2=T$ is taken, more generally, as the time of attainment of steady-state deflection, $i_2=0$, and the analysis is yet as given, except now θ_2 is the position of steady-state deflection. This position may or may not be that of maximum deflection for if L is sufficiently large, the fluxmeter will overshoot as evidenced in Table VI and discussed in Appendix I. If it is, $|\theta_2|=|\theta_m|$; if not, $|\theta_2|<|\theta_m|$.

From the practical point of view, the experimental curves of Figure 5 evidence that the self-inductance of the circuit must be considerable before steady-state deflection is influenced; whence it could be deduced directly from experiment that analysis based on neglect in equation 9 not only of Li_2, but of the influence of L on the value of

$$(UR/G)\int_0^T\theta/dt$$

as well, is valid up to very large values of L.

References

1. An Electric Quantometer, R. Beattie. *The Electrician* (London, England), volume 50, 1902, pages 383–5.

2. Fluxmètre, M. E. Grassot. *Journal de Physique Théorique et Appliques* (Paris, France), series 4, volume 3, 1904, pages 696–700.

3. Etude Théorique et Expérimentale du fluxmètre, G. Darmois, G. Ribaud. *Annales de Physique* (Paris, France), series 10, volume 1, 1924, pages 172–212.

4. Sur la Théorie du Fluxmètre Grassot, J. Surugue. *Journal de Physique et le Radium*, series 7, volume 6, 1935, pages 486–8.

5. The Grassot Fluxmeter as a Quantity Meter, H. K. Dunn. *Review of Scientific Instruments* (New York, N. Y.), volume 10, 1939, pages 368–70.

6. The Grassot Fluxmeter, E. W. Golding. *The Electrician* (London, England), volume 111, 1933, pages 503–5.

7. The Use of the Grassot Fluxmeter as a Quantity Meter, E. W. Golding. *Journal*, Institution of Electrical Engineers (London, England), volume 76, 1935, pages 113–17.

8. Fluxmeter Method of Measurement, H. A. Miller. *The Electrician* (London, England), volume 136, 1946, pages 1577–9.

9. Time–Deflection Characteristics of Moving Coil Instruments, G. F. Tagg. *Journal*, Institution of Electrical Engineers (London, England), volume 92, part II, 1945, pages 214–25.

10. Electrical Measuring Instruments (book), C. V. Drysdale, A. C. Jolley. E. Benn, Ltd., London, England, 1924.

11. Elements of Electrical Engineering (book), W. J. Creamer. McGraw-Hill Book Company, New York, N. Y., 1948, page 273.

12. Electrical Measurements and Measuring Instruments (book), E. W. Golding. Sir Isaac Pitman and Sons, Ltd., London, England, edition 3, 1940, page 348.

13. A Fluxmeter with Counter-Balanced Restoring Torque, F. E. Haworth. *Review of Scientific Instruments* (New York, N. Y.), volume 2, 1931, pages 125–8.

14. Electronic Fluxmeter, R. H. Dicke. *Review of Scientific Instruments* (New York, N. Y.), volume 19, 1948, pages 533–4.

15. Note on Calibration of Grassot Fluxmeters, R. S. J. Salisbury. *Journal of Scientific Instruments* (London, England), volume 22, 1945, pages 213–15.

Discussion

Frank Wenner (Consulting Physicist, Washington, D. C.): Obviously the authors have arrived at a very much better understanding of the actions of a fluxmeter (that is, a heavily overdamped galvanometer when used ballistically) than that apparently possessed by most of those who have written on the subject. There are, however, a number of points which might be explained in such a way as to ease the requirements on the part of the reader. One only, and scarcely the most important of these, is selected for discussion here. It is the transition from equation 15 to equation 31.

Keeping in mind equation 15 and applying the principle of the potential divider, it follows from an inspection of Figure 2 (neglecting L_t, L_f) that for the same electromotive force impulse into the fluxmeter the flux-linkage change must be increased by the factor $(R_t+R_{sh})/R_{sh}$. Also on looking at the circuit from the standpoint of the fluxmeter it follows from an inspection of Figure 2 that the resistance $R=R_f+R_{sh}R_t/R_{sh}+R_t$. Consequently when the fluxmeter is shunted, equation 15 takes the form

$$\lambda_2-\lambda_1=[(R_t+R_{sh}/R_{sh})]\{[R_f+R_{sh}R_t/(R_{sh}+R_t)]K/G+G\}(\theta_2-\theta_1)$$

This is equivalent to equation 31 provided the typographical error in the equation defining R_p is taken into account. Thus by using relations well-known to most of those skilled in electrical measurements instead of one of Kirchhoff's laws, not only is a considerable amount of algebra avoided but a clearer picture is presented to the reader.

Morley J. Lush (Rawson Electrical Instrument Company, Cambridge, Mass.): The authors are to be congratulated for finally bringing together, in one equation and discussion, all the error-producing factors which can influence the results when using regular commercial models of the fluxmeter.

Recent experiments by this writer have shown that values of both K and U can be reduced considerably without making the instrument as delicate as the old Grassot type.

It should be noted that the results in the paper were obtained with a circuit giving practically instantaneous changes of flux linkage. If hand-operated search-coils are used, with time intervals up to one-half second, it is not possible to use search-coil resistances as high as those mentioned. Under these conditions, the integral term in equation 10 due to spiral torque is sufficient to cause an error which is variable unless the time interval can be standardized in some way. The Rawson Company has always recommended a search-coil resistance under one ohm whenever possible, practically eliminating all errors, or a maximum value of 10 ohms if corrections can be made.

It is possible to invert equation 15 using the first two terms of a series expansion, and put it into the form

$$S=S_o\left[1-\frac{S_oKR_{sc}}{G}\right]$$

where S is the general sensitivity, and S_o is the scale calibration sensitivity ($R_{sc}=O$). Both S and S_o are in divisions per flux linkage. The equation is within one-half of 1 per cent if the second term is less than 5 per cent. To one further degree of approximation

$$S=S_o\left[1-\frac{S_0^2KR_{sc}}{10^9}\right]$$

This form of the equation is interesting because it shows that the variation in calibration constant with search-coil resistance depends upon air damping constant K and the square of the sensitivity. If a deflection sensitivity higher than that of the old Grassot instrument is attempted, the calibration constant becomes very dependent upon the search-coil resistance (unless the air damping can be reduced in some way). This explains why some makers have adopted the practice of requiring all search-coils to be adjusted to a particular value of resistance.

Thomas James Higgins and Glenn Robertson: Mr. Lush's equation neatly exhibits the dependence of sensitivity on search coil resistance and very well complements the author's discussion thereof.

With respect to Dr. Wenner's remark that "there are a number of points which might have been explained in such a way as to ease the requirements on the part of the reader" the authors would point out that Dr. Wenner's purported derivation of their equation 31 is invalidated by his remark "neglecting L_t, L_f".

If the authors had chosen to neglect terms *a priori* they could have shortened the over-all analysis of the paper considerably. Rather, it was their stated intention, to carefully and completely consider the influence of all pertinent factors and to establish definitely under what conditions certain approximations could be introduced, that necessitated the approach through Kirchhoff's laws. Thus, it is clearly established in the paper under what conditions terms involving inductances drop out, thus resulting in equation 31. On the other hand, Dr. Wenner merely "neglects L_t, L_f" whence (on the basis of Dr. Wenner's derivation) no specific condition results as to when his resulting equation is valid.

Further, it was not the intention of the authors that their paper would be intelligible merely "to most of those skilled in electrical measurements" but rather to so write that the paper as a whole would be clear to any reader with an ordinary undergraduate training in circuit analysis and electrical measurements. For it is the engineer with only an average training in electrical measurements who commonly falls into error in using a fluxmeter, not the highly-skilled worker in electrical measurements.

A 66-Kv Subtransmission Plan for a Metropolitan Area

K. M. SMITH
MEMBER AIEE

E. L. MICHELSON
MEMBER AIEE

THE planning of the subtransmission system to deliver power from the power supply station to the substation represents a fertile field for the activities of the system planning engineer. Many different possibilities present themselves in any particular system, and there are numerous factors that require consideration before the most desirable plan can be determined. The basic conditions change from time to time, so that it is necessary to review the problem periodically to take into account new developments that affect the economics of the subtransmission system.

In Chicago a new arrangement was adopted recently to provide for the expansion in load which is being experienced. This arrangement utilizes as a subtransmission voltage 66 kv, formerly used only at transmission levels. In this paper the basic reasons for the adoption of the new system are described, and some of the details of the system are discussed from the viewpoint of the system planning engineer. In order to provide a background for this discussion, the history of the Chicago system is reviewed briefly.

History of the Chicago Power System

In the early development of the power system in the city of Chicago, all the generators were located inside the city and relatively close to the load. The subtransmission for the 60-cycle load consisted of lines at the generated voltage (12 kv) direct from the generating station to the distribution substations, and the various generating stations were sometimes interconnected by these same 12-kv lines. The complexity of this arrangement caused difficulties in operation and protection, and led to the separation of the 12-kv system into various subtransmission zones, supplied from major 12-kv switchhouses originally located exclusively at the sites of generation. These zones were then interconnected by a 66-kv transmission system.

Figure 1 shows in detail the connections of one of these generating station switchhouses and a typical arrangement of 12-kv lines and substations. Several substations are combined into a group, which operates independently of other groups. In this manner a highly reliable system has been developed, which is relatively simple to operate and protect.

A further development in the subtransmission arrangement occurred in the late 1920's and early 1930's, when two large distributing stations were constructed along the route of the 66-kv interconnections, midway between generating stations. These distributing stations were supplied by 66/12-kv transformers from the 66-kv transmission system, and served as a source of supply for the subtransmission lines in their vicinities. The length of 12-kv lines required for subtransmission was thereby reduced. The 12-kv switching structures at these stations are similar in size and construction to the switchhouses at generating stations.

The system as it existed previous to the current development under discussion is shown in simplified form in Figure 2. At this time there was a total of 275 12-kv lines supplied from seven switchhouses located on six sites. These lines are usually 3-conductor 500,000-circular-mil underground cable.

With a closely interconnected high capacity system, it is obvious that potentially high fault currents are available. The switchhouses built since 1921 have therefore been of isolated-phase construction. This design is carried through the 66-kv transmission system by the use of single phase transformers and single conductor cable, so that the possibility of a phase-to-phase fault on these elements of the system was practically eliminated. Neutral grounding resistors are used on the 66-kv system and in the 12-kv generating stations to limit the phase-to-ground fault currents, and reactors are installed on the 12-kv lines at the switchhouses to reduce the effect of phase-to-phase faults on the 3-conductor subtransmission cables. As a result of this design, it has been possible to operate the phase-isolated 12-kv switchhouses and 66-kv terminals with circuit breakers rated below the calculated 3-phase fault current. Normal or automatic sectionalization of busses has effected a further reduction of fault current.

Recent Developments

While the design of switchhouse construction and the subtransmission arrangement described have been quite successful and reliable, certain recent developments, as outlined in the following, have stimulated consideration of an alternative plan:

1. In recent years it has been necessary to develop generating sites outside the city. Power from these sites is transmitted into Chicago at high voltage, and is made available to the 66-kv interconnecting system in the city. This situation has a considerable effect on the economics of the subtransmission system, because it makes more favorable the use of 66 kv as a subtransmission voltage to deliver the power closer to the point of utilization. Present indications are that the trend towards power being made available in the city at a higher voltage will continue.

Paper 49-172, recommended by the AIEE System Engineering and Transmission and Distribution Committees and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20–24, 1949. Manuscript submitted March 2, 1949; made available for printing April 29, 1949.

K. M. SMITH is System Planning Engineer and E. L. MICHELSON is Section Engineer, both with the Commonwealth Edison Company, Chicago, Ill.

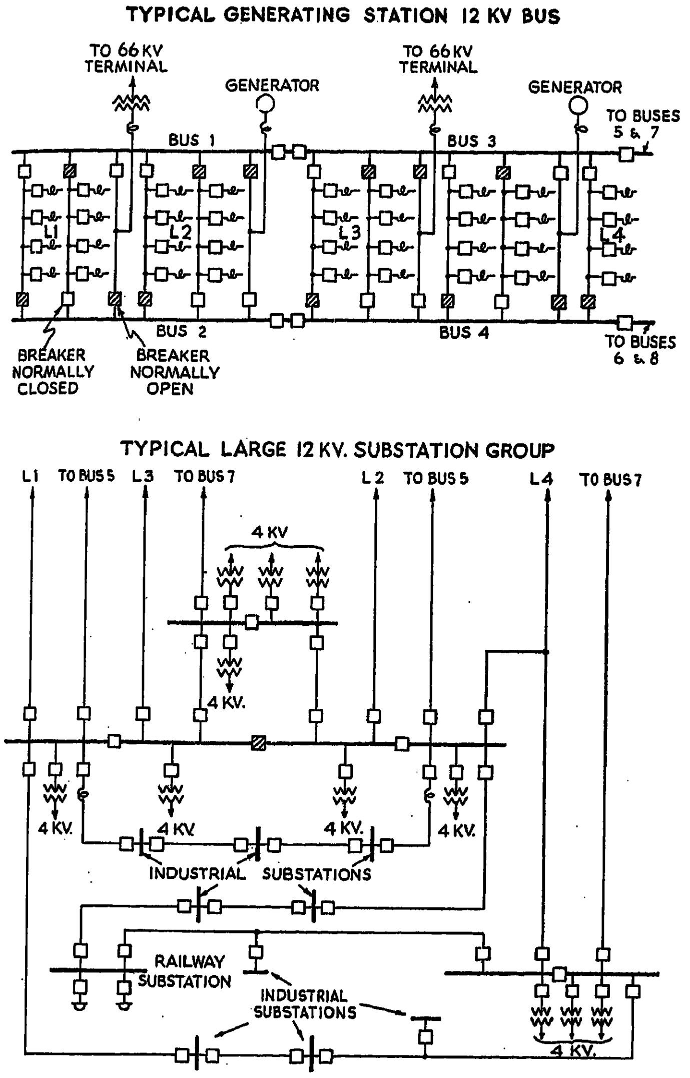


Figure 1. Generating station 12-kv bus and 12-kv substation connections

2. The present 12-kv lines average about four miles in length, and the longest lines extend eight to ten miles from the switchhouse. With rapidly increasing load density, particularly in areas remote from the present switchhouse sites, continued subtransmission from these sites at 12 kv involves considerable costs, and the need for a higher voltage is indicated.

3. In recent years there has been an extensive development of factory assembled switchgear, which has many attractive features. Switchgear of this type, in a price class which makes it suitable for substation use at 12 kv, has been made available in interrupting capacities as high as 500,000 kva. A plan which would permit the integration of this type equipment into the system appeared desirable.

In the light of these developments, and as the need for 12-kv switch positions for subtransmission purposes has increased, various studies have been made of the economies of extending the 66-kv system for subtransmission purposes. Instead of constructing additional large switchhouses, or extending present ones, consideration was given to the use of smaller 66/12-kv substations of simplified construction which would be supplied from 66-kv lines and be superimposed on the existing pattern of 12-kv subtransmission. Investigation disclosed the following advantages for the use of the smaller type of 66/12-kv installations:

1. It has been the experience that the large switchhouses designed for 40 to 60 line positions have a costly development period due to the necessity for constructing the station in large increments. As a result, it may take many years to obtain full utilization of the capacity of the switchhouse. The use of smaller substations permits a closer match between system requirements and actual construction.

2. The use of 66-kv reduces the amount and cost of cable required for subtransmission purposes. The small 66/12-kv substation permits extending the 66-kv lines close to the center of the load, with the result that the lines between the 66-kv substation and other substations requiring 12 kv in the vicinity are relatively short. In addition, if the 66/12-kv substation can be located adjacent to an existing major 12/4 kv-substation, 12-kv lines for the latter are eliminated completely.

3. With the larger switchhouses, involving as many as 60 12-kv cables and the various 66 kv, and possibly 4 kv, cables required for the station, the problem of cable congestion in the streets adjoining the station can become a serious problem. In some cases the number of possible conduit runs is rather limited, and the cable congestion may lead to excessive heating with resultant reduction of cable capacities.

4. Smaller installations inherently will have a lower value of short-circuit current. While the calculated value of fault current at the larger 12-kv switchhouses ranges from 1,000,000 to 2,000,000 kva, it was found that an economical size of 66/12-kv substation could be selected which would easily restrict fault current values to 500,000 kva. This makes possible the use of low interrupting capacity switchgear. It also makes possible the elimination to a large extent of 12-kv line reactors which are required on lines emanating from the larger switchhouses.

5. Substations are more adaptable to supervisory control than large switchhouses due to the simplicity of the layout and the smaller amount of load being supplied from the station. As a result, the operating costs for a 66/12-kv substation of relatively small size can be kept to a minimum.

6. The smaller 66/12-kv substations provide improved reliability for the 12-kv system by reducing the number of 12-kv lines to one location. To the extent that a switchhouse is subject to complete outage from fire, explosion, and other similar occurrences, the reduction in the number of 12-kv lines originating at one location should reduce the severity of any one case of trouble.

New 66/12-Kv Substations

The growth of load in Chicago during the recent war and postwar period resulted in prospective full utilization of some 12-kv switchhouses. The general advantages of 66 kv for subtransmission purposes using small 66-kv substations, appeared to make the use of this voltage very attractive. Also, general economic studies, which are discussed in a later section of this paper, showed considerable savings for the 66-kv system. After a review of conditions in each specific case, it was decided to provide expansion of the subtransmission system by the construction of 66/12-kv substations, rather than by enlarging the existing switchhouses or constructing additional large switchhouses.

The installation of several small 66/12-kv substations is planned, and one of these stations was placed in service in the latter part of 1948. Figure 3 shows the existing subtransmission system with the new 66-kv lines and substations superimposed. In each case, the new 66-kv substation supplements the existing system by supplying load on the fringes of the present zones. In this manner, the great-

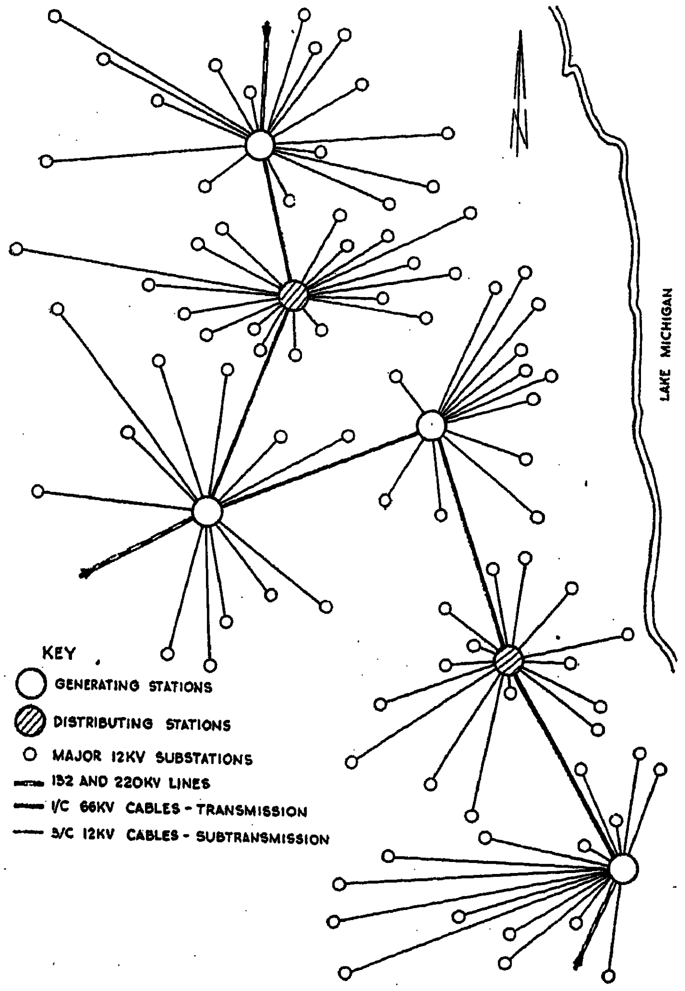
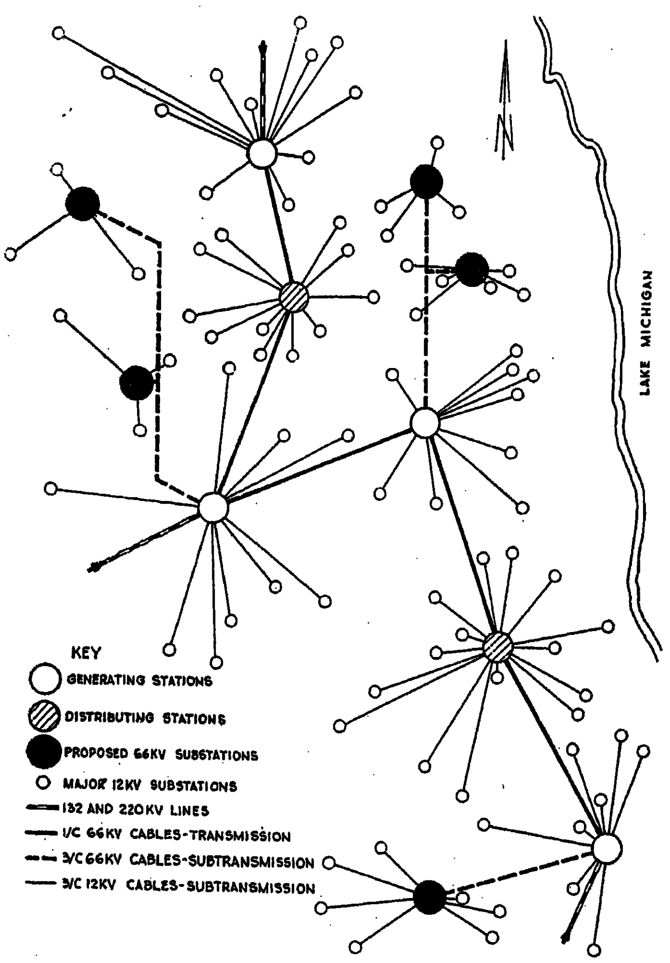


Figure 2. Generating and distributing station connections in Chicago with 12-kv supply to major substations



N
LAKE MICHIGAN
KEY
GENERATING STATIONS
DISTRIBUTING STATIONS
PROPOSED 66KV SUBSTATIONS
MAJOR 12KV SUBSTATIONS
132 AND 220KV LINES
1/C 66KV CABLES-TRANSMISSION
3/C 66KV CABLES-SUBTRANSMISSION
3/C 12KV CABLES-SUBTRANSMISSION

Figure 3. Generating and distributing station connections in Chicago with 12-kv supply to major substations and 66-kv supply to the proposed 66-kv substations

est reduction in the length of 12-kv lines is effected, and the maximum economy is obtained.

The following is a description of the elements of these subtransmission systems which concern the planning engineer. The general layout for one of these systems is shown in Figure 4.

Basic Capacity Elements

From a planning point of view, an important problem was the co-ordination of capacity of the various components of the system. As the 66-kv substation lines will operate as a radial system not interconnecting 66-kv transmission terminals and without 66-kv bussing at the substations, it is possible to co-ordinate line and transformer capacity on an individual line basis. The maximum size of 3-conductor 66-kv cable which may be installed in the existing duct system is 650,000 circular mils, which will have a normal summer rating of 49,000 kva, and an emergency rating of 66,000 kva. To match this line capacity, a transformer size was selected which permits the connection of two transformers per line, that is, a 3-phase unit with a 25,000 kva forced-air rating and an emergency rating of 33,000 kva. Thus, a 3-line subtransmission group will supply two substations, each with three transformers, and each having a firm capacity of 66,000 kva so that the total load will match the emergency line and transformer capacity with one line out of service. The cable between the two substations is, of course, smaller as it must serve only one transformer. Cable of 250,000 circular mils was selected for this purpose.

66-Kv Lines

All 66-kv cable is 3-conductor oil filled. At or near the first substation at the termination of the 650,000-circular-mil cable, a 3-position disconnect device is installed from which will be taken the tap to a transformer in the first substation and the 250,000-circular-mil cable supplying a transformer in the second substation. This device will permit disconnection of either outgoing tap and restoration of service to the other.

At the supply point, each of the 66-kv subtransmission lines is connected to a different bus section for maximum reliability. The use of 3-conductor cable makes it necessary to provide line reactors at this point. The reactors have a value of 3.5 ohms, which will reduce the value of fault current imposed upon the 66-kv phase isolated terminal, by a multiphase fault on the 3-conductor cable, to less than 1,000,000 kva.

Transformers

The 3-phase 66/12-kv transformers will have an impedance value of 11 per cent, based on the self-cooled 20,000-kva rating. This is the maximum impedance value available without special design. Here again, the factor of co-ordination appears, as with three transformers of the size and impedance selected, the 3-phase fault current on the 12-kv side is somewhat less than 500,000 kva, the value corresponding to the interrupting capacity of an economical type of standard switchgear.

The transformers are equipped with automatic tap changers under load on the 12-kv side with a range of ±10 per cent. The 66-kv busses from which these substations are supplied cannot be regulated solely to satisfy the requirements of the 66-kv substations, and as a considerable portion of the load on these substations is unregulated elsewhere, adequate range of regulation must be provided at this point. Two $2^1/_2$ per cent fixed taps are provided on the 66-kv windings above and below the nominal winding ratio of 66 to 12.6 kv.

12-Kv Connections

As shown in Figure 4, the 12-kv bus consists of four sections connected into a ring. The use of four bus sections with only three transformers was chosen on the basis that the units of load supplied from the 12-kv bus would require up to four 12-kv lines. Because the 12-kv connections are planned on the basis of carrying full load with one line out of service, it is desirable to have four separate bus sections supplying a group of four lines.

The 12-kv bus arrangement is designed for the maximum electrical separation between busses and transformers with the minimum use of 12-kv circuit breakers. With the arrangement shown in Figure 4, any one failure in the station will cause a maximum loss of one transformer.

The substation includes 16 12-kv line positions. Based on a maximum 12-kv load of 66,000 kva, and without allowance for spare switches, the average coincident load per line will be lower than the actual loadings achieved on existing lines from generating and distributing stations where 12-kv lines are operated in groups of eight, with a reserve ratio of approximately 1 to 8. With the smaller 66-kv substations, the number of 12-kv lines grouped together will be smaller, with consequently higher line reserve ratio. However, with the smaller substations located closer to the load, the length of each 12-kv line will be relatively short, and as the increment costs of the line circuit breakers in these substations is relatively low, the lesser line loading does not adversely influence the economics of the 66-kv subtransmission system to a serious extent.

Since the maximum possible fault current is below the capacity of the 500,000-kva circuit breakers, reactors are not required on outgoing lines to protect the substation equipment. However, line reactors will be required in a few cases to protect lower capacity circuit breakers at customer substations supplied at 12 kv from 66-kv substations.

System Grounding

The substation transformers will be delta connected on the 66-kv side, and star connected on the 12-kv side. The 12-kv winding neutral is grounded through a 3-ohm resistor, principally for the purpose of limiting the value of phase-to-ground fault current which will be imposed on the lead sheaths of the 12-kv underground cables. Resistance is used in the neutral rather than reactance

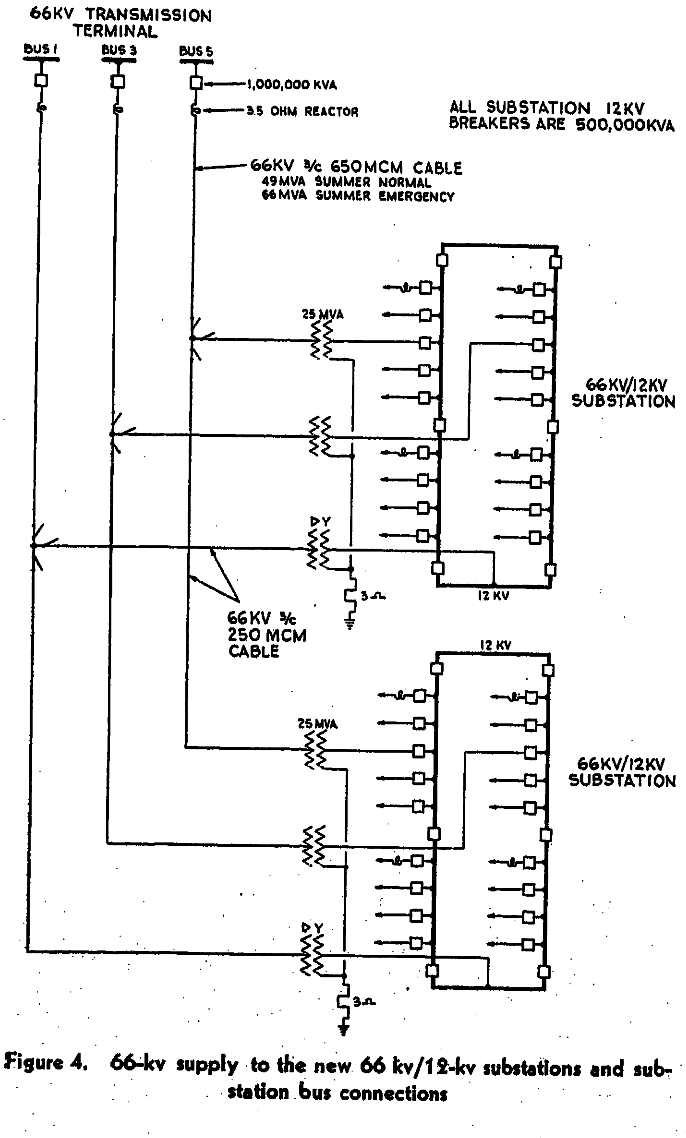


Figure 4. 66-kv supply to the new 66 kv/12-kv substations and substation bus connections

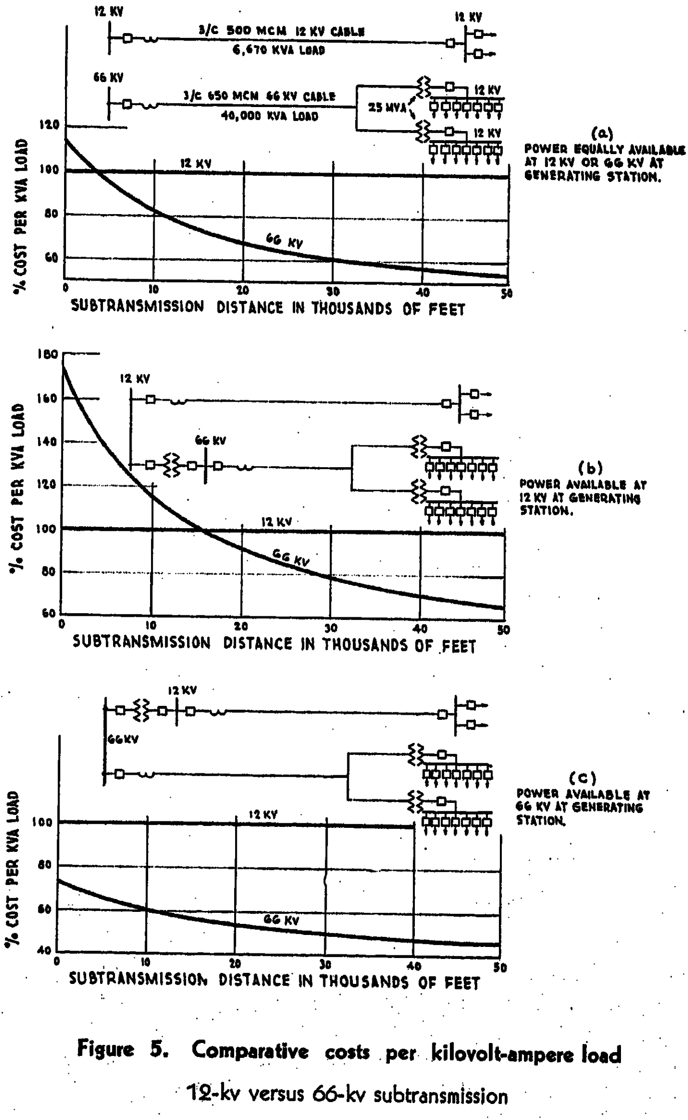


Figure 5. Comparative costs per kilovolt-ampere load 12-kv versus 66-kv subtransmission

to limit transient overvoltages in the 12-kv system.

Relay Protection

The relay protection provided for the new subtransmission system is conventional in most respects. Over-current relaying is provided on the 66-kv and 12-kv lines, and differential protection is provided for the transformers and busses. Because the substation transformers are connected to the lines without 66-kv circuit breakers, the transformer differential relays actuate a remote trip circuit to clear the remote ends of the lines. This trip circuit is incorporated at small additional cost in the pilot wire cable which is required for the oil-pressure alarm system for practically the entire length of the 66-kv cable.

Other Possible Arrangements

It is possible, with satisfactory co-ordination, to use the same units of capacity to develop 4-transformer substations, two of which would be supplied by a group of four 66-kv lines. With this arrangement a substation would be operated in two parts, two transformers normally connected to each part, in order to keep interrupting duties below 500,000 kva. This plan is available for use where justified by higher load density. The basic system is, of course, inherently adaptable to various groupings of lines and transformers, providing careful attention is given to interrupting duties and to co-ordination of capacity.

Economic Comparisons of 66-Kv and 12-Kv Systems

In order to show the comparative costs of 66 kv and 12 kv for subtransmission purposes, representative system patterns may be analyzed in terms of cost per kilovolt-ampere of load.

For the 12-kv system, the arrangement shown in Figure 1 represents a typical case. Lines from a switchhouse are grouped to serve a number of load units centering about one large distribution substation. At that substation power is distributed directly to its 4-kv transformers and by 12-kv tie lines to various other distribution and industrial substations in the vicinity.

For the 66-kv system, the arrangement shown in Figure 4 provides a basis for comparison. The 66-kv lines and transformers, in effect, replace the 12-kv lines between generating station and the load center of the group substation.

Figure 5 shows the results of the economic comparison. The 12-kv system costs include the switch position in the generating station, the 12-kv cable, and a total of three circuit breakers at the substation. The latter allocation is based on actual experience and takes into account all the circuit breakers, at each large substation, including bus ties, transformer circuit breakers, and tie lines to other substations. Since the 66-kv substation supplants all of these circuit breakers, a credit is taken for them in the cost comparison. The average maximum load per line of 6,670 kva is based on actual experience with the Chicago system. The comparable 66-kv system consists of one 66-kv line position, the 650-000- and 250,000-circular-mil 3-conductor cable, and the transformers and 12-kv circuit breakers supplied by this line. For convenience the 66-kv line is evaluated in terms of equivalent length of the larger size. The expected maximum line loading is 40,000 kva.

Figure 5 shows the relative costs of the two systems using the cost of the 12-kv system as a base. Three cases are shown; the first applies to a condition where power is available either at 12 kv or 66 kv, as would be the case at a location where there is power available either at the generated voltage or from the transmission bus in the same station. For this case, the 12-kv system has a lower cost for a distance of approximately 3,000 feet, but beyond this distance, the 66-kv system is more economical and approaches a relative cost of 50 per cent for longer distances.

If power is available only at 12 kv, as at a generating station where there is no power transmitted to the station from the high-voltage system, the 66-kv plan is less favorable, as shown by curve (b); although, even here, 66 kv would be more economical for distances greater than 15,000 feet. In the third case, where power is available only at the transmission voltage, the use of 66 kv for subtransmission is most advantageous.

These comparisons reflect the lower cost of 66-kv cable, as compared to 12-kv cable, the 66-kv cost per kilovolt-ampere being less than one-half the 12-kv cost. Another factor favoring the 66-kv plan is the comparatively high cost per switch position in the isolated phase 12-kv switchhouses. This tends to make the 66-kv system economical even for the shorter distances of subtransmission.

While these comparative studies provide a general basis for appraising the economy of the higher voltage system, there are various other practical considerations that affect the final answer. For instance, the general comparison is based on the installed cost of the 12-kv or 66-kv lines, while in actual practice any existing 12-kv lines released by the introduction of the 66-kv system will yield only salvage value. Fortunately, however, portions of such 12-kv cables can, within a short period of time, be used in place to provide for load growth in the area closer to the switchhouse. Thus, to a great extent, the actual salvage value approaches their cost in place.

The general comparison also is based on average 12-kv subtransmission line loadings. In areas remote from the switchhouse, this may not always be achieved due to the necessity for a somewhat lower loading to meet the requirements of voltage regulation. In such cases the 66-kv system has an economic advantage.

Another practical factor is the interrelationship between 66-kv subtransmission and transmission. Where the 66-kv subtransmission parallels the transmission system in the direction of power flow upon the latter, it may serve load taken from several 12-kv zones, thus reducing the load on the transmission system, and thereby providing, in effect, a reinforcement of the transmission system.

Studies of specific cases of the possible use of 66-kv for subtransmission have shown substantial savings, and have led to the use of this higher voltage where reinforcement of the subtransmission system was required. This paper has aimed at a brief description of the planning which led to the use of this system. Higher load densities, increased transmission of power into the city from outside stations, comparative costs of construction for extending existing facilities, and the availability of economical factory-assembled switchgear are primary factors which have been considered.

The gain in economy and reliability resulting from the use of the system described is evidence that time which is devoted to the planning of the subtransmission portion of a system may be well justified.

Discussion

H. R. Wall (The Commonwealth and Southern Corporation, Jackson, Mich.): The authors' interesting description of the use of higher voltage for subtransmission extensions in their territory presents a convincing demonstration of the benefits which can be obtained in this manner. Their use of radial 69-kv subtransmission supply to step-down substations, without 69-kv automatic switching at those substations, presents an economically attractive subtransmission plan.

It is noticed that the paper mentions the new extensions as "supplying load on the fringes of the present zones." It is assumed that even "fringe" territory in this location is so congested that it is not feasible to use 69-kv wood pole lines. In less congested territory where overhead lines can be used the use of higher subtransmission voltage, such as 69 kv, offers material economic advantages, since the voltage drop in the lower voltage (overhead) subtransmission circuits also is a serious consideration, as well as the thermal capacity limitations found in cable circuits. Higher voltage subtransmission might be indicated to avoid additional crowding of city streets by a greater number of low-voltage subtransmission lines.

The necessity for restricting maximum fault current on these subtransmission systems, as pointed out by the authors, with the intentional introduction of additional impedances in order to achieve that purpose, leads to a question regarding the possible use of switched capacitors for voltage control instead of, or supplementing, the tap changing underload apparatus, in order to reduce the additional losses associated with the intentionally added impedances.

The authors have mentioned the simplified relaying applied in their plan. No mention is made of the means of clearing 69-kv faults by the 12-kv circuit breakers of the step-down transformers. Since the low-voltage busses of the step-down transformers are operated in parallel, it is assumed that directional overcurrent relays are used to open the proper 12-kv circuit breakers for phase-to-phase faults. It would be interesting to know whether any provision is made to clear line-to-ground faults on the 69-kv circuits by opening the 12-kv transformer circuit breakers.

D. K. Blake (General Electric Company, Schenectady, N. Y.): It was a pleasure to read this paper for three reasons. First it is well written. I want to commend the authors for their brief and clear presentation of the pertinent factors. Second, it is encouraging and refreshing to see this departure from complications towards simplification. Third, it is a confirmation and illustration of the same economic principles that determine substation sizes whether they are for transformation from 69 kv to 12 kv or from 12 kv to 4 kv.

Some perhaps will be startled, after reading the authors' "the small 66/12-kv substation," to find they are 66,000 kva firm rating. In many other systems this would be considered "large" and would be large for those areas.

Whether a substation is large or small is not defined on economic grounds by its kilovolt-ampere rating. It is defined more properly and accurately by the area in square miles, or if one prefers, by the number and average length of the feeders departing from it. As the area decreases in size, with increasing load density, the feeder lengths to cover the area eventually become so short that a point of diminishing return is reached. There is not much left to save in 12-kv lines to offset the added substation and 66-kv extension cost inherent in diminishing substation size. Increases in load density from thereon are met by increasing the number of feeders until line or duct congestion impose their limits.

The concept of separating the load supplying function from the transmitting function in system design is a very sound procedure in my opinion and should be more widely adopted. The load supplying function is what distinguishes "distribution" from "transmission." Do the authors propose to eliminate 12-kv transformation and step from 66 to 4 kv in areas where applicable? All that is needed is enough load to economically load the 66-kv circuits and after that it becomes possible to save money by eliminating a transformation.

V. E. Hill (Duquesne Light Company, Pittsburgh, Pa.): This is a very interesting paper that has been well organized and which was easy to follow. The authors describe a principle that has become quite necessary in recent years, that is, the use of much higher transmission voltages for the distribution of power to large load centers. The plan adopted certainly has the merits of simplicity and economy. However, while going through the paper, a few questions arose.

In the reference to the use of higher voltages (that is above 66 kv) to bring power into the Chicago area from the more distant generating systems, has any consideration been given to stepping these higher voltages directly down to 12 kv? Likewise, was there any consideration given to the establishment of direct 66/4-kv substations? The latter statement assumes the installation of high capacity distribution substations in one locality. There seems to be a tendency to get away from this type of station with the use of the unit substation.

Use of 12 kv as a subtransmission voltage was, of course, quite common during the early days of the industry, because loads were light and because this was the generator voltage and it avoided the step-up transformation. However, with modern, heavily concentrated loads, and the duct congestion which is frequently encountered near generating stations or bulk supply stations, such as are described here, it is wondered whether any consideration has been given to the use of higher voltage cables, such as 22 kv in place of 12 kv.

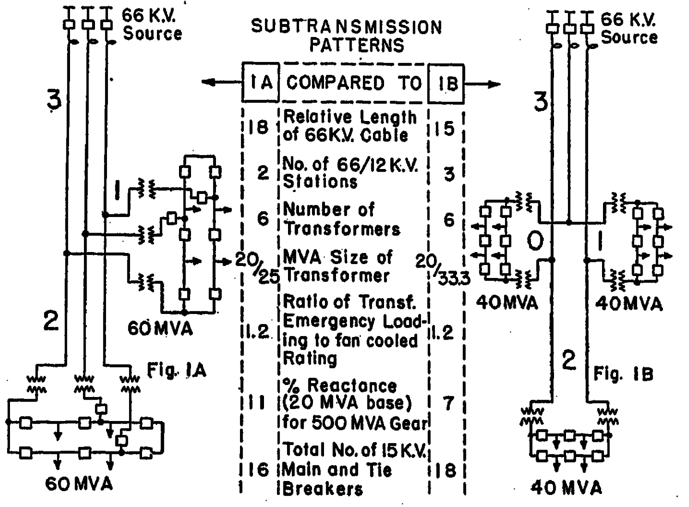
We were very much interested in the fact that these new 66/12-kv transformers are being acquired with automatic tap changing under load. We have been checking the need of this additional control device on our 66/22-kv bulk supply stations for some years, but have not yet quite justified the cost except at one station where the voltage regulation is rather extreme. In this connection we are wondering what the regulation would be on the 12-kv busses without the tap changing under load. Are you maintaining a flat 12-kv voltage or are you adjusting it automatically by means of load compensators?

In reference to the use of four 12-kv bus sections, it was mentioned that this number was chosen because the size of the (load) units required four 12-kv lines and provision is made for one line out of service. How large are the "units" that are referred to here? That raises the question of whether there are any customers so large that they are supplied directly from the 66-kv system. The latter voltage also may be dictated by flicker limitation.

The economic comparisons in Figure 5 of the paper are very interesting and demonstrate that it is very essential to consider at what voltage the power is available.

C. A. Woodrow (General Electric Company, Schenectady, N. Y.): Any subtransmission investigation can easily become extremely complicated. Subtransmission generally includes a simple transmission function; that is, the point-to-point transfer of bulk power. But, it must go further! It must reach down and adequately feed the underlying distribution system. This latter function involves an area concept and all the ramifications of varying load density. Therefore, the authors of this paper are to be particularly complimented on their simple straightforward presentation. They clearly make two particular points which seem widely significant:

1. As in Chicago, the trend is towards power availability at high voltage rather than generator voltage. Even in the large metropolitan systems, as that power is fed down into the load areas, it logically and economically clears through relatively small subtransmission stations.
2. The cost and space requirements of the modern metalclad, 13.8-kv oil-less switch-gear (indoor and outdoor) are so



SUBTRANSMISSION PATTERNS

IA	COMPARED TO	IB
18	Relative Length of 66K.V. Cable	15
2	No. of 66/12 K.V. Stations	3
6	Number of Transformers	6
20/25	MVA Size of Transformer	20/33.3
1.2	Ratio of Transf. Emergency Loading to fan cooled Rating	1.2
11	% Reactance (20 MVA base) for 500 MVA Gear	7
16	Total No. of 15 K.V. Main and Tie Breakers	18

Figure 1. Alternative subtransmission patterns.

attractive that they dictate subtransmission patterns which limit the 12- and 13.8-kv short circuits to 500,000 kva or less.

Though the authors have made an important contribution, a note of caution seems justified. The firm capacity of 60 to 66 megavolt-amperes may be small for 66/12-kv stations in Chicago but it is large for a great many urban areas. Quite often, considerable expense in 12- or 13-kv circuits can be saved if much smaller stations can economically and judiciously be provided within the load area. Figure 1(B) of the discussion illustrates a possible step in that direction. It provides three 40-megavolt-ampere stations as an alternate to the Chicago plan, Figure 1(A) with two 60-megavolt-ampere stations. A 2-source rather than a 3-source station is used for two reasons:

1. To reduce the number of transformers and 66-kv cable T taps.

2. It is much less costly to run two, rather than three, 66-kv laterals out into the load area to supply 40 megavolt-amperes.

As is indicated by the comparison chart of Figure 1, the principal difference in substation apparatus cost is the use of high velocity fans (or equivalent), Figure 1(B), rather than low velocity fans, Figure 1(A), on the 20-mva 3-phase transformers. However, as is illustrated by the relative cable length shown, this extra cost can often be largely eliminated. If load ratio control transformers are used, the two patterns appear to break even (real estate and underlying 12-kv cable *not* included) when the 66-kv 3-conductor cable saving of Figure 1(B) is equivalent to about two miles of cable. Without load ratio control, they break even with about one mile of cable saving.

Where the additional substation site can be made available, subtransmission pattern Figure 1(B) often warrants a serious consideration. In many cases the same pattern can be economically used to serve a total load of 80 to 90 megavolt-amperes. Correspondingly smaller subtransmission stations and smaller 3-conductor 66-kv cable would be used in these cases.

H. G. Barnett and **D. F. Shankle** (Westinghouse Electric Corporation, East Pittsburgh, Pa.): Voltages in the 15-kv class are frequently too low for subtransmission in present-day metropolitan distribution systems. Many utilities probably would adopt a higher subtransmission voltage if they did not already have extensive installations in the 15-kv range. Mr. Smith and Mr. Michelson have shown how 12-kv subtransmission can be supplemented by 66-kv circuits. The congestion and operating difficulties of the 12-kv subtransmission system are reduced by the over-all more economical 66-kv circuits. Perhaps eventually the growth of the 12-kv system can be stopped by the use of higher subtransmission voltages such as 66-kv subtransmission. Further future simplification of 66-kv underground installations may make it possible to essentially supplant the 12-kv system.

It is interesting to note that the 66-kv circuits are treated as subtransmission circuits and not as parts of the transmission system. This simplifies the operation of the system and improves the over-all economy of both the subtransmission and transmission systems.

Eric T. B. Gross (Illinois Institute of Technology, Chicago, Ill.): The authors state that it is planned to ground the 12-kv neutrals of the transformers through 3-ohm resistors, principally for limiting single-line-to-ground fault currents.

I am wondering if resonant neutral grounding has been considered. It seems to me that ground fault neutralizers would bring additional advantages; they have been used in this country in underground cable systems when no other solution promised power supply to the consumers as free from disturbances as resonant grounding.[1] The experiences have been very satisfactory.

I do not underestimate the importance of transient overvoltages, but the differences between their values at various kinds of neutral grounding are small, and should be negligible in a 12-kv system. Resonant neutral grounding has proved successful in underground cable systems at much higher voltages. Relaying of ground faults in resonant grounded systems can be done easily by means of sensitive single-phase directional relays which are activated by the residual feeder current, and polarized by the neutral displacement (zero sequence) voltage. If such relays are installed, any voltage dips are eliminated which are otherwise unavoidable on single-line-to-ground faults.

REFERENCE

1. RESONANT GROUNDING OF DISTRIBUTION SYSTEMS, E. Herzog, *Proceedings*, Midwest Power Conference, Illinois Institute of Technology, Chicago, Ill., volume 11, 1949.

K. M. Smith and **E. L. Michelson**: The authors appreciate very much the interest in their paper and the various discussions which have been offered.

Mr. Woodrow has provided an excellent example showing that in some cases it would be more economical to use a smaller substation than the one being used in Chicago. It is very possible that with lighter load densities than those existing in Chicago, the smaller substation would provide greater economy. Local conditions vary a great deal, and in any economic study these dictate the final answer. It may be of interest that in a later project in Chicago, in an area of very high load density, the substations will have a capacity of 100,000 kva. Another important factor in connection with the size of station is the difficulty of acquiring real estate in crowded city areas. This situation tends to favor substations of a larger size.

Mr. Blake and Mr. Hill both mention the possibility of 66/4-kv substations. This arrangement has been investigated, but for various reasons it has not appeared to be advantageous for the Chicago conditions. It seems that a more logical development would be the use of 12-kv for distribution purposes to replace the existing 4-kv system.

While this possibility has been seriously considered, many difficult design and operating problems for the conditions existing in the Chicago system have prevented the adoption of the higher distribution voltage.

Mr. Wall is correct in his assumption that the fringe territories referred to in the paper are sufficiently congested that it is not feasible to use overhead construction. He refers to the use of switched capacitors for voltage control, but this method is not being used in Chicago due, principally, to the disadvantages of locating these capacitors in crowded alleys. In connection with the relay protection for the 66-kv lines, directional relays are used to open the 12-kv circuit breaker for a phase-to-phase fault on the 66-kv line. In the case of a phase-to-ground fault, the ground relays at the supply end of the line energize the same remote-trip wires that are used for transformer protection, and in this manner all terminals of the line are opened for all types of line and transformer faults.

In answer to Mr. Hill's questions, the power brought into Chicago from distant generating systems now is being supplied to the 12-kv busses of the generating and large distributing stations. In the new development the only change is that the 66-kv system is carried closer to the ultimate load. The advantages gained by the use of 22-kv instead of 12-kv do not appear to be sufficient to justify a major change in the subtransmission system, due to the relatively small increase in voltage. In effect, the new system in Chicago has increased the subtransmission voltage from 12-kv to 66-kv. The 12-kv cables which are required with the new arrangement are relatively short and are not an important factor in the over-all economy. With regard to the 12-kv bus voltage at the new substations, the voltage is compensated for load, with the result that the bus voltage varies from 12-kv at minimum load to approximately 12.4-kv at maximum load. In Chicago there are a few customers supplied directly from the 66-kv system. The units of loads supplied from the new 66-kv substations in some cases may be a single customer, but in most cases consist of small substation groups which require a maximum of four 12-kv lines.

Resonant neutral grounding is not being used in Chicago at the present time. Dr. Gross' suggestion for its use in the new 66-kv substations is a very logical one, and consideration should be given to its use. However, it does not appear that this type of grounding offers any over-all advantage compared to resistance grounding when it is considered that reserve capacity is provided for all substations and 12-kv customers, and, therefore, there is practically no need to operate a cable while it is faulted.

Overcurrent Investigation on a Rural Distribution System

G. F. LINCKS
MEMBER AIEE

D. R. EDGE
ASSOCIATE AIEE

W. C. McKINLEY
NONMEMBER AIEE

J. H. LEH
NONMEMBER AIEE

GOOD service continuity with the least possible outlay is a major problem on every rural distribution system. To make the rural line pay, the farmer must use electric power for milking machines, hay drying, water pumping, and many other tasks about the farm which affect his livelihood. With such industrialization of the farm, the average monthly consumption per consumer approaches 150 or 200 kilowatt-hours at present and is expected to reach as high as 400 or 500 in some areas in the future. Loads of these magnitudes make provision for high quality service more economically practicable.

Experience has shown that distribution primary line faults are the major cause of power service outages. Sectionalizing the line with overcurrent protective devices is an accepted procedure for economically reducing the number and duration of outages caused by primary line faults. The problem now is to evaluate numerically the benefits with different types of equipment and better protective methods.

Objective of Investigation

The objectives of the overcurrent investigation on the Central Electric Co-operative's system at Parkers Landing, Pa., during the years 1944, 1945, 1946, and 1947, were to:

1. Obtain a measure of the quality of service continuity provided to consumers.
2. Evaluate the effect of tree trimming on service continuity.
3. Gain experience with, and measure the effectiveness of, a new protective method employing overlapping operation of fuses and oil circuit reclosers to improve service continuity and to reduce restoration expense.
4. Study the effect of line length on service continuity.
5. Obtain operating data on the overcurrent protection problems at individual transformers.
6. Obtain fundamental data on overcurrents on rural circuits.

The Rural System

Characteristics of the Central Electric Cooperative's 7,200/12,470-volt 4-wire multigrounded wye rural-distribution system included long radial circuits, penetration into less densely populated areas resulting in wider separation of lightning protective equipment, and other factors which magnify the overcurrent protection problem. The Cooperative's management recognized the imperative need for improving service continuity and made the system available for experimentation in a combined overvoltage[1] and overcurrent investigation. It was believed that the data obtained would be of value to all operators of rural systems.

This system feeds an area of approximately 40 by 60 miles in the hilly country along the Allegheny River in northwestern Pennsylvania, where lightning, wind, trees, and other hazards to good service are of typical severity. Rural Electrification Administration type of line construction is employed, using practically only one make of electric equipment. In 1944, there were 1,015 miles of primary line ($87\frac{1}{2}$ per cent single phase line-to-neutral) feeding 4,178 consumers through 3,100 transformers. East and west sections of the system were fed by separate 900-kva transformer banks, supplied from the West Penn Power Company's 25-kv line. There were two feeders on each section, as shown in Figure 1. Tables I and II show the growth of the system during the period of the investigation.

The Overcurrent Protective Equipment History

As on many rural distribution systems, originally fuses alone were used to sectionalize the main feeder and to isolate faults to branches, and sub-branches. However, fuses are not capable of differentiating between nonpersistent faults, which can be cleared with only a momentary outage, and persistent faults which require the service of a line crew. The high percentage of nonpersistent faults prevalent on rural systems caused too many unnecessary outages and service trips. This led to the substitution of reclosers in the main line and on long branches. Most branches were not protected, as shown in Figure 1, since in 1944 available reclosers did not provide for coordination with fuses and to use reclosers in each branch was too costly.

Neither fuses alone nor reclosers alone even approached the desired service continuity. The need was recognized[2] for co-operative operation of fuses and reclosers in which the recloser would clear all nonpersistent faults without even partially melting the fuses, and the fuses would isolate all persistent faults to the faulted section of the line. This system, referred to as "overlapping protection," has been used successfully for many years with substation relays and line fuses.[3–4]

System Requirements Studied

In planning the investigation to use the overlapping fuse-recloser protection, the

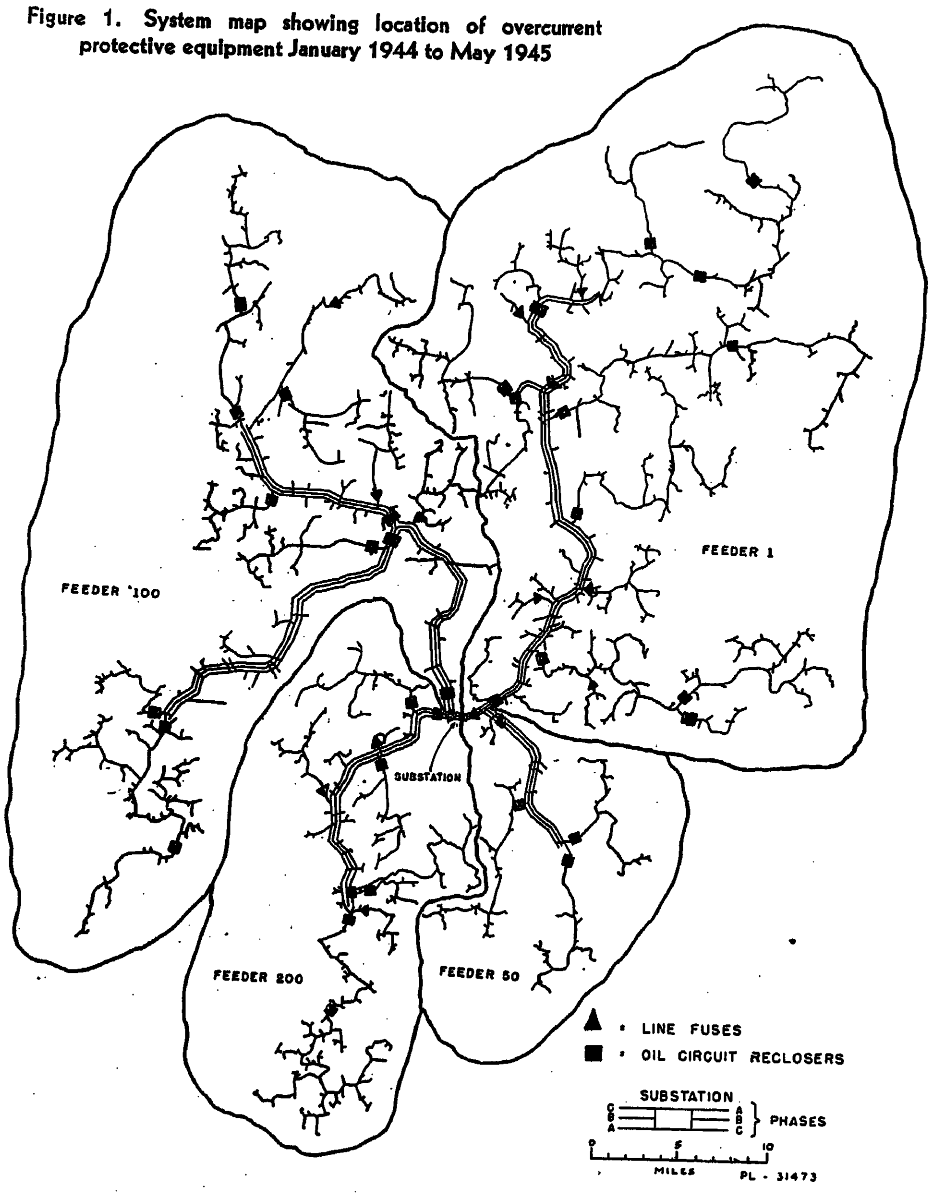
---

Paper 49-175, recommended by the AIEE Transmission and Distribution Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20–24, 1949. Manuscript submitted November 15, 1948; made available for printing May 2, 1949.

G. F. LINCKS is with the General Electric Company, Pittsfield, Mass.; D. R. EDGE is with the Graybar Electric Company, Inc., Chicago, Ill.; W. C. MCKINLEY is with the Central Electric Co-operative, Inc., Parkers Landing, Pa.; and J. H. LEH is with the General Electric Company, Pittsburgh, Pa.

The authors wish to express their appreciation to the Board of Directors of the Central Electric Co-operative Inc., and to the line crew of the same company, as well as D. D. MacCarthy, J. W. Kalb, W. H. Eason, D. A. Stann and to Mrs. M. C. Lovejoy.

Figure 1. System map showing location of overcurrent protective equipment January 1944 to May 1945



following system requirements were apparent:

1. The overlapping protection must include the entire system from the substation to the ends of feeders and all branches. Otherwise the maximum improvements in service continuity and in reduction of restoration expense can not be realized.

2. For economical use of reclosers, each recloser should clear nonpersistent faults on as much of the feeder and connected branches as it is capable of protecting.

3. Generally reclosers, applied as in (2) will protect from 15 to 25 miles, or more, of line. Such distances are too great for quick location of faults and, at times, for the physical endurance of the crews. Thus additional sectionalizing is necessary between reclosers to isolate persistent faults. Fuses are most economical for achieving this additional sectionalizing.

4. Overcurrent protective means must be provided for the isolation of persistent faults to the faulted branch or sub-branch. Again fuses are the most economical devices.

5. All reclosers and fuses in the line must co-ordinate properly with both external and internal primary fuses at the individual distribution transformer installations. Self-protected transformers of pre-1939 manufacture, with the larger internal fuses,[5-6] were scattered all over the system. Since determination of pre- or post-1939 manufacture was not considered practicable, a minimum primary line fusing of 15 or 20 amperes for $1^1/_2$- and 3-kva transformers and 20 or 25 amperes for 5–$7^1/_2$- and 10-kva ratings was necessary.

6. The same 100 per cent successful co-ordination, which has been proved practicable between fuses, must be provided in the co-ordinated overlapping protection using reclosers with line sectionalizing fuses, branch fuses, sub-branch fuses, and internal transformer fuses, all connected in series for sequential operation.

Table I. System Growth During Investigation—Miles of Line and Number of Consumers

Approximately 10 Per Cent is Three-Phase, $2^1/_2$ Per Cent is Two-Phase and $87^1/_2$ Per Cent is Single Line-to-Neutral Construction

Feeder and Phase	Exposure in Miles* 1944	1945	1946	1947	Number of Consumers 1944	1945	1946	1947
Feeder number 1	(437)†	(455)	(476)	(476)				
	510	528	549	567.5	1,841	1,938	1,950	2,476
A	180	186	187	196	633	666	670	832
B	80	84	169	172	299	315	610	755
C	250	258	193	199.5	909	957	670	889
Feeder number 50	(72)	(74)	(88)	(88)				
	90	92	106	112.5	310	339	337	490
A	44	45	54	59	101	110	110	176
B	82	33	33	34.5	118	128	127	140
C	14	14	19	19	91	101	100	174
Feeder number 100	(319)	(334)	(335)	(339.5)				
	423	438	440	451	1,230	1,245	1,240	1,520
A	77	80	149	158.5	170	175	406	579
B	264	273	158	159	725	730	496	593
C	82	85	133	133.5	335	340	338	348
Feeder number 200	(187)	(202)	(208)	(208)				
	221	236	242	254	797	866	861	1,100
A	48	53	71	72	125	148	222	344
B	81	86	74	77	321	344	267	322
C	92	97	97	105	351	374	372	434
	(1,015)	(1,065)	(1,107)	(1,111.5)				
	1,244	1,294	1,337	1,385	4,178	4,388	4,388	5,586

* Changes due to growth and rephasing.

† Mileage values shown in parentheses treat three-phase and two-phase as one line. Other mileage totals add three lengths for three-phase and two for two-phase.

Overcurrent Protective Methods and Equipment

Three co-ordinated overlapping fuse-recloser protective methods were analyzed by making actual co-ordination studies on the Central Electric Cooperative system, the findings are shown in Table III.

This analysis showed the co-ordinated fuse-lock-closed recloser method (number 3) to be best. Therefore, a mechanism, Figure 2, was designed to modify the type *FP-119* reclosers in use on this rural system so the reclosers opened instantaneously twice and then they locked closed.

Because of lack of energy to reset this mechanism, it had to be reset manually after the properly co-ordinated fuse blew. This was neither as efficient nor error-proof as the reclosers designed later for automatic resetting.[9]

Co-ordination Study

It was decided to make the study on the feeder on which the lightning investigation[1] was being conducted, thus making possible the co-operative use of an automatic oscillograph, to be described later. The proper rating of reclosers and fuses were determined by a co-ordination study.

All branches from each of the main feeders were numbered using series starting with 1, 50, 100, and 200 and the feeders became known by these numerals, as shown in Figure 1. All of the sub-branches were lettered on each of the main branches.

The map, Figure 3, shows the location of the reclosers and fuses and Table IV gives a history of the installations of protective equipment during 1944, 1945, 1946, and 1947.

The maximum and minimum (40-ohm fault resistance) short-circuit currents were calculated using the General Electric simplified template method of calculation,[10] and were checked independently within 2 per cent by graphical calculation.[11] When checked later against oscillograph data, the calculated values proved to be slightly high. Field tests showed this to result from not factoring sufficient reductions in fault current at the substation during light load generation.[12]

The co-ordination of the reclosers and fuses was determined as follows:

1. Reclosers were located on the main feeder at, or on the source side of, the point where the calculated minimum fault current equalled the minimum pick-up current of the next recloser on the source side. Reclosers were located at the branch junction points of long branches which the main line recloser would not protect to the ends.
2. Charts were made for the overlapping fuse-recloser co-ordination, which included a correction for the cooling of the fuse link during the period between the first opening and reclosing of the recloser.
3. Existing charts[13] were used for fuse-to-fuse co-ordination.

Obtaining the Data

System Operating Data

The data originated from individual job reports of the linemen and trouble crews which were recorded in a log book

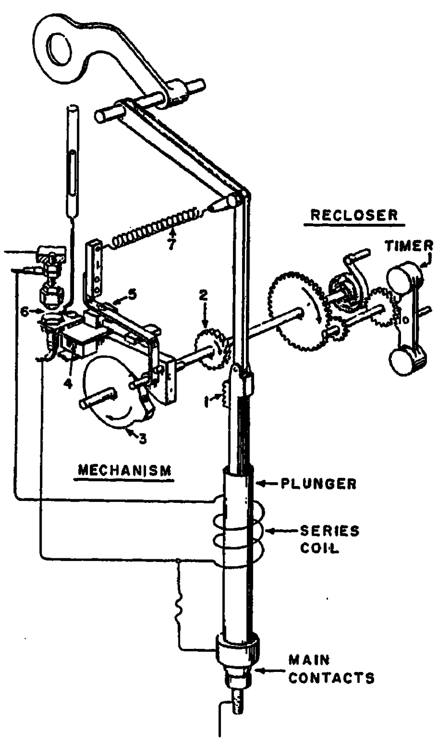


Figure 2. Mechanism modifying *FP-119* reclosers to provide two instantaneous openings to lock closed

Operation:—Fault current through series coil moves plunger up opening main contacts. When plunger resets to reclose contacts, rack 1 engages pinion 2, rotating eccentric cam 3. Second reclosing moves cam 3, so armature 4 is picked up by alnico magnet 5, closing auxiliary contacts 6, which shunt series coil and lock recloser closed. Spring 7 moves magnet 5 away from armature 4 for manually resetting mechanism

Table II. System Growth During Investigation—Distribution Transformers and Load

Year	Average Number of Transformers	Average Kilowatt-Hours Consumed Per Consumer Per Month
1944	3,100*	81.6
1945	3,100*	91.1
1946	3,225*	100.5
1947	3,410*	150 (approx.)

* Approximately 950 of these were the conventional distribution transformers with external fuse cutouts and lightning arresters (on the first 400 miles.) All other transformers were the self-protected type with internal primary fuses and secondary circuit breakers.

Original system: 400 miles energized in August 1938 with 1,200 consumers; 400 miles energized in June 1940 with 1,200 consumers; and 300 miles energized in February 1941 with 1,600 consumers.

During this 4-year period the load increased to 184 per cent of its original value. In doing so it greatly exceeded the ultimate load for which the system was designed.

Table III

Method Number	Nonpersistent Faults Cleared Without Partially Melting Fuses by:	Sectionalizing or Branch Fuse Caused to Isolate Faulted Section by:	Findings
1	Three instantaneous openings by recloser	Going to recloser location and manually shunting the recloser with a fuse after the recloser locks open	(a) Does not save travel time and expense. Does aid in locating fault (b) Required line fuses too small (below 15 amperes) to co-ordinate with internal transformer fuses
2*	Two instantaneous openings by recloser	Two time delayed openings of recloser (lock-out of recloser clears a section of feeder on the load side)	(a) Presented a problem of co-ordination with fuse on source side (b) Sectionalizing between reclosers generally must be done with more costly reclosers. If fuses are used, it increases the number of cases under (c) (c) In some cases required line fuses too small to co-ordinate with internal transformer fuses
3*	Two instantaneous openings by recloser	Recloser locks closed until fuse blows, then resets automatically (fuse at recloser location clears a section of feeder on the load side after recloser locks closed)	(a) Co-ordination with source side fuse is no problem (b) Meets all of the system requirements outlined previously

* No commercial reclosers available when investigated. Findings were rechecked later when they became available.[7,8,9]

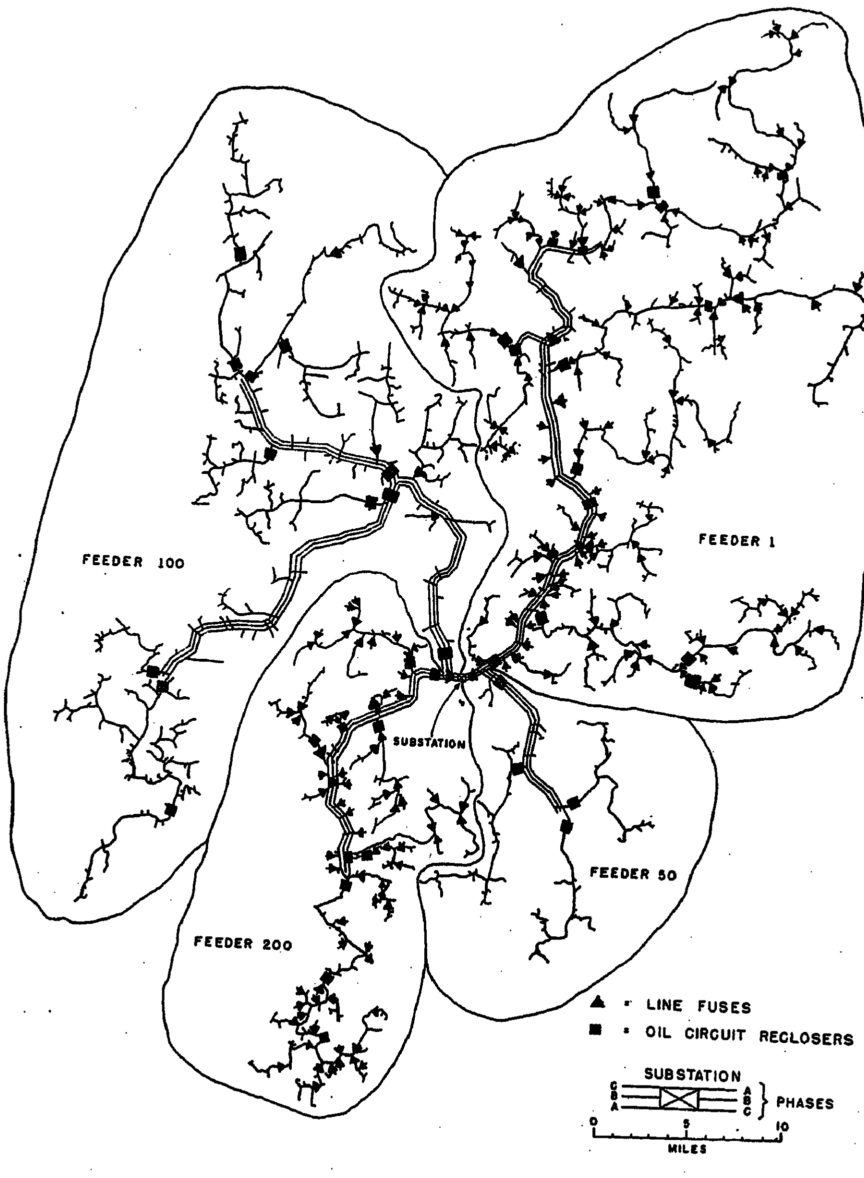


Figure 3 (left). System map showing location of overcurrent protective equipment for 1947

See Table III for dates of installation of coordinated fuses and recloser on feeders 1 and 200

and on line and distribution transformer outage reports, Figure 4.

Fundamental Data on Overcurrents

Information on overcurrents were obtained from *GE-PM-13* automatic electromagnetic oscillographs,[14] which were in operation on feeder number 1 during the periods from April to November of the years 1945, 1946, and 1947, and on feeder number 200 from July to November 1946 and April to November 1947. Each oscillograph tripped automatically within one-half cycle and recorded all 60-cycle fault currents beyond the oscillograph in each of the 3-phase conductors, and the system neutral, as well as *A* and *C* phase voltages. Its operation was checked by periodic calibration and daily inspection and also by periodically short circuiting the ends of the line.

The System Operating Data Obtained on Primary Lines

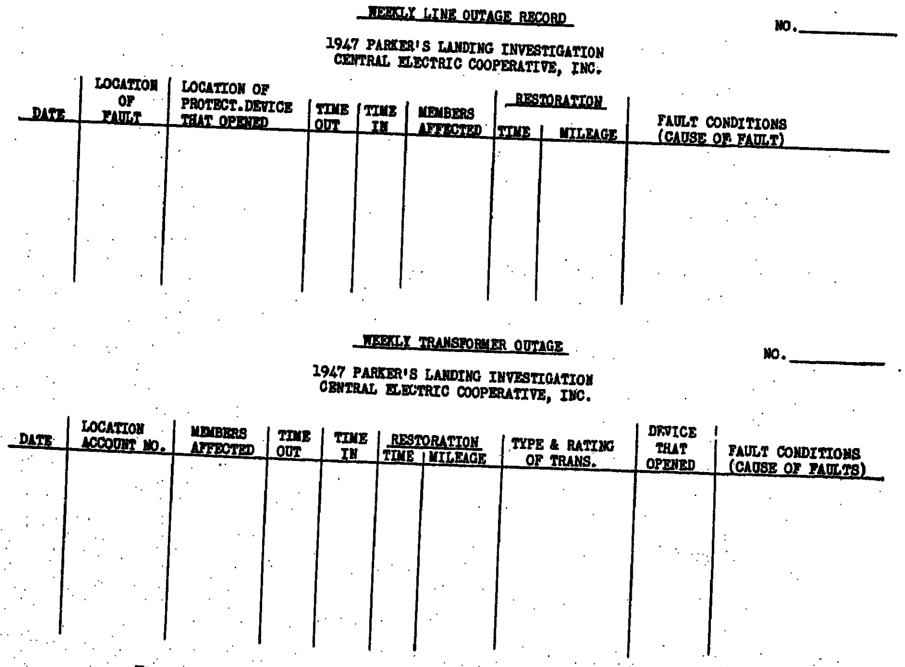
The data, presented in a series of charts, cover persistent faults, which caused prolonged outage and required the attention of trouble crews. All parts of feeders 1, 50, 100, and 200 were protected by reclosers and thus nonpersistent faults were cleared with only a momentary outage. They were not recorded, except by the oscillographs, since the services of the trouble crews were not required. In order to interpret these results in terms of total faults, the percentage of nonpersistent faults on feeders 1 and 200 are given in Table V, as obtained from oscillograph records.

Causes of Persistent Line Faults, Figure 5

The causes of persistent line faults are compared on the basis of the number per mile of line, in order to factor differences in exposure. A chart plotted on the basis of the number of faults is similar to Figure 5. Note the relatively wide variations for different years which indicates the value of several years' experience in obtaining good average data.

A comparison with other published data, on the basis of the number of faults and the percentage of the total number of faults, is given in Table VI using the terminology for causes of Gilkeson, Jenne, and Davenport.[8]

A relatively low percentage of flash-



WEEKLY LINE OUTAGE RECORD

NO.________

1947 PARKER'S LANDING INVESTIGATION
CENTRAL ELECTRIC COOPERATIVE, INC.

DATE	LOCATION OF FAULT	LOCATION OF PROTECT. DEVICE THAT OPENED	TIME OUT	TIME IN	MEMBERS AFFECTED	RESTORATION		FAULT CONDITIONS (CAUSE OF FAULT)
						TIME	MILEAGE	

WEEKLY TRANSFORMER OUTAGE

NO.________

1947 PARKER'S LANDING INVESTIGATION
CENTRAL ELECTRIC COOPERATIVE, INC.

DATE	LOCATION ACCOUNT NO.	MEMBERS AFFECTED	TIME OUT	TIME IN	RESTORATION		TYPE & RATING OF TRANS.	DEVICE THAT OPENED	FAULT CONDITIONS (CAUSE OF FAULTS)
					TIME	MILEAGE			

Figure 4. Samples of weekly line and transformer outage reports

Table IV. History of Installation of Overcurrent Protective Equipment During Investigation*

<table>
<tr><th></th><th colspan="12">1944</th><th colspan="12">1945</th><th colspan="12">1946</th><th colspan="12">1947</th></tr>
<tr><th></th><th>Jan.</th><th>Feb.</th><th>Mar.</th><th>Apr.</th><th>May</th><th>June</th><th>July</th><th>Aug.</th><th>Sept.</th><th>Oct.</th><th>Nov.</th><th>Dec.</th><th>Jan.</th><th>Feb.</th><th>Mar.</th><th>Apr.</th><th>May</th><th>June</th><th>July</th><th>Aug.</th><th>Sept.</th><th>Oct.</th><th>Nov.</th><th>Dec.</th><th>Jan.</th><th>Feb.</th><th>Mar.</th><th>Apr.</th><th>May</th><th>June</th><th>July</th><th>Aug.</th><th>Sept.</th><th>Oct.</th><th>Nov.</th><th>Dec.</th><th>Jan.</th><th>Feb.</th><th>Mar.</th><th>Apr.</th><th>May</th><th>June</th><th>July</th><th>Aug.</th><th>Sept.</th><th>Oct.</th><th>Nov.</th><th>Dec.</th></tr>
<tr><th></th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th><th>1</th></tr>
<tr><td>Feeder Number 1</td><td colspan="20">16—FP119 reclosers
1—section fuse
5—branch fuses</td><td colspan="12">May 11
19—Modified FP119 reclosers**
5—fused section points
125—branch fuses</td><td colspan="8">6—fused section points
151—branch fuses†</td><td colspan="8">171—branch fuses†</td></tr>
<tr><td>Feeder Number 50</td><td colspan="48">6—FP119 reclosers
0—fused section point
2—branch fuses</td></tr>
<tr><td>Feeder Number 100</td><td colspan="48">18—FP119 reclosers
1—fused section point
2—branch fuses (3 element)</td></tr>
<tr><td>Feeder Number 200</td><td colspan="32">9—FP119 reclosers
0—fused section point
2—branch fuses (3 element)</td><td colspan="8">Sept. 1
16—modified FP119 reclosers
5—fuses section point
71—branch fuses</td><td colspan="8">90—branch fuses†</td></tr>
</table>

* See Maps, Figures 1 and 3. ** See Figure 2. † Extra fusing found to be advisable through operating experience.

overs were recorded as causing persistent faults, see investigation number 1, Table VI. A majority of flashovers, all whipping conductors, some tree limbs on line, and so forth, were nonpersistent faults, being cleared with only a momentary outage. Conversely, in investigation 9, Table VI, the fuses in reclosing cutouts probably were blown by some of these faults. Thus the comparison of values in investigations 1 and 9 may indicate the margin of benefit of co-ordinated fuses and reclosers versus reclosing fuse cutouts. A similar condition is indicated in Table IX showing 0.12 and 0.28 persistent faults per mile per year respectively for investigations 1 and 9. These differences would indicate an additional margin in benefits for overlapping fuse-lock-closed recloser co-ordination versus 3-shot reclosing cutouts over that shown later for overlapping fuse-lock-closed recloser co-ordination versus reclosers only.

Section 10 on "impulse blowing of fuses," Table VI, (lightning storm-blown fuses, Figure 5) is analyzed in more detail in Table VII. Fuses were considered to have been blown by lightning currents passing through the fuse to ground where there were no oscillograph records of 60-cycle current corresponding to the blowing of a line fuse during a lightning storm. If lightning arresters had been installed on each side of the line fuse, the impulse currents might have gone to ground without passing through the fuse. Thus Table VII in showing lightning blowing of fuses to be about 5.2 per cent for all the fuses on the line, represents the most severe condition.

The section on "insulator or conductor breakage," Figure 5, resulted almost entirely from irresponsible shooting by hunters.

The sections on "acts of man" in Table VI and "Trees—man, and motorized equipment" in Figure 5 are high because of extensive right-of-way clearing on the entire system in 1945, heavy logging operations along the right-of-way in 1947, and the strip coal mining carried on in the area.

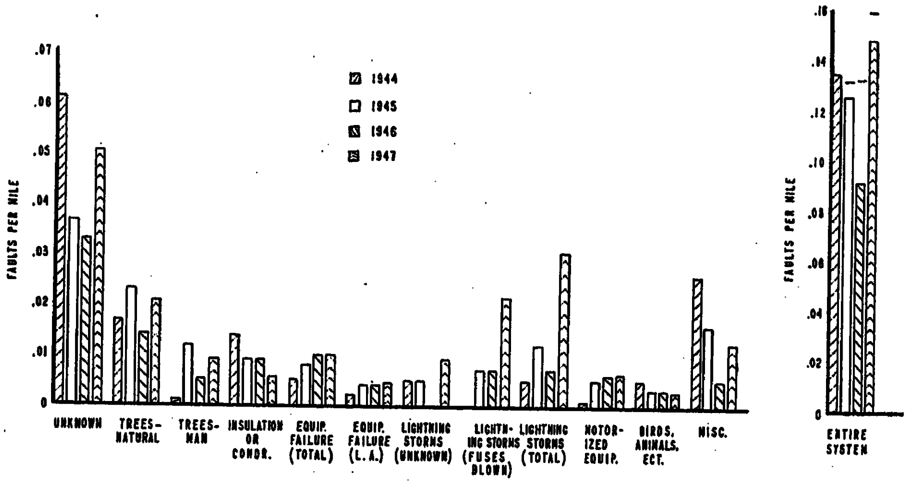


Figure 5. Causes of persistent line faults

Faults per mile of line

"Wires down" were recorded under other causes in Figure 5 and thus is left blank in Table VI. The 78 cases of wires down are analyzed in Table VIII showing that quicker fuse blowing might have prevented burndown in only 12 cases (15 per cent). Mechanical breakage occurred in 67 per cent of the cases and may have in 18 per cent more.

Exposure—Number of Persistent Faults, per Mile of Line, Figure 6

The exposure, expressed in terms of the number of faults per mile, affords a measure of the severity of overcurrent protection problems. Direct comparisons of exposure can be made for different periods on the same system or between systems provided causes of unusual numbers of nonpersistent faults are taken into consideration or only persistent faults are included, as is done in this investigation.

Note, in Figure 6, the wide variation in the exposure on different phases (single-phase circuits) of the same feeder during the same year. This shows the value of making the analysis on the basis of 12 single-phase circuits rather than just four feeders where loads and branches are predominantly single phase. This is also shown in Figures 7, 9, and 10. In the lightning investigation,[1] spot checks covering a large area on this rural system,

Table V. Nonpersistent Faults in Per Cent of Total Faults (From Oscillograph Data)

Feeder Number	1944	1945	1946	1947
1	*	84.8%**	80.4%†	82.9%†
200	*	*	88.1%‡	89.0%†
1 and 200	*	*	84.3%	85.9%

* No oscillograph data. ** May to November. † April to November. ‡ July to November.

Table VI. Comparison of Causes of Persistent Line Faults, Figure 5 with Other Published Data

Causes of Persistent Line Faults*	Investigation 1	2	3	4	5	6	7	8	9	10
Bibliographical Reference	Figure 6	3	3	3	3	3	3	2	15	16
	Number of Faults Per Percent of Total Number of Faults									
1. Flashover (lightning storm unknown)	20 3.7%	26 87%	146 76%	62 52%	670 96.3%	1 5%	260 88%		495 43.5%	104 52%
2. Insulator or bushing failure (insulation of conductor)	40 7.7%						1 0.3%		28 2.5%	
3. Wires down	Included in other causes		15 90%	41 35%	2 0.3%	3 14%	15 5%	10.1%	11 1%	
4. Birds or animals	14 2.8%		1 0.5%		3 0.5%	8 36%				11 5.6%
5. Poles broken or blown down		1 3%	2 1%	2 2%				1.9%		
6. Whipping conductors	Not recorded			5 4%	1 0.2%				63 5.6%	
7. Crossed wires			7 3.5%		1 0.2%					
8. Trees or limbs on line (trees natural)	80 15.3%	1 3%	5 2%	1 1%		1 5%	7 2.7%	25.5%	212 18.3%	47 23.6%
9. Acts of man (trees-man and motorized equipment)	49 9.3%								55 4.8%	
10. Impulse blowing of fuses (lightning storms fuses blown)	39 7.4%									
11. Under fused									4 0.4%	
12. Equipment failures	26 5.0%		17 9%	7 6%	16 2.5%		12 4%	11%	77 6.8%	5 2.6%
13. Miscellaneous	62 11.8%							18.5%	20 1.8%	
14. Cause unknown	194 37%	2 7%				9 40%		21.2%	165 14.5%	32 16.2%

Data on Systems Investigated										
Type of Circuit	Rural 5 Per Mile	Urban	Rural Suburban	Urban	Rural Suburban	Urban	Rural 7 Per Mile	Rural	Rural	Rural
Voltage, kilovolts	7.2/12.5	4	4	6.9	12	13.8	13.8	7.2	2.3, 4, 7.2, 12.5	6.9
Lightning protection	Expulsion and valve lightning arresters	Lightning arresters	Lightning arresters	Lightning arresters	Spill gaps	Spill gaps expulsion lightning arresters	Spill gaps	½ spill gaps ½ lightning arresters		Lightning arresters
Tree condition	Bad	Bad-Good	Good-Fair	Average	Average	Light	Light			
Protective equipment	Reclosers and fuses	Station Circuit Breaker**	Station Circuit Breaker**	Station Circuit Breaker**	Station Circuit Breaker**	Station Circuit Breaker**	Station Circuit Breaker**	Reclosers	Single and reclosing fuses	Reclosers and reclosing fuses
Length of circuit in miles	1015 to 1111	43	193	118	633	22	55,238	369	35	295
Period of investigation	1-1-44 to 12-31-47	6-14-34 to 1-1-36	10-25-33 and 6-8-34 to 5-12-38	7-19-34 to 10-13-38	5-20-36 to 3-7-39	3-10-38 to 7-11-38	5-26-36 to 7-11-38	4-1-42 to 10-1-42	1943 to 1947	Summer 1945
Type of faults†	*a*	*b*	*b*	*b*	*b*	*b*	*b*	*c*	*d*	*a*

* Terminology in parentheses is that for Figure 5.

** Instantaneous ground relays and immediate reclosure locking out after initial operation and inverse time delay phase relays. Fuses and reclosers used in branches.

† Type of fault included in data.

a Persistent faults only, requiring trouble crew.

b Persistent and nonpersistent faults recorded by oscillograph.

c Persistent and nonpersistent faults determined by counter readings on reclosers which may be high because often successive recordings are made for individual faults.

d Persistent faults only. Blowing the third fuse of reclosing cutouts by temporary faults caused recorded outages.

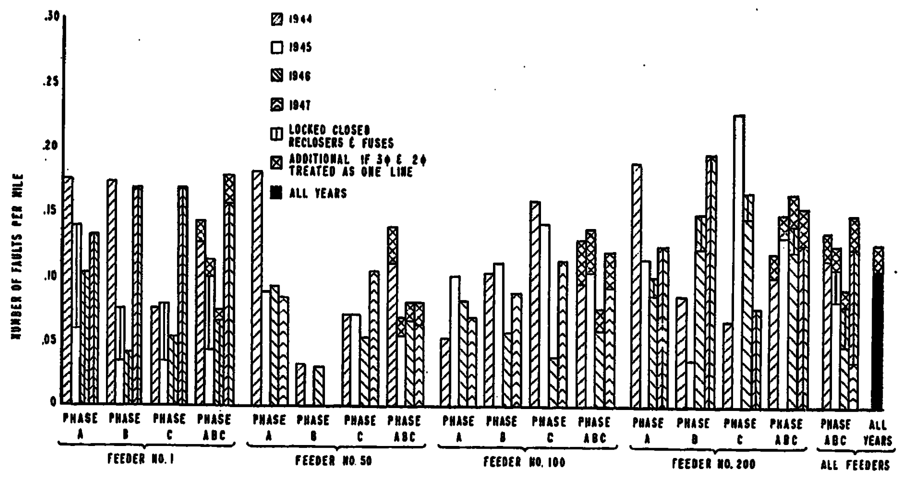


Figure 6. Exposure—Number of persistent faults per mile per year

showed wide variations in the number and severity of lightning storms even for distances of 10 or 15 miles.

The comparison of exposure with other published data, in Table IX, indicates about equal conditions to that for other systems where comparable lightning protection was employed. The "spill gaps" used for lightning protection in investigations 5, 6, and 7 naturally increased the number of faults per mile. The data in investigation 7 may be high since it is prorated to a yearly basis from data taken during the lightning period and by counter readings on reclosers which do not take into consideration successive recordings of the counter on the same fault in some cases.

Hours Out Per Outage, Figure 7

The hours out per outage for any year or period are influenced largely by whether or not there were any long outages, as shown by spot checking the data. Most of these long outages result either from long repair jobs or a fault which was difficult to locate, such as a damaged insulator. Therefore Figure 7 is not usable to compare the benefits from the use of different types of protective equipment. However, since it does show the prevalence or absence of long outages, the data in Figure 7, when combined with the exposure data in Figure 6, are extremely valuable to check the severity of fault conditions in making other comparisons.

The average restoration time curves, Figure 8, show that the "fault location time" is the only portion of the total hours out per outage affected by different types of overcurrent protection. However, when the overcurrent protection on one portion of a system markedly reduces the number of consumers affected by faults such as on feeders 1 and 200, it is natural to send the crew to take care of greatest number of consumers first, as on feeders 50 and 100, not having the improved protection, if a choice is necessary. This added the total restoration time for faults on feeders 50 and 100 to the travel time for faults on feeders 1 and 200 on numerous occasions in this investigation.

Hours Outage per Consumer Per Year, Figure 9, or Hours Outage Per Consumer Per Mile Per Year, Figure 10

The hours outage per consumer per year or the hours outage per consumer per mile per year appear to be the only measures which will define the quality of service rendered either the individual consumer or all the consumers on a circuit or system.

They are determined as follows:

$$\text{Hours Outage Per Consumer Per Year} = \frac{\text{Yearly sum of (no. of consumers affected) by (number hours out) for each outage}}{\text{Total number of consumers on circuit}}$$

$$\text{Hours Outage Per Consumer Per Mile Per Year} = \frac{\text{Hours outage per consumer per year}}{\text{Total number miles of line in circuit}}$$

The number of consumers and length of circuit existing at the first of the year were used throughout the year regardless of growth.

The only known published data using hours outage per consumer per year are those presented by W. J. Lyman[16] and a mathematical study made on a hypothetical circuit in 1941.[17] Other data, such as in references 2, 3, 16, 19 and 20 have measured the effectiveness of automatic reclosing protection by the number of momentary circuit interruptions versus the number of lockouts.

A study of Figures 9 and 10 shows the minimum and maximum hours outage per consumer per year (or per mile per year) for the 12 circuits to vary as shown in Table X.

Note that the minimum and maximum values were obtained on the same feeder and phase for both the hours outage per consumer per year and the hours outage per consumer per mile per year, except for the maximum in 1944, the minimum in 1946 and the maximum in 1947.

Average 1 in Table X, based on averaging the hours outage per consumer per year (or per mile per year) for the 12 single-phase circuits, differs considerably from average 2 (or 2'), in which the system is treated as a unit. This difference was shown for each of the four 3-phase feeders. In average 1 only the actual number of consumers (and mileage) involved were employed for each of the 12 circuits which when averaged gave the truer values. Thus average 1 was used in plotting Figures 9 and 10.

Comparison of a phasing map with Table X shows that in general the minimum values are for the shorter single-phase circuits while the maximum values are for the longer single-phase circuits which extend out to the extreme ends of the feeder. The circuit (100A) was relatively short in 1944 when showing the minimum value of hours outage per consumer per year. However, through rephasing, it was a relatively long circuit in 1947 when showing maximum values. Circuit *1C* is one of the longest circuits on the system, but had the overlapping fuse-lock-closed recloser protection when showing the minimum hours outage per consumer per mile per year in 1946. Circuit *50A*, the maximum in 1947, while a relatively short circuit is the longest of feeder 50 and did not have the improved protection.

Some urban circuits are reported to have 1.25 hours outage per consumer per year. W. J. Lyman[20] gives the service continuity standards for his company as one hour for urban systems and two hours for rural systems. These values are well below most of those shown in Figure 9 for the early years of the investigation. This indicated considerable margin for improvement by the use of co-ordinated fuses and lock-closed reclosers if such a continuity standard was to be provided.

Outages for Individual Consumer

While studies of relative service continuity applied to the system as a whole

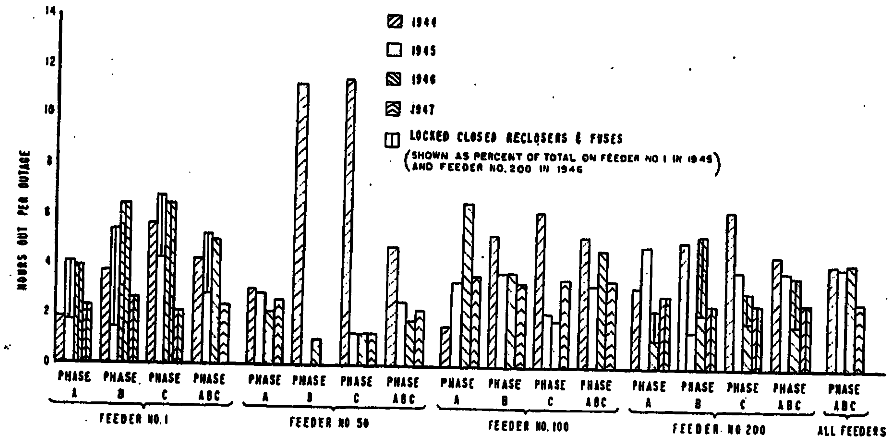


Figure 7 (above). Hours out per outage

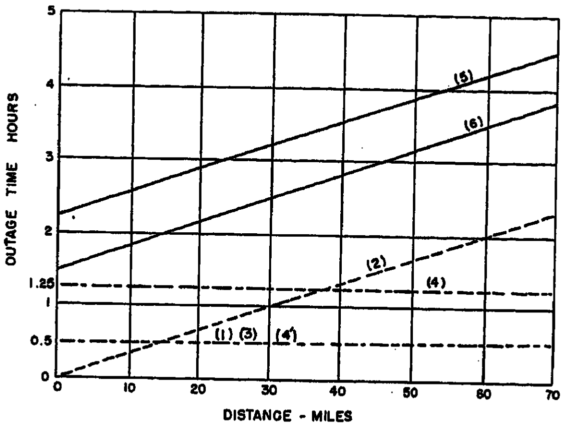


Figure 8 (left). Average restoration time curves based on approximately 480 outages 1944–1947

The total restoration time (5) with reclosers only or (6) with improved fuse and recloser protection = (1) Time elapsed before outages report+(2) travel time+(3) repair time+(4) fault location time with reclosers only or (4') with improved fuse recloser protection

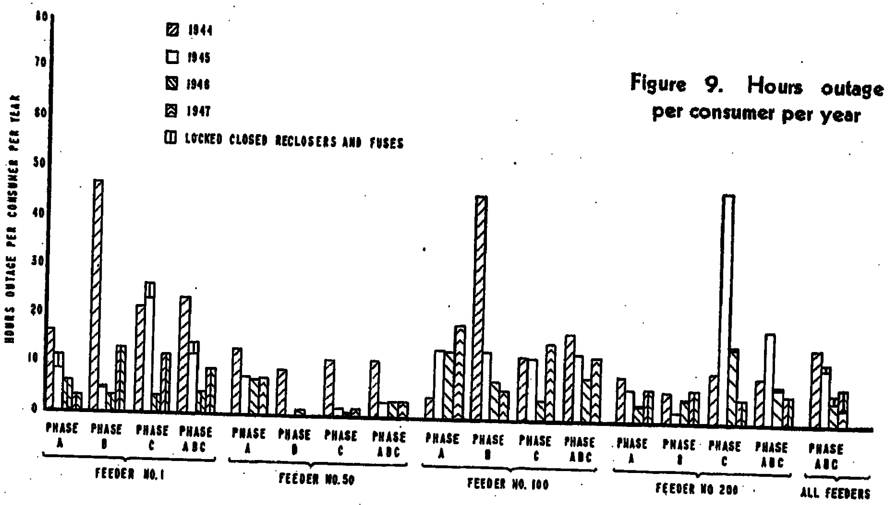


Figure 9. Hours outage per consumer per year

afford good over-all data, the individual consumer is interested in the frequency and length of outages to him personally. It is natural that curves plotted from data on the number and duration of individual consumer outages vary with the distance from the substation. This is also shown in Table XI which gives representative data on outages of individual consumers. It shows minimum and maximum values for the maximum hours outage per year and the number of outages for any individual consumer (items 1 and 6).

Also, Table XI gives a ratio (item 9) with which to multiply the hours outage per consumer per year for the whole circuit to determine the maximum hours outage per year for any individual consumer. The average ratio for all four years was 3.18, with a minimum of 1 and a maximum of 9.8.

RESTORATION EXPENSE, FIGURE 11

The manhours and mileage traveled given in Figure 11, as obtained from the weekly reports during 1947, show reductions of maintenance expense which are vitally important today. The fact that 61.5 per cent of the total line mileage for the system had the overlapping fuse-lock-closed recloser protection while only 38.5 per cent was without this improved protection, tends to make comparisons more conservative. Also, in figuring the actual costs, the assumptions of $1.25 per hour average pay for the trouble crew and the 6 cents per mile for transportation probably are low in most cases.

Determination of Benefits Obtained with Fuse-Lock-Closed Recloser Co-ordination

HOURS OUT PER OUTAGE

Figure 8 shows that 1/2 hour was required to locate faults with the co-ordinated fuses and reclosers as against 1.25 hours with only the reclosers having four

Table VII. Lightning Blowing of Line Fuses

Determined From Oscillograph Records Where No Fault Current Was Recorded When a Fuse Link Blew During a Lightning Storm

	1945*			1946**			1947†			Average		Other Data‡	
Rating of Fuse	Number Blown	Number in Circuit	Per Cent Blown	Number Blown	Number in Circuit	Per Cent Blown	Number Blown	Number in Circuit	Per Cent Blown	Number Blown	Number in Circuit	Per Cent Blown	Number in Circuit
15	4	28	14.3	3	56	5.35	6	73	8.2	13	157	8.3	72
20	2	27	7.4	2	33	6.0	3	52	5.8	7	112	6.2	78
25	0	18	0.0	1	45	2.2	3	58	5.2	4	121	3.3	No Data
30	0	19	0.0	1	12	8.3	2	23	8.7	3	54	5.5	No Data
40	1	12	8.3	0	16	0.0	2	28	7.1	3	56	5.4	No Data
15 to 40	7	104	6.7	7	161	4.3	16	234	6.8	30	500	6.0	No Data
All Rating	7	128	5.5	7	181	3.9	16	261	6.1	30	670	5.2	No Data

* On feeder number 1 May to November.

** On feeder number 1 May to November and feeder number 200, July to November.

† On feeders number 1 and 200, April to November

‡ Three years data on another system.

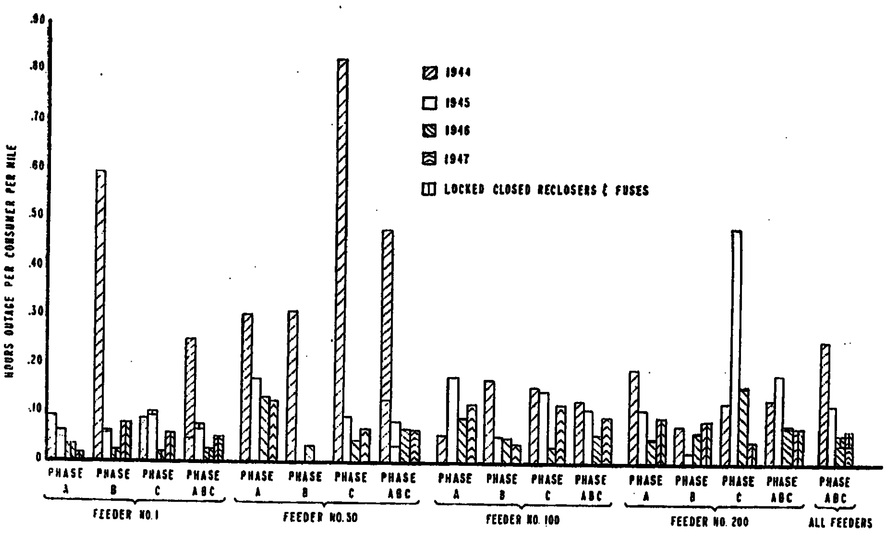


Figure 10. Hours outage per consumer per mile per year

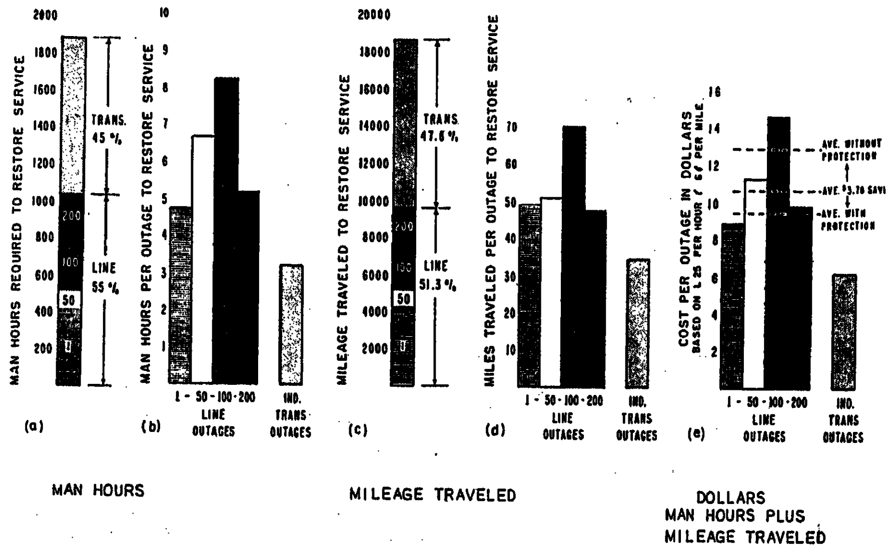


Figure 11. Restoration expense

instantaneous openings and three reclosures.

Service Continuity for the System as a Whole

The improvement in service continuity provided by the overlapping fuse-lock-closed recloser co-ordination was evaluated as follows:

1. In 1945, the results on feeder number 1 for the $4^1/_2$ months prior to the installation of the fuse-lock-closed recloser co-ordination were compared with those for the $7^1/_2$ months following the installation. On each of the single-phase circuits the severity of fault conditions measured in the number of outages per mile, Figure 6, and the hours out per outage, Figure 7, were slightly greater during the period with the fuse-recloser co-ördination than without it. Thus the comparison of the values for each period in Figure 10 is conservative in showing the following very substantial reductions in hours outage per consumer per mile per year: on *A* phase—64.6 per cent; on *B* phase—88.8 per cent; on *C* phase—85.5 per cent; and *ABC* phases—80.0 per cent.

2. In 1946 and 1947, the operating data were compared for the portions of the system having each type of protection (feeders 1 and 200 versus feeders 50 and 100), since comparisons could not be made on one circuit. As pointed out, feeders 50 and 100 benefited indirectly from the overlapping fuse-lock-closed recloser protection on feeders 1 and 200, so these comparisons are conservative. The consumer hours outage per mile were reduced 67.5 per cent in 1946 with slightly more severe fault conditions indicated by Figures 6 and 7.

In 1947, 70.5 per cent of the faults per mile, shown in Figure 6, were on the portion of the system with the co-ordinated fuses and reclosers. The hours per outage, Figure 7, were approximately the same (2.5 versus 2.8). Even under these conditions there were 21 per cent less hours outage per consumer per mile per year. If the faults per mile had been approximately equal on the two portions of the system, this saving would have been about 66 per cent.

A mathematical study of a hypothetical circuit in 1941[17] showed a possible saving of 88 per cent with overlapping line protection versus no line protection.

R. O. Askey and C. V. Miller reported[21] that benefits were obtained during $1^1/_2$ years operating experience with fuses and lock-closed reclosers co-ordinated for overlapping operation on a 17-mile 7.2-kv line. They also discussed other advantages of this method of protection.

Ability to Meet Desired Service Continuity

The hours outage per consumer per year on the portion of the system with the overlapping fuse-lock-closed recloser protection in Figure 9 varied as follows on the single-phase circuits.

Feeder Number	Year	Hours Outage	Average
1	1946	3.29 to 5.9	4.2
1	1947	3.1 to 13.0	9.2
200	1947	4.5 to 6.6	5.9

These values indicate that even with the best overcurrent protection it may not be possible to meet a service continuity standard of 2 hours outage per consumer per year on all single-phase circuits or a whole 3-phase rural feeder having extremities from 45 to 60 miles from the substation.

Since feeder 50 has a maximum length of only 25 miles and received a slight benefit from quicker handling of faults, because of the improved service continuity from the protection on feeders 1 and 200, its hours outage per consumer per year was much lower, as follows:

Year	Outage	Average
1946	0.8 to 8.0	3.0
1947	0 to 7.3	2.9

With the overlapping fuse-lock-closed recloser protection, it is probable that the service continuity on this feeder would have approached a standard of two hours outage per consumer per year.

RESTORATION EXPENSE

The restoration expense chart for 1947, Figure 11, shows that the co-ordinated fuses and lock-closed reclosers on 61.5 per cent of the total line mileage of the system required: 2.45 (32.8 per cent) less manhours per outage; 17.5 (26.8 per cent) less mileage traveled per outage; and $3.70 (28.5 per cent) less cost per outage.

The saving in cost per outage of $3.70 times the 138 outages per year equals $510 per year saving in out-of-pocket operating expense. Assuming a carrying charge of 15 per cent per year, the saving of $510 would justify an investment of $3,400. Assuming a carrying charge of 4 per cent per year, the same saving would justify an investment of $12,750.

The total investment in line equipment, on this rural system, to produce this saving of $510 would be $10,500. Therefore, the out-of-pocket saving alone resulting from the $10,500 investment will either carry approximately one-third of the investment or result in a net profit of $90 per year over the carrying charge, depending on the financing.

Other comparable published data, based on a mathematical study of a hypothetical circuit,[22] showed possible savings of 60 to 70 per cent in manhours and also in mileage traveled when comparing overlapping line protection with no line protection.

Table VIII. Lines Broken or Burned Down

	1944	1945	1946	1947	Total
Number lines broken or burned down	16	17	15	30	78
Could fuse or recloser have prevented burn down? Yes	0	4	1	7	12
No	12	9	11	20	52
?	4	4	3	3	14
Did Fuse Blow? Yes	12	12	10	16	50
No	0	4	5	5	14
Recloser opened Yes	4	1	0	9	14
Conductor					
Which 1 phase was down? Phase only	3	3	3	6	15
Neutral only	0	3	0	3	6
Both phase and neutral	8	7	7	16	38
3 phase—all four lines down	2	1	0	0	3
1 or 3 phase lines down	3	3	5	5	16
Lines damaged but not broken	2	3	0	3	8

Table IX. Comparison of Exposure in Number of Faults Per Mile Per Year with Other Published Data*

Type of Fault	1	2	3	4	5	6	7	8	9	10
Nonpersistent and persistent	0.88*	0.88	1.01	1.24	2.56	4.71**	1.76	3.65**	0.63†	No Data
Persistent only	0.12							0.07**	0.28†	

* For bibliographical reference and data on system see Table VI.

** Actual data based on period of less than a year. Values prorated to yearly basis and thus are only approximately correct.

† Based on 944 "cases of trouble" which are persistent faults. The nonpersistent and persistent value is based on the 2,314 fuses reported as blown assuming that these were blown by nonpersistent faults which blew fuses in the three shot cutouts and cleared the fault with only a momentary outage.

Table X

Hours Outage Per Consumer Per Year	1944	1945	1946	1947
Minimum	4.0 (100A)*	0 (50B)	0.8 (50C)	0 (50B)
Maximum	46.9 (1B)	46.5 (200C)	15.3 (200C)	19.0 (100A)
For All Four Feeders				
Average 1**	17.3	12.2	5.8	7.8
Average 2†	22.1	16.3	6.4	9.2
Hours Outage Per Consumer Per Mile Per Year				
Minimum	0.053 (100A)	0.0 (50B)	0.018 (1C)	0.0 (50B)
Maximum	0.082 (50C)	0.48 (200C)	0.158 (200C)	0.125 (50A)
For All Four Feeders				
Average 1	0.248	0.118	0.059	0.069
Average 2′‡	0.018	0.012	0.005	0.0075

* Figures in parentheses are single-phase circuits.

** Average 1 is based on averaging the hours outage per consumer per year (or per mile per year) for the 12 single-phase circuits.

† Average 2 is based on the total hours outage for all faults on the systems divided by the total number of consumers on the system.

‡ Average 2′ is average 2 divided by the total mileage on all the feeders.

The Operating Data on Outages at Individual Transformers

Outages at individual transformer installations accounted for 0.23 and 0.20 hours outage for every consumer on the system in 1946 and 1947 respectively. These values would be about 10 per cent of an over-all rural system performance of two hours outage per consumer per year and thus are significant. The restoration expense for outages at transformer installations, Figure 11(A) and (C) is nearly 50 per cent of the total.

Figure 12(A) shows the relationship in per cent of the total number of faults for each type of outage at individual transformer installations. Figure 12(B) shows the frequency of fuse blowings per 1,000 installations of external primary fuses at 950 conventional transformers; the blowing of internal primary fuses and the operations of secondary circuit breakers in 2,150 to 2,260 self-protected transformers.

Consumer faults or overloads on the load side of the service entrance fuses, caused the greatest number of outages; generally by opening the secondary circuit breaker. It is believed that a large percentage of the outages from unknown causes resulted from consumer faults. Probably the inability to make replacements influenced the 1946 and 1947 figures.

Lightning caused about an equal number of outages with both types of transformers. The blowing of 2-ampere fuse links at $1^1/_2$- and 3-kva and 3-ampere fuse links at 5-kva conventional transformers during lightning storms (when protected by interconnected lightning arresters located on the line side of the fuse), varied as follows:

	1944	1945	1946	1947	Average
Fuses blown by lightning in per cent of fuses installed	0	0.12	0.06	0.34	0.13

Generally 1 per cent fuse blowing during lightning storms is considered normal under similar conditions of lightning protection.

Open circuit causes resulted from mechanical breakage of secondary service wires or wires pulling loose at connection points.

Fundamental Data Obtained on Overcurrents

Fault Resistance

The data in Figure 13 should aid in determining the assumed fault resistance to be included when calculating minimum fault currents. To obtain the full protective ability of reclosers they should be located where the minimum fault current is equal to, or greater than, the minimum pickup current of the recloser on its source side. An assumed fault resistance of 40 ohms was used in the calculations for the investigation.

Figure 13 takes into consideration the lower short-circuit currents at the substation during periods of light load generation. This affects minimum fault currents and thus must be included in the calculations.[12]

Inrush Currents Following Prolonged Outages

Inrush currents to start a high percentage of the appliance motors on a circuit can make it very troublesome in getting a circuit re-energized following a prolonged outage. The inrush currents on feeders 1 and 200 did not exceed two and one half to three times the load current and thus were not of sufficient magnitude to be troublesome. However, on several occasions it was impossible to re-energize feeder 100 through reclosers with four instantaneous openings.

Unfortunately there was no oscillograph on feeder 100 to record the currents, but undoubtedly they were higher than those recorded on feeders 1 and 200. The reclosers tripped open and, in order to establish service, had to be shunted by fuses of equivalent ratings to that used at similar points with the lock-closed recloser on feeders 1 and 200.

Division of Fault Currents in Neutral and Ground on Multigrounded Circuits

A study of many oscillograms of single-phase faults showed that 40 per cent of the fault current in the phase wire returned in the neutral conductor and 60 per cent through the ground. This was generally true regardless of the magnitude of the fault current except near the substation. A fault about a mile from the substation on the source side of the oscillograph initiated the oscillograph and recorded current traveling in the neutral away from the source to find a return path through ground.

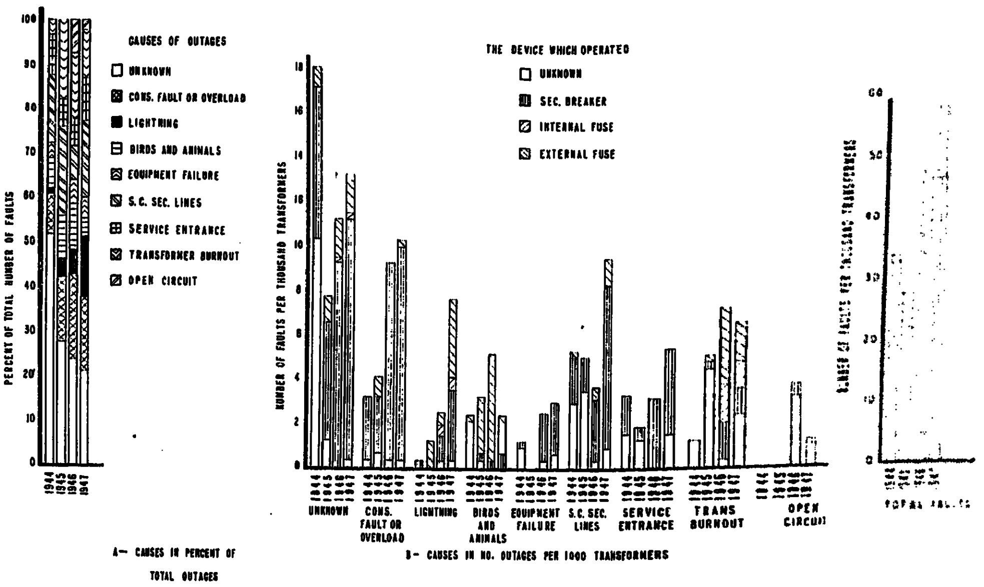


Figure 12. Outages at individual transformer installations

Conclusions

General

1. Comparison of the data on circuit conditions on the Central Electric Cooperative system with other published data indicates that this investigation was conducted on a representative rural system.

2. Three-phase systems with predominantly single-phase loads and branches can be studied and protected best if each phase is treated as a separate circuit.

3. The maximum hours outage experienced by any individual consumer varied from 1.0 to 9.8, with an average of 3.2, times the hours outage per consumer per year for that phase.

4. Blowing of primary sectionalizing or branch line fuses by lightning impulse currents, not accompanied by 60-cycle fault currents, was 5.2 per cent per year for all fuses and varied from 5 to 8 per cent for individual ratings up to 40 amperes. A lightning arrester located on each side of the line fuse undoubtedly

Table XI. Data on Number and Duration of Outages to Individual Consumers

Item	1944		1945		1946		1947	
	Min.	Max.	Min.	Max.	Min.	Max.	Min.	Max.
Data to Show Minimum and Maximum Values for Maximum Hours Outage Per Year to Individual Consumers. (With Corresponding Values for Other Items)								
1. Maximum hours outage per year to individual consumer	8.5^{1}	104.7	17.8	94.8^{2}	1.0^{3}	40.0^{5}	9.2^{6}	56.9
2. Hours outage per consumer per year	4.1	45.2	1.8	46.5	1.0	5.0	4.5	15.6
3. Ratio $\frac{\text{Item 1}}{\text{Item 2}}$	2.18	2.31	9.80^{4}	2.02	1.0	6.78	2.05	3.65
4. Number outages per year to individual consumer	3^{1}	11	4	23^{2}	1^{3}	10^{5}	4^{6}	7
5. Miles from substation	16^{1}	47	4	38^{2}	8^{3}	32^{5}	42^{6}	36
Data to Show Minimum and Maximum Number of Outages (With Corresponding Values for Other Items)								
6. Number outages per year to individual consumer	3^{1}	12	3	23^{2}	1^{3}	10^{5}	4^{6}	11
7. Maximum hours outage per year to individual consumer	8.5^{1}	93.3	24.0 18.7	94.8^{2}	1.0^{3}	40^{5}	14.4 11.7 9.2^{6}	56.8
8. Miles from substation	16^{1}	61	12 18	38^{2}	8^{3}	32^{5}	12 12 42^{6}	62
Data to Show Minimum, Maximum and Average Ratios Item 1 to Item 2								
9. Ratio $\frac{\text{Item 1}}{\text{Item 2}}$ Maximum	1.34	4.38	1.51	9.80^{4}	1.00^{3}	6.96	1.61^{6}	7.31
Average	2.45		3.64		3.14		3.31	

Superscripts $^{1, 2, 3, 4, 5}$ and ^{6} following items in any one year indicate that they are values for the same single-phase circuit.

would have reduced those percentages.

5. Mechanical breakage proved to be the greatest cause of lines falling to the ground.

6. Rapid clearing of faults reduced the number of wires burned down to a relatively small percentage.

7. The recloser or the fuse de-energized the broken or burned down line in most cases.

EFFECT OF TREE TRIMMING ON SERVICE CONTINUITY

8. The extensive program of tree trimming in the right-of-way in 1945 was undertaken before the trees reached the line level and thus showed only a slight improvement in service continuity. However, further growth, without this trimming, would have caused a serious increase in outages.

EFFECT ON SERVICE CONTINUITY OF FUSES AND LOCK-CLOSED RECLOSERS CO-ORDINATED FOR OVERLAPPING PROTECTION

9. The lock-closed recloser was shown to provide the most advantages for co-ordinated overlapping operation with several fuses in series (a) at sectionalizing points between recloser, (b) at branch junctions, (c) at sub-branch junctions and (d) at transformer installations especially of the self-protected type.

10. Such co-ordinated overlapping protection was shown to be necessary on the entire circuit in order to even approach adequate service continuity on rural distribution systems.

11. The investigation showed that fuses and lock-closed reclosers co-ordinated for overlapping protection, when applied to the entire feeder, will provide the following benefits over lines protected by reclosers only:

a. The average time to locate faults was reduced three-quarters of an hour per fault, or 60 per cent.

b. The hours outage per consumer per mile per year was reduced 70 to 90 per cent below that provided by reclosers alone.

c. The savings in restoration expense is 32.8 per cent in manhours, 26.8 per cent in mileage traveled, and 28.5 per cent in cost.

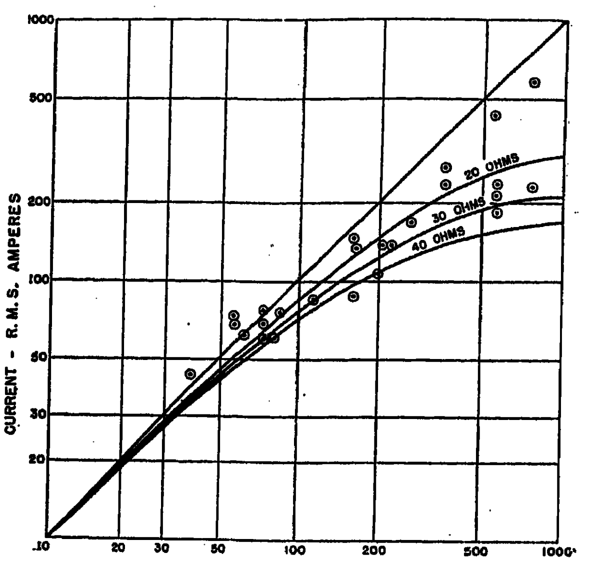
d. This conservative estimate of average yearly cost saving for restoring service will justify an investment in reclosers and fuse cutouts of $3,400 with an annual charge of 15 per cent or $12,750 with a 4 per cent charge.

This will either carry one-third of the investment, or result in a net saving of $90 per year, depending on the financing.

e. A comparison with unprotected lines or lines protected with reclosing fuse cutouts would show even greater benefits.

Figure 13. Comparison of actual fault currents with calculated maximum (copper to copper) and minimum currents using 20, 30, and 40 ohms fault resistance

⊙ Fault current measured on oscillogram



12. A service continuity standard of two hours outage per consumer per year does not appear to be possible, even with the fuse-lock-closed recloser overlapping co-ordination, where rural circuits extend 45 to 60 miles from the substation.

13. Such a standard appears to be possible where the greatest length of line is 25 miles and the overlapping fuse-lock-closed recloser co-ordination is employed.

14. Inrush currents following outages is not a problem with the co-ordinated fuses and lock-closed reclosers since the fusing can be high enough to prevent fuse blowing on inrush currents even if the recloser is locked closed. The recloser with four instantaneous openings did lock open and had to be shunted by fuses in order to restore service.

EFFECT OF OUTAGES AT INDIVIDUAL TRANSFORMER

15. The reduction of the hours outage per consumer per year, as caused by outages at individual transformer installations is very important with both conventional and self-protected types. It can be as much as 10 per cent of the total for the system when the primary line is adequately protected.

16. Outages caused by faults on the consumers premises which cause the internal secondary circuit breakers to open or primary fuses to blow, instead of being cleared by the service entrance fuses, deserve further study as this was the largest cause of transformer outages. Such a study would weigh the increased outage time and restoration costs against the effect of better protection.

17. Blowing of 2- and 3-ampere external primary fuse links at transformer installations during lightning storms varied from 0.06 to 0.34 per cent per year, with an average of 0.13 per cent.

FUNDAMENTAL DATA ON OVERCURRENTS

18. The nonpersistent faults varied from 80 to 89 per cent of the total faults with an average of 84.6 per cent.

19. The assumed 40-ohms fault resistance used in this investigation, proved to be more than ample for determining minimum fault currents and might have been reduced to 30 ohms.

20. The decrease in short-circuit currents available at the substation during periods of light load generation should be factored in calculating minimum fault currents.

21. On this multigrounded neutral circuit 40 per cent of the current returns in the neutral conductor and 60 per cent in the ground. Oscillograph data showed that the ground current travels in the neutral for several pole spans in both directions from the fault, seeking a path to ground.

References

1. LIGHTNING INVESTIGATION ON A RURAL DISTRIBUTION SYSTEM, D. D. MacCarthy, D. A. Stann, D. R. Edge, W. C. McKinley. *AIEE Transactions*, volume 68, part I, 1949, pages 428–38.

2. EXPERIENCE WITH OIL CIRCUIT RECLOSERS ON REA SYSTEMS, Lee M. Moore, Bruce O. Watkins. *Electrical Engineering* (*AIEE Transactions*), volume 62, December 1943, pages 531–35.

3. PERFORMANCE OF GROUND RELAYED DISTRIBUTION CIRCUITS DURING FAULTS TO GROUND, C. L. Gilkeson, P. A. Jenne, J. C. Davenport. *AIEE Transactions*, volume 61, 1942, pages 40–48.

POSITIVE DISCONNECTION OF DISTRIBUTION CIRCUITS DURING FAULTS TO GROUND, *Engineering Report Number 47*. Joint Sub-Committee on Development and Research, Edison Electric Institute and Bell Telephone System (New York, N. Y.), July 24, 1941.

4. TREND IN DISTRIBUTION OVERCURRENT PROTECTION, G. F. Lincks, P. E. Benner. *Electrical Engineering* (*AIEE Transactions*), volume 56, January 1937, pages 138–52.

5. SECTIONALIZING AND INTERNAL TRANSFORMER FUSING ON RURAL POWER CIRCUITS, B. O. Watkins. *AIEE Transactions*, volume 66, 1947, pages 383–88.

6. LINE SECTIONALIZING WITH INTERNALLY FUSED

Transformers, G. F. Lincks. *Distribution* (Schenectady, N. Y.), January, 1945, pages 14–16.

7. Improved Fault Protection for Rural Distribution Systems, A. Van Ryan. *Electrical Engineering* (*AIEE Transactions*), volume 64, May 1945, pages 261–63.

8. An Improved Automatic Circuit Recloser, J. M. Wallace. *AIEE Transactions*, volume 66, 1947, pages 255–58.

9. A New Relayed Automatic Oil Circuit Recloser, E. J. Casey, E. T. McCurry. *Distribution* (Schenectady, N. Y.), April 1948, pages 3–7.

10. Quicker Way of Computing Distribution Short Circuits, J. D. Sauter, G. F. Lincks. *Electrical World* (New York, N.Y.), March 21, 1942, pages 70–72.

11. Graphical Method of Calculating Fault Currents on Rural Distribution Systems, F. W. Linder. *Electrical Engineering* (*AIEE Transactions*), volume 64, January 1944, pages 16–19.

12. Short Circuit Currents and Recovery Voltages on Rural Distribution System, W. H. Eason, I. B. Johnson, J. W. Kalb, H. A. Peterson. *AIEE Transactions*, volume 68, part II, 1949, pages 930–38.

13. Coordinating Fuses for Distribution Reliability, G. F. Lincks. *Electrical World* (New York, N. Y.), July 29, 1933, pages 140–43.

14. Advances in Technique of Lightning Measurements, T. Brownlee. *AIEE Transactions*, volume 68, part I, 1949, pages 515–19.

15. Farm Line Protection Presents a Difficult Coordination Problem, C. F. Dodson. *Electric Light and Power* (Chicago, Ill.), June 1946, pages 80–82.

16. Keeping Down Costs for Service Continuity, W. J. Lyman. *Electrical World* (New York, N. Y.), July 13, 1940, pages 50–53, 106.

17. Relative Value of Different Types of Overcurrent Protection for Distribution Circuits, G. F. Lincks. *Electrical Engineering* (*AIEE Transactions*), volume 61, January 1942, pages 19–26.

18. One-Shot Reclosers Improve Service Stop Complaints, T. H. Kinnear, L. R. Patterson. *Electrical West* (San Francisco, Ill.), September 1937, pages 63–64.

19. Reclosing Fuses Reduce Outages on Rural Lines 61 Per Cent, C. C. Cornelius. *Electric Light and Power* (Chicago, Ill.), February 1937, pages 32–33.

20. Service Continuity Standards Aid Economical Distribution Planning, W. J. Lyman. *Bulletin* Edison Electric Institute (New York, N. Y.), September 1947, pages 325–29.

21. The Application of Oil Circuit Reclosers to Rural Distribution Systems, R. O. Askey, C. V. Miller. Midwest Power Conference (Chicago, Ill.), April 8, 1948.

22. Relative Expense for Service Restoration with Different Types of Overcurrent Protection on Distribution Systems, G. F. Lincks, C. R. Craig. *Electrical Engineering* (*AIEE Transactions*), December 1942, volume 61, pages 821–31.

23. Field Tests of Oil Circuit Recloser Substantiate Analytical Coordination Method, G. G. Auer, R. A. Branflick, L. J. Woodward, W. C. McKinley. *AIEE Transactions*, volume 68, part II, 1949, pages 801–10.

Discussion

C. E. Asbury (The Commonwealth and Southern Corporation, Jackson, Mich.): This paper tabulates and summarizes some very useful data for rural systems such as outage times per customer per year, the ratio of persistent to nonpersistent faults, the number of transformer failures, classification of various types of faults experienced, and so forth.

The necessity of good service continuity to rural systems is being realized more and more since the agriculturist of today depends very heavily upon his electrical servant to do many of his farm chores. The paper mentions average consumption per consumer for rural areas in that region to be in the order of 2,000 to 2,500 kilowatthours per year. Recent reports from some midwestern states with rather large rural areas show annual energy consumption in the order of 3,000 to 4,000 kilowatthours per farm customer. It is obvious that loads of these magnitudes must be given dependable service.

It should be kept in mind that all comparisons made for automatic circuit recloser performance are based on four instantaneous trips versus two instantaneous plus locked-closed operation with co-ordinated fuses.

Three overcurrent protective methods were considered for this particular application:

1. Use of automatic circuit reclosers having three instantaneous openings. We have shared the findings of the authors in attempting such applications and also find it is practically impossible to obtain any degree of co-ordination between such devices and fuses.

2. Reclosers having two instantaneous openings followed by two time-delay openings to lockout were considered but were not used for three reasons listed in the paper:

(a) It presented a problem of co-ordination with fuses on the source side. This difficulty is especially troublesome with certain makes of reclosers and fuses and emphasizes the desirability of recloser and fuse characteristics being co-ordinated in design to parallel each other. The so-called fast type of fuse having steep slope on the time current characteristics are especially troublesome in this co-ordination problem. Also, in general, fuses which are designed to melt in five minutes with current of 150 per cent rating do not co-ordinate as well with reclosers and fuses having a melting time of five minutes at the 200 per cent rating. Using fuses with different ampere ratings in the two designs to give equivalent time current characteristics is not always satisfactory because these fuses do not have the same load ability.

(b) It is not clear just what the authors mean by item 2(b) of Table III of the paper when they say sectionalizing between reclosers generally must be done with more costly reclosers. It does not appear to be good practice to consider inserting fuses between reclosers for sectionalizing in the main trunk line. In most applications, reclosers are used out on the rural line away from the substation and a station circuit breaker is used in the substation with fuses on the high side of the transformer. In the smaller rural stations, reclosers especially in the larger capacities are being used in place of the conventional circuit breakers for line protection. In such applications it is, of course, necessary to allow for the cumulative heating on the high side fuse due to recloser operations before lockout. This problem is not particularly new in co-ordination since a similar co-ordination factor must be taken into consideration where automatic reclosing conventional circuit breakers are to be co-ordinated with high side fuses.

(c) Item 2(c) in Table III states that in some cases the line fuses were too small to co-ordinate with internal transformer fuses for reclosers when two instantaneous and two time-delay shots were used. As pointed out in another section of the paper, some transformers using internal fuses are fused with rather large fuses in the order of 15 to 20 amperes which provides no protection for the transformers and such fuses are so large that it places a very definite handicap on applying sectionalizing fuses in a rural system. This is one of the disadvantages of using transformers with internal fuses. This is especially true on systems which may be electrically long where the short-circuit current may be low at the remote end of the line. With ordinary 3-kva transformers having internal fuses it is necessary that a minimum sectionalizing fuse of 25 or 30 amperes be used near the end of the circuit. Obviously, this limits the number of sectionalizing points that may be used on such a circuit and it also results in very large fuses near the station. Where conventional external fuses are used for transformers a minimum sectionalizing fuse of 15 amperes can be utilized which co-ordinates nicely with recloser applications especially when reclosers and fuses are selected having similar slopes in their time current characteristics.

Mention was made in the paper of sectionalizing fuses as well as two or three ampere transformer fuses blowing on lightning surges. It would be helpful if the authors would point out the type and characteristics of the fuses used in this particular application. Apparently, the transformer fuses were not of the newer design known as the high-surge type. Fuses classified as "fast" type are more susceptible to this type of operation than the so-called "slow" fuses. Fuses designed to have a melting time of 300 seconds with 150 per cent of rated current have much less surge ability for the same ampere ratings than do fuses designed to operate at the same time with 200 per cent of rated current. The proposed Edison Electric Institute–National Electrical Manufacturers Association (EEI–NEMA) Standard for universal fuse links is based upon all fuses being designed to have a melting time of 300 seconds at 200 per cent of rating. This should reduce considerably fuse blowing on lightning surges. Fuses designed to have characteristics within the limit described by the proposed EEI-NEMA Standard Curve *T* should present much less difficulty in blowing due to lightning surges than the so-called fast fuses described as *K* by the Standard.

The paper tabulates some comparative savings in outage time, replacement costs, traveling time, and man-hours. Again, it must be kept in mind that these comparisons are based on recloser operation with two instantaneous and locked closed performance. It is not clear in the paper on just what basis this comparison is made. Apparently, it is a comparison between two instantaneous and locked-closed recloser operations as compared with four instantaneous recloser operations or with the circuit being protected by single shot or possibly reclosing fuses. It would be helpful if the authors would state with which of the three possible comparisons the locked-closed recloser operation was compared. It must be kept in mind that such a comparison does not reflect what might be expected when using reclosers having one or two instantaneous operations followed by two time-delay operations.

The curves in Figure 13 showing the comparison of actual fault currents as compared to the calculated values with various values of resistance supplies interesting information. It is not clear just what unit the abscissa of the curve represents. How was the actual fault location for these plotted points established? It is very important that the actual location be known quite accurately in order that a misinterpretation as to fault impedance not be obtained.

It is difficult to arrive at a reasonable value of fault impedance for calculations on lower voltage systems. If too high a value of fault impedance is used, the application of protective equipment is made particularly difficult and is likely to result in applying a large number of sectionalizing devices. This is especially true as the system voltage is decreased. A fault resistance of 40 ohms was assumed for this particular investigation. If 40-ohms fault impedance is applied to systems having line-to-line voltages of 7,200 volts and less, the reach of protective devices is very short. It appears that in order to make reasonable applications of overcurrent protective devices a sliding scale needs to be used in the selection of fault impedances with lower values of fault impedance being selected for the lower voltages. It would be interesting if the authors would point out their practice in this respect and state the value of fault impedance used in lower voltage systems, such as 7.2, 4.16, and 2.4 kv.

It appears that feeders number 1 and number 200 shown in Figure 3 of the paper are considerably overburdened with sectionalizing devices. Not only does this represent an appreciable investment but is entirely too many sectionalizing devices to be co-ordinated adequately in series. On some of these feeders it appears necessary to co-ordinate 10 to 12 sectionalizing devices in series between the substations and the end of the feeders.

The information outlined in this paper on performance of reclosers having four instantaneous trips or two instantaneous trips and locked-closed operation with the particular fuses used in the application should be useful to the industry in making applications of similar equipment. It would be helpful if further data could be obtained from similar studies where modern reclosers having one or two instantaneous followed by one or two time-delay openings were co-ordinated with fuses having characteristics similar to the proposed EEI-NEMA Standard, especially with transformers being fused with external primary fuses of the high-surge type or with links having similar characteristics to that used for sectionalizing purposes.

G. F. Lincks, D. R. Edge, W. C. McKinley, J. H. Leh: Mr. Asbury has presented some interesting and helpful comments on our paper. As he points out, it is important to keep in mind that all comparisons of both the service continuity and the restoration expense data, were based on reclosers having four instantaneous openings with no fuse sectionalizing versus fuses co-ordinated with reclosers having two instantaneous plus a locked (or hold)-closed operation. However, it should be appreciated that equal improvements in service continuity and in reduction in restoration expense would be obtained if it were economically and technically practical to install reclosers having four instantaneous openings at all the locations where fuses and reclosers were employed in the investigation. This would hold true for any overcurrent protective equipment or combination of equipments (such as co-ordinated fuses and reclosers with two instantaneous plus two time-delay operations or reclosers with isolators or sectionalizers), provided that in themselves or co-operatively, they clear a majority of all temporary faults with only a momentary outage and isolate all persistent faults to equally short sections of the lines. The savings discussed in our paper show economic justification for the co-ordinated fuse-lock-closed recloser combination. However, these savings did not provide much margin for substituting more costly equipment, for the low cost fuses. If, for economical or technical reasons, fewer sectionalizing points are protected than we employed, the benefits in improved service continuity and in restoration expense shown by this investigation will be decreased proportionately, as is shown superlatively by the comparisons made in the investigation. As pointed out in our paper, technical difficulties would have necessitated fewer sectionalizing points with the instantaneous plus time-delay reclosers. This led to the choice of the instantaneous plus lock-closed reclosers.

If the co-ordinated fuse-lock-closed recloser combination had been compared with fuses alone (no reclosers) even greater benefits would have been shown over those given in the paper. The mathematical studies in references 17 and 22 of our paper indicate such a comparison would show only a slight change in the service continuity, but a very marked change in the restoration expense. Without the recloser to protect the fuses, they would be blown by both temporary and permanent faults causing outages to only a few customers on each branch, but necessitating many extra trips to restore service. Reclosing fuses would approach the advantages of the fuse-lock-closed recloser combination to the extent that manual replacement of fuses blown by temporary faults approaches the automatic resetting of the recloser in eliminating prolonged outages. Maintenance cost, however, probably would be prohibitive.

Mr. Asbury questions the use of fuses between reclosers for sectionalizing main trunk lines or long branches. Again this is an economic consideration. The orbit of protection (or "reach") of a recloser is bounded by the points on the main line and connected branches where the minimum fault current (limited by line impedance and fault resistance at the time of minimum generation on the system supplying the distribution circuit) equals the minimum pick-up (or tripping) current of the recloser. On a 7.2/12.5-kv multigrounded Y-circuit, the orbit can reach out 15 to 25 miles. Obviously, sectionalizing is necessary between reclosers located 15 to 25 miles apart. The function of a recloser or a fuse at this additional sectionalizing point is to isolate persistent faults. An additional recloser located within the orbit of protection of another recloser offers practically no additional benefits in clearing non-persistent faults. Thus, why pay for a recloser when a fuse will do the job equally well at one-tenth the cost. The lock-closed recloser permits the use of a fuse whereas the instantaneous plus time-delay recloser necessitates the use of a recloser. With the instantaneous-time-delay reclosers, a fuse at this additional point, which would have the correct rating to co-ordinate for the overlapping function with the recloser on its source side, would be too large to permit the recloser on its load side to lock open before the fuse was partially melted.

As to the overburdening of feeders 1 and 200 with sectionalizing devices, nearly five years operating experience has proved that:

1. The system functions successfully,
2. Any less number of devices would not provide the same improvement in service continuity or savings in restoration expense, and
3. These savings will pay from one-third to more than all the yearly carrying charges on the investment depending on the financing. To this would be added the savings from loss of revenue while the circuit is out, plus those chargeable to consumer good will.

There were a maximum of nine primary line sectionalizing devices connected in series. Seven of these installations are normal. Two installations are abnormal; one because the two main feeders branch off from a common feeder a mile from the substation, and the other, because an inaccessible portion of the line had to have a device at each end to save unnecessary hunting for troubles on snow shoes during $4^1/_2$ months each year. The furthest transformer was 67-line miles from the substation so the normal sectionalizing was approximately only one installation every ten miles of line. Shortening the lines, as suggested in our conclusions, would reduce the number of sectionalizing devices in series while permitting the same coverage.

Mr. Asbury suggests that fuse links on the source side of reclosers which melt at 200 per cent rated current in five minutes co-ordinate better with the reclosers than do fuse links which melt at 150 per cent rated current, such as were used in our investigation. Also, he indicates benefits would be obtained in reducing unnecessary lightning blowing. He is correct in these statements if he compares the fuse links only on the basis of the ratings, since then the 200 per cent rated links have a larger physical size and melt at higher currents throughout the whole range of time-current characteristics. However, they will not provide as good protection to connected apparatus. The 5-minute (300-second) melting current of a fuse link governs the overcurrent protection it affords connected apparatus and thus should be the basis of comparison between different fuse links. The rating is the maximum current the fuse link will carry continuously without causing excessive heating of the fuse cutout in which it is installed. The speed ratio (0.1 to 300 second melting currents) of fuse links having similar 300 second melting currents, governs the ability to co-ordinate with relays, reclosers, and other fuse links. This is true regardless of whether the fuse link is rated to carry continuously, say for example 25 amperes with the 200 per cent basis of rating or 40 amperes with the 150 per cent basis of rating. It may not be possible to apply the 25-ampere link where the 40-ampere link is applicable unless the 25-ampere link can carry that much more current than its rating. Similarly, the speed ratio, and not the basis of rating, affects the relative lightning blowing characteristics of different fuse links since such blowing involves high currents and short times. The speed ratio is a product of the physical design of the fuse link. It is not affected by the basis of rating except where higher melting temperature fusible metals, requiring the 200 per cent basis of rating, may handicap the designer in providing a desired higher speed ratio; the slow time-current characteristic which Mr. Asbury states, and we agree, may be beneficial.

The fuse links employed in our investigation have a speed ratio of from six to seven. The standard for universal fuse links currently being proposed by the Edison Electric Institute Subcommittee on Distribution Fuse Standardization, to which Mr. Asbury refers, have a speed ratio of six to eight for the *K* fast fuse links, and 10 to 13 for the *T* slow fuse links. Thus, the fuse links used in this investigation have practically the same ability, to co-ordinate with reclosers and the same lightning blowing characteristics as the *K* fast fuse links currently being proposed by the Edison Electric Institute, if compared on the basis of the 300-second melting currents. The fuse links, employing a tin fusible element as used in our investigation, are rated to carry more current continuously than it is possible to rate fuse links in the proposed standard, which also must include fuse links employing the higher melting temperature silver and copper fusible elements.

The lightning blowing of the 2- and 3-ampere fuse links used at conventional transformer installations in our investigation never exceeded one-tenth of one per cent of those installed, in any one of the four years. One to two per cent fuse blowing during lightning storms is considered normal on most systems having the lightning arrester ground interconnected with the secondary neutral. Thus, the margin of possible benefit was too small to warrant including Hi-Surge type of fuse link in our investigation.

Time-delay, time-current characteristics paralleling fuses would be desirable in reclosers, as suggested by Mr. Asbury, if co-ordination with source side fuses was the only problem. Also, it would be beneficial in co-ordinating the instantaneous opening of reclosers so as to prevent partial melting of load side fuses by non-persistent faults. However, parallel fuse and time-delay recloser characteristics starting directly from the minimum pick-up current permit co-ordination with only one or two adjacent ratings of load side fuses that also could be protected by the instantaneous opening of the recloser. A wider range of ratings was found necessary in order to fuse sectionalizing points between reclosers, branches, and sub-branches connected in service. Thus the time-delay characteristics of reclosers having two instantaneous plus two time-delay operations to lock out must be a compromise between the opposing characteristics which are desirable for co-ordination with source side and with load side fuses. The ability of the lock-closed recloser to eliminate the time-delay characteristic and all the co-ordination problems entailed therewith led to its choice as the best recloser function for our investigation.

With reference to the questions on Figure 13, the abscissa is the calculated maximum (copper to copper) fault current. The automatic oscillographs recorded the date and time each record was made so the reading could be related to the faults as reported by the trouble crews. The data were included only when the fault location was determined accurately. We know of no similar data which is available for other systems. However, it would appear that the fault resistance would be a function of the type of line construction or fault conditions rather than the voltage, except possibly as voltage effects the length of the short circuit path between conductors. Additional field data on this subject are very desirable, both on 7.2-kv and lower voltage circuits.

Short-Circuit Currents and Recovery Voltages on Rural Distribution Systems

W. H. EASON
MEMBER AIEE

I. B. JOHNSON
MEMBER AIEE

J. W. KALB
ASSOCIATE AIEE

H. A. PETERSON
FELLOW AIEE

THIS paper deals with the nature of short-circuit currents and of the recovery voltages that can occur on multigrounded neutral rural distribution systems. This information is useful in the design, application, and preparation of test standards of lightning arresters, fuse cutouts, oil circuit reclosers, and other protective devices for these rural systems.

The results were obtained from field tests made on an actual system and from calculations and analysis using the transient analyzer[1] or miniature system method. The transient analyzer studies were made primarily to analyze the most severe or boundary conditions obtainable. The field studies were necessarily limited in the type of circuit conditions, and represent more typical operating conditions. Several sets of field operating conditions were duplicated on the transient analyzer for purposes of comparison.

In so far as the authors are aware, this paper presents the first published data concerning measured values of recovery transient voltages on a rural distribution system and their correlation with transient analyzer measurements.

Fault currents and recovery voltages were studied on various combinations of single-phase and 3-phase circuits that included the effects of the angle of incidence of the fault on the voltage wave, of the location of the fault on the line, of the length of line, of the transformer rating, and of the type of fault—single line-to-ground or 3-phase.

The results are presented and discussed in three parts: transient analyzer study; field study; and comparison of field investigation with analyzer results and calculations. The analyzer study presents data from a representation in miniature of a multigrounded neutral rural distribution system, and the field study includes data from an actual 7,200/12,470-volt multigrounded-neutral wye rural distribution system.

Summary of Results and Conclusions

Some factors which affect the severity of transient recovery voltages experienced on rural distribution lines are:

1. The length of line remaining connected to the substation after the fault interruption. The rate of voltage rise decreases with increasing length of line. Whereas, in general, the crest recovery voltage appears to be relatively low with no line remaining connected after the fault, it increases to a maximum as line is added and then decreases with further increases in length of line.

2. Distance between substation and fault. For single, unbranched lines the crest recovery voltage generally decreases with increasing distance between substation and fault.

3. X/R ratio of the line between substation and fault. Crest recovery voltage decreases with decreasing X/R ratio of the line.

4. Arc voltage characteristics of the fault clearing device. A sharp rise in arc voltage immediately before arc extinction tends to increase crest recovery voltage of the transient.

5. Complexity of system network. Reflections from branch points and ends of branch lines influence the nature of the transient recovery voltages.

6. Magnitude of system load connected after interruption of fault current. The damping effect of load resistance reduces the magnitude of crest recovery voltages.

7. Kilovolt-ampere rating of substation transformer bank. Rate of voltage rise increases with increasing transformer kilovolt-amperes. Crest recovery voltages tend to decrease with increasing transformer kilovolt-amperes.

8. Lumped capacitance between line and ground such as substation bus structure or power factor capacitors. This will reduce the rate of voltage rise. An increase in capacitance up to certain values is accompanied by an increase in the crest recovery voltage.

The most severe recovery voltages measured on the transient analyzer were obtained on a study involving six miles of unloaded, unbranched line. This length of line was selected because shorter lengths of line, while giving more rapid rates of voltage rise, would generally result in lower crest voltages. Table I lists measurements obtained on this study for various values of substation kilovolt-amperes.

The shortest measured time to 0.9 times normal crest voltage, $T_{0.9}$, for either field or analyzer tests, was 22 microseconds. This was measured during field tests at the 900-kva substation bank with no connected line.

Crest recovery voltage, V_M, was 0.94 times normal.

The highest crest recovery voltage,

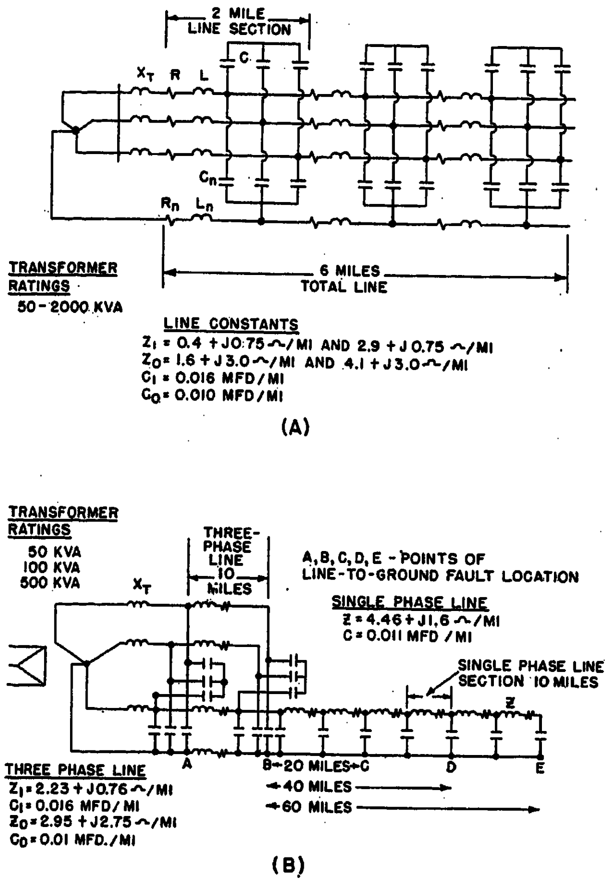
Table I

Substation Transformer, 3-phase Kva	V_M Crest Recovery Voltage, Times Normal Crest Voltage	$T_{0.9}$ Time to 0.9 Normal Crest Voltage, Microseconds
50	1.75	280
100	1.70	230
500	1.55	110
1,000	1.43	85
2,000	1.28	80

Paper 49-177, recommended by the AIEE Transmission and Distribution Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20–24, 1949. Manuscript submitted November 15, 1948; made available for printing May 2, 1949.

W. H. Eason and J. W. Kalb are with the General Electric Company, Pittsfield, Mass., I. B. Johnson is with the General Electric Company, Schenectady, N. Y., and H. A. Peterson is with the University of Wisconsin, Madison, Wis.

The authors desire to acknowledge the work of E. J. Wade, D. D. MacCarthy, G. F. Lincks, P. H. Light and D. Inwood. The field tests were made possible by the co-operation of the personnel of the Central Electric Cooperative, Inc.



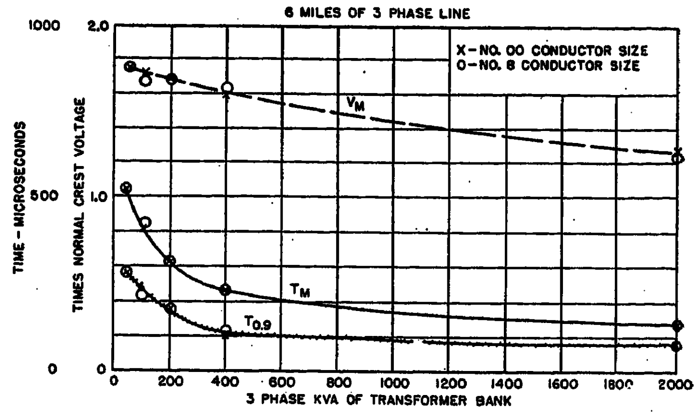


Figure 1 (left). Representative circuits in miniature for 6,900-volt rural distribution systems.

Figure 3 (above). Effect of kilovolt-ampere rating of substation transformer on recovery voltage characteristics

Six miles of line connected to substation
Miniature system data

V_M, measured during field tests was 1.33 times normal.

Comparison of field measurements and transient analyzer results for similar circuit conditions show good agreement, demonstrating the accuracy of the transient analyzer in securing data on recovery voltages for rural distribution systems. Its value lies in the ability to extend circuit conditions beyond those found readily in the field.

Short-circuit currents measured in the field agree with values obtained by simplified fault calculators.[3,4]

Transient Analyzer Study

In this study the systems used in miniature represented 6,900-volt multigrounded neutral rural distribution systems. Substation transformer bank ratings of 50 to 2,000 kva were considered with the high voltage side of these transformers assumed connected to an infinite bus. The transformer reactances were taken as 5.5 per cent on their own base. Neither the effect of other transformers than the substation transformer nor the effect of capacitors for power factor correction were investigated. Except for special cases to be discussed later, no system load was represented in the miniature system.

The systems in miniature used in the analyzer studies were single radial lines—3-phase and/or single-phase—emanating from the substation transformer with no branch lines connected thereto. These circuits are shown in Figure 1. In Figure 1(A) the 3-phase line was built up in 2-mile sections to represent a total of six miles of line. The line constants for this circuit are given in the figure where it is indicated that two values of positive sequence or line resistance were considered, covering a range of line conductor sizes from the order of number 00 to number 8 American wire gauge. In Figure 1(B) the 3-phase and single-phase lines were built up in 10-mile sections to represent line lengths up to 70 miles. The line constants shown in this figure were based on a rural distribution system in which the single-phase lines, as well as the 3-phase lines and the neutral were number 6 copper, and in which spacings of 36 inches between the phase and neutral wires and between the phase wires were assumed.

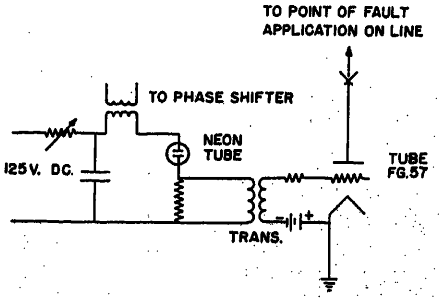


Figure 2. Circuit for producing fault initiation and fault interruption in miniature system

The device used for the initiation of a fault and the removal of the fault at the first subsequent current zero, simulating the operation of an expulsion lightning arrester or a transformer fuse, is shown in Figure 2. The voltage drop across the *FG-57* tube simulating arc voltage during the period of fault current flow was made approximately zero for the tests made using the circuit of Figure 1(B). For the tests made with the circuit of Figure 1(A) no attempt was made to eliminate tube voltage drop. The device was connected to various points on the distribution lines of Figure 1 wherever it was decided to determine the nature of the recovery voltage as a consequence of the application and removal of a line-to-ground fault at that point.

In the operation of lightning arresters and transformer fuses, the fault current can be initiated at any instant relative to the 60-cycle fundamental frequency voltage. For an analytical treatment of the effect of initiating angle and circuit X/R on recovery transient severity, see Appendix I.

On the transient analyzer the angle of incidence of the fault was varied to give the maximum severity in recovery voltage, and these recovery voltage characteristics were expressed in terms of: (a) the maximum crest recovery voltage, V_M, that appears across the protective device following its operation; (b) the time, T_M, to reach crest of this transient recovery voltage; and (c) the time, $T_{0.9}$, for this recovery voltage to reach 90 per cent normal system crest voltage.

This value, $T_{0.9}$, was chosen arbitrarily in the absence of a universally accepted definition of initial rate of voltage rise. In instances where arc drop was present, T_M and $T_{0.9}$ were measured from the instant the recovery transient crossed the zero voltage axis.

In Figure 3 are shown the values of V_M, T_M, and $T_{0.9}$ as a consequence of fault current interruption at the terminals of the substation transformer. These values are for the 6-mile length of circuit, Figure 1(A), and for 3-phase transformer bank ratings of 50 to 2,000 kva. The condition of a fault at the transformer terminals with short lengths of connected line would be expected to give the most severe transient recovery voltages. These results indicate that V_M decreases with increasing kilovolt-amperes of the transformer bank from 1.75 times normal for a 50-kva bank to 1.28 times normal for a 2,000-kva bank. Values of T_M and $T_{0.9}$ decreased with increasing kilovolt-amperes of the transformer bank and reached values of 140 and 80 microseconds respectively for a 2,000-kilovolt-amperes bank.

To study the effect of longer lengths of line remaining connected after fault clearing and variation in fault locations, the circuit of Figure 1(B) was used. The results are shown in Tables II and III. Some of these results also are presented graphically in the curves of Figures 4, 5, and 6. The data of Tables II and III for remaining connected lines of 10-mile lengths or greater, show the following:

1. Maximum crest voltage, V_M, decreases as the number of miles of remaining connected single-phase line increases. For a fault at the terminals of a 500-kva bank, V_M for 60 miles of connected line is about 84 per cent of that for 20 miles of connected line.
2. Maximum crest voltage, V_M, decreases as the distance between the fault and the substation transformer increases. For a 60-mile line supplied through a 500-kva bank, V_M for a fault at the end of the line is 69 per cent of that for a fault at the transformer terminals.
3. In general, T_M and $T_{0.9}$ increase as the distance between the fault and the step-down transformer is increased. For a 60-mile length of remaining connected line supplied through a 500-kva bank, $T_{0.9}$ for a fault at the end of the line is 1,530 per cent of that for a fault at the substation.

It was found that the addition of single-phase line in the two unfaulted phases had very little effect on the recovery voltage of the faulted phase. This was because the only between-phase mutual coupling in the miniature system studied exists in the short 10-mile section of 3-phase line. Therefore, any unbalance

Table II. Characteristics of Recovery Transients—Miniature System Study

Single-Phase Line Only

Fault Location, Miles from Substation	Kilovolt-Amperes of Transformer Bank	Miles of Line Connected	V_m	T_M, Microseconds	$T_{0.9}$, Microseconds
0	50	20	1.66	810	420
		40	1.60	1,200	610
		60	1.50	1,600	800
	100	20	1.62	600	240
		40	1.50	460	410
		60	1.40	1,000	600
	500	20	1.54	320	150
		40	1.39	600	150
		60	1.32	800	150
20	50	20	1.50	900	500
		40	1.53	1,220	720
		60	1.46	1,600	860
	100	20	1.25	700	460
		40	1.32	1,000	600
		60	1.28	1,280	800
	500	20	1.02	3,000	1,700
		40	1.01	2,900	1,700
		60	0.93	2,600	2,300
40	50	40	1.36	1,420	820
		60	1.39	1,610	950
	100	40	1.22	2,900	760
		60	1.15	1,300	920
	500	40	1.00	3,900	1,800
		60	0.90	2,600	2,500
60	50	60	1.28	1,800	1,100
	100	60	1.02	1,400	1,100
	500	60	0.90	2,600	2,300

V_M = Maximum recovery voltage. Times normal crest voltage.

T_M = Time to maximum recovery voltage V_M.

$T_{0.9}$ = Time for recovery voltage to reach 90 per cent of normal crest voltage.

Table III. Characteristics of Recovery Transients—Miniature System Study

With Ten Miles of 3-Phase Line Between Substation and Single-Phase Line

Fault Location, Miles from Substation	Kilovolt-Amperes of Transformer Bank	Total Miles of Line Connected	V_M	T_M, Microseconds	$T_{0.9}$, Microseconds
0	50	10	1.63	700	400
		30	1.65	1,100	580
		50	1.60	1,400	680
		70	1.52	1,800	960
	100	10	1.58	500	260
		30	1.58	800	400
		50	1.46	1,000	540
		70	1.42	1,200	600
	500	10	1.50	280	140
		30	1.40	440	240
		50	1.35	700	260
		70	1.35	960	270
10	50	10	1.60	640	340
		30	1.61	1,100	540
		50	1.58	1,400	720
		70	1.50	1,800	920
	100	10	1.47	520	280
		30	1.50	900	480
		50	1.46	1,100	600
		70	1.38	1,300	800
	500	10	1.02	2,600	300
		30	1.05	2,400	400
		50	1.02	2,100	640
		70	1.00	2,800	900
30	50	30	1.48	1,300	740
		50	1.50	1,600	900
		70	1.50	1,900	1,040
	100	30	1.18	900	600
		50	1.25	1,260	800
		70	1.25	1,540	1,000
	500	30	1.00	2,500	1,600
		50	1.00	2,200	1,900
		70	1.00	3,000	2,400
50	50	50	1.38	1,600	900
		70	1.40	2,000	1,100
	100	50	1.10	1,400	1,000
		70	1.13	1,600	1,120
	500	50	0.97	2,300	2,000
		70	1.00	3,000	2,440
70	50	70	1.32	2,000	1,200
	100	70	1.07	1,800	1,280
	500	70	1.00	3,000	2,400

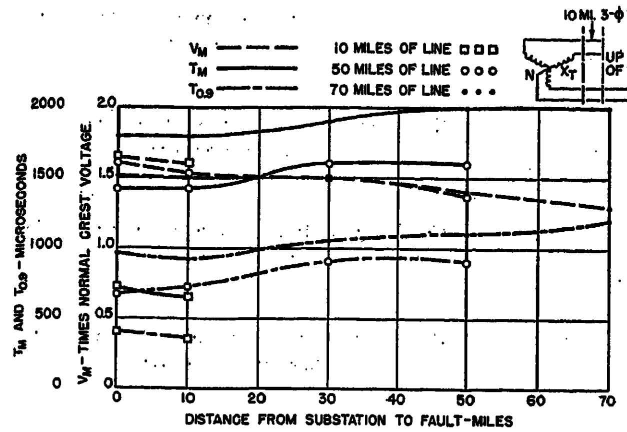


Figure 4. Effect of distance between fault location and substation on recovery voltage characteristics

50-kva transformer at substation. Miniature system data

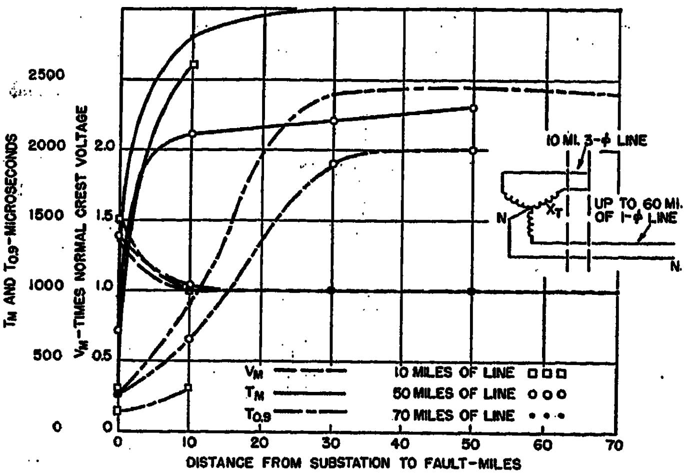


Figure 6. Effect of distance between fault location and substation on recovery voltage characteristics

500-kva transformer at substation. Miniature system data

between phases would not be expected to change the recovery voltage characteristics on any one phase.

Field Investigation

Description of System

This study was made as part of a comprehensive field investigation[5,6] in cooperation with the Central Electric Cooperative, Inc., in western Pennsylvania, on their 7,200/12,470-volt multigrounded wye rural distribution system. Figure 7 is a map of that portion of the system and substation bank upon which these tests were made.

The 3-phase substation transformer bank is rated 900-kva, 4.01 per cent impedance.

As Figure 7 shows, the system basically consists of long single-phase lines branching from 3-phase feeders. Forty-two miles of 3-phase, and 524 miles of single-phase 7,200-volt line were involved.

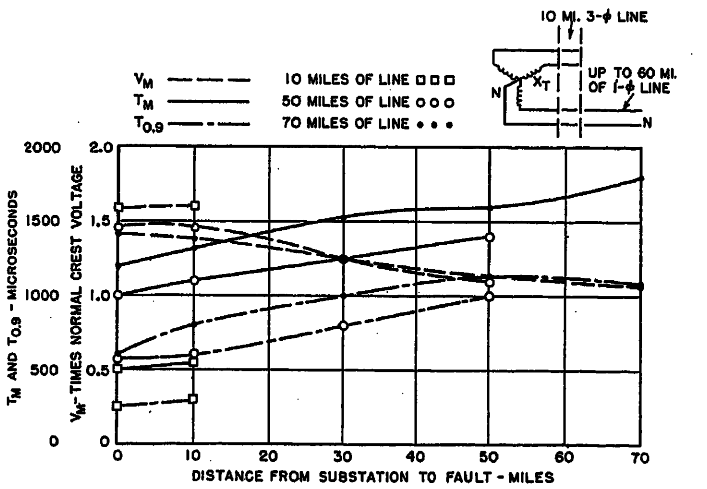


Figure 5 (left). Effect of distance between fault location and substation on recovery voltage characteristics

100-kva transformer at substation. Miniature system data

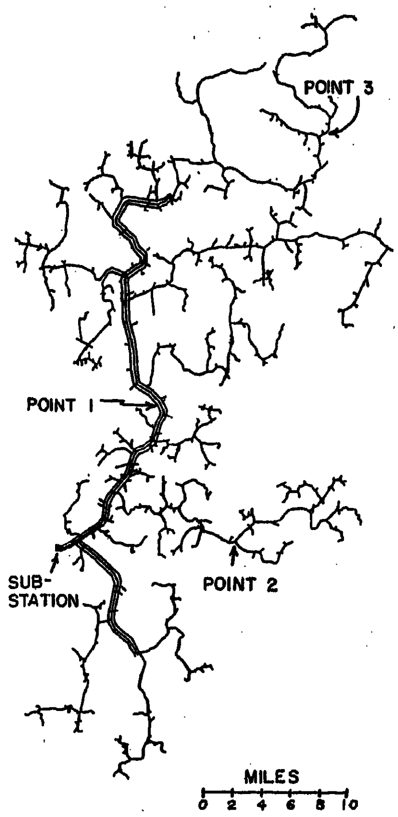


Figure 7 (right). Map of 7.2-kv multigrounded lines upon which field tests were made

Table IV. Characteristics of Recovery Transients Measured During Field Tests

Test Location	Miles from Substation	Type of Fault, Number of Phases	Fault Cleared by	Miles of Line Connected After Fault on Particular Phase Involved	Fault Current, Average Amperes Rms Symmetrical	Characteristics of Most Severe Recovery Transients Recorded V_M	T_M	$T_{0.9}$	Clearing Angle, Degrees
Substation	0	3	Sectionalizing fuse	0	748	0.95	30	28	73
Substation	0	3	Line-to-ground fuse	241	748	1.20	1,000	360	57
Substation	0	1	Sectionalizing fuse	0	817	0.94	25	22	53
Substation	0	1	Line-to-ground fuse	289	817	1.15	1,480	340	55
Point 1	16	3	Sectionalizing recloser	29	270	1.33	550	190	35
Point 1	16	3	Line-to-ground fuse	289	270	1.32	1,520	670	41
Point 1	16	1	Sectionalizing recloser	29	220	1.09	660	260	50
Point 1	16	1	Line-to-ground fuse	289	220	1.10	1,640	880	49
Point 2	18½	1	Line-to-ground fuse	241	130	1.00	570	340	59
Point 3	52½	1	Sectionalizing recloser	215	56	1.03	1,360	980	50
Point 3	52½	1	Line-to-ground fuse	289	56	1.14	1,570	1,030	65

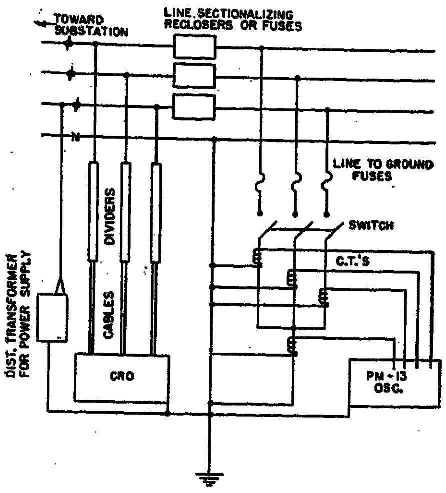
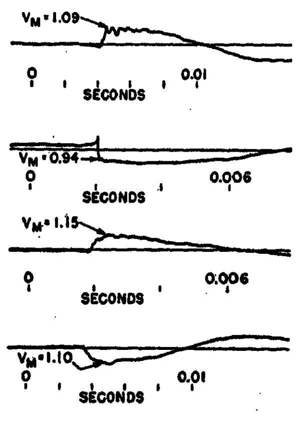


Figure 8. Equipment for making short-circuit current and recovery voltage field tests



(A) Phase C fault at point 1 cleared by sectionalizing recloser

(B) Phase C fault at substation cleared by sectionalizing fuse

(C) Phase C fault at substation cleared by line-to-ground fuse

(D) Phase C fault at point 1 cleared by line-to-ground fuse

(E) Phase C fault at point 3 cleared by line-to-ground fuse

Figure 10. Oscillograms of voltage transients obtained during field tests

Method of Test

Faults were produced at the substation and at points designated by 1, 2, and 3 on Figure 7. Figure 8 illustrates the method of applying faults and of measuring currents and recovery voltages. The 3-element moving film cathode-ray oscillograph[2] was used to measure the transient recovery voltages at fault clearing. The General Electric *PM-13* automatic magnetic oscillograph[2] measured phase and neutral currents at the fault. A second *PM-13* magnetic oscillograph located near the substation recorded phase and neutral currents (normal load as well as fault currents) and two of the 3-phase voltages.

Faults were initiated by means of a switch which was capable of simultaneous closing in the case of 3-phase faults. Fault current was interrupted either by blowing of line-to-ground fuse, or opening of line sectionalizing device, usually an oil circuit recloser. In this case the remaining lengths of connected line affecting recovery voltages were those on the substation side of the fault location since the line beyond the fault was disconnected by the sectionalizing device.

Test Conditions

At the substation and at Point 1 of Figure 7, both single-phase and 3-phase faults were produced. At Points 2 and 3 only single-phase faults could be produced. Table IV summarizes the conditions associated with each of the field tests.

Approximate load currents at the substation preceding initiation of faults were:

Phase *A*: 23 rms amperes
Phase *B*: 10 rms amperes
Phase *C*: 25 rms amperes

The remaining lengths of connected line shown in Table IV are composed of moderately long branches together with many short spurs as shown on Figure 7. The farthest point from the substation (3-phase + single-phase line) on the lines studied is 68 miles.

Results and Conclusions

Table IV shows the average recorded fault currents and the characteristics of the most severe recorded recovery transients associated with each test condition. These results indicate:

1. Fault current decreases as expected with increase in distance between fault and substation.

2. Rate of voltage rise in the recovery transient decreases as the remaining length of connected line is increased and as the distance of fault from substation is increased. Lowest value of $T_{0.9}$ is obtained at the substation with no remaining connected line and is 22 microseconds.

3. There is little correlation between the

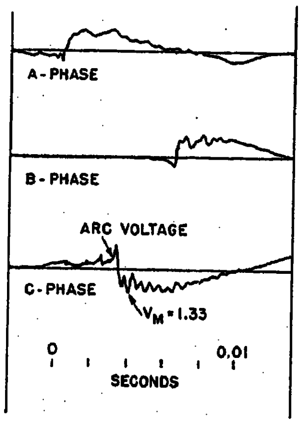


Figure 9. Oscillograms obtained during field tests at point 1

Voltage transients resulting from clearing of 3-phase fault by sectionalizing reclosers

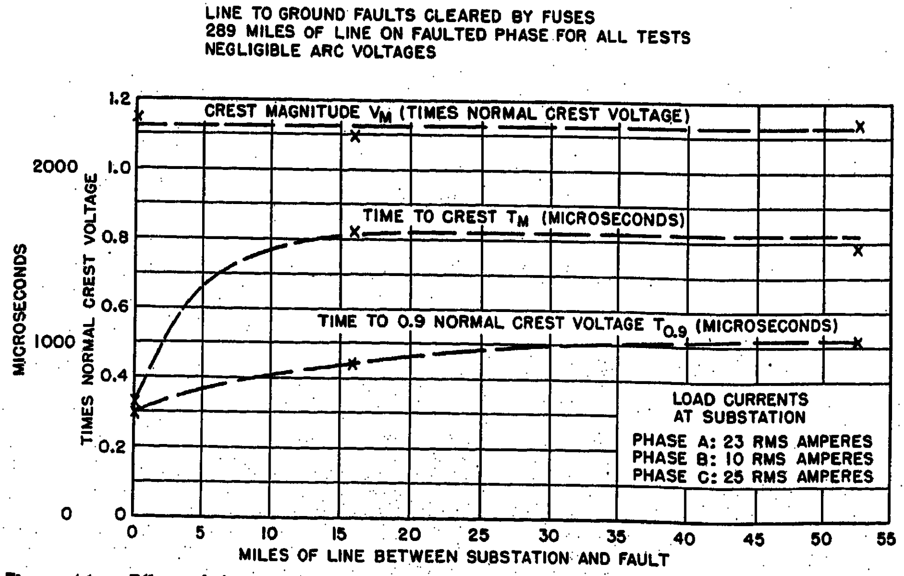


Figure 11. Effect of distance between fault and substation on recovery voltage characteristics. Field test data

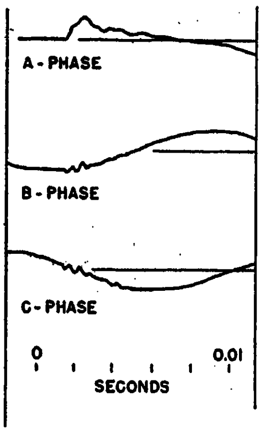


Figure 12. Oscillograms obtained during field tests at point 1. Voltage transients on phase *A* and minor disturbances on phases *B* and *C* due to coupling between phases

crest magnitudes of the most severe recovery transients and the test circuit conditions as will be discussed in a following paragraph.

4. Measured clearing angles for faults at the substation are less than 90 degrees. This results in lower restored circuit voltages and therefore the crest recovery voltages are less than the maximum possible. Clearing angle is defined as the time measured in electrical degrees by which the current zero lags the preceding voltage zero.

Examination of several of the oscillograms of recovery voltage transients helps explain 3 and 4 above. Figure 9 shows the cathode-ray oscillograms of recovery voltage obtained during the 3-phase fault at Point 1 which was cleared by the line sectionalizing reclosers, see Table IV. This was the maximum recorded recovery transient, V_M, of 1.33 times normal crest voltage and is seen occurring on phase *C*. Its severity is largely due to the final rapid rise in arc voltage of the line sectionalizing recloser just prior to current interruption. During most field tests, the arc voltage characteristics of the fault clearing devices were an important factor in determining magnitude of the recovery voltages. Figure 10(A) shows the oscillogram obtained during the single-phase fault at the same location, Point 1, and cleared by the same (*C*-phase) sectionalizing recloser. In this case the recovery voltage magnitude, V_M, was only 1.09 times normal crest voltage.

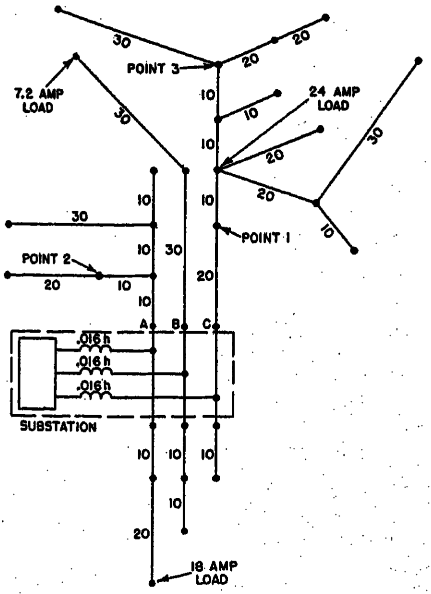


Figure 13. Miniature system used for transient analyzer study of field test conditions

Numbers represent miles of line in a given section

Table V. Symmetrical Rms Fault Current for Copper to Copper Faults

Fault Location	Miles from Substation	Type of Fault, Number of Phases	Fault Current: Measured During Field Tests	Fault Current: Using Fault Calculator *GES-3261* (Based on Measured Values at Substation)
Substation	0	3	748	(748)
	0	1	817	(817)
Point 1	16	3	270	270
	16	1	220	225
Point 2	$18^1/_2$	1	130	140
Point 3	$52^1/_2$	1	52	56

The reduction in magnitude, rate of voltage rise, and change in clearing angle shown in Table IV appear to be due to the change in arc voltage characteristics of the recloser between the two tests.

Figure 10(B) shows the recovery transient obtained on clearing a single-phase fault at the substation with the sectionalizing fuse. It illustrates the rapid rate of voltage rise possible under these conditions. The value of V_M is only 0.94 times normal crest voltage.

During a few of the tests, the recovery transients were modified very little by the current interrupting device, thus permitting other circuit conditions to control the transient recovery voltage characteristics. These involved faults cleared by line-to-ground fuses in which the following factors remained relatively constant.

1. Miles of connected line constant at 289.
2. Substantially no final rapid rise in fuse arc drop before current interruption.
3. Low fuse arc drop during current flow.
4. Clearing angles near most severe condition.
5. Load at substation constant at about 25 rms amperes on the faulted phase.

The oscillograms of the recovery transients are shown in Figures 10(C), 10(D) and 10(E). Results are shown on the curves in Figure 11. These results show that devices such as expulsion lightning arresters and fuses located at transformers are subjected to very moderate recovery transients with respect to both crest magnitudes and rates of rise under normal conditions anywhere on the system under study. When short lengths of line remain connected after the fault as in the case of line sectionalizing devices near the substation, more severe recovery transients may be experienced.

The effect of coupling between phases along the 3-phase line is illustrated in Figure 12. The transient voltage on *A*-phase at Point 1 resulted in the minor disturbances on the other phases.

Comparison of Field Investigation with Analyzer Results and Calculations

Short-Circuit Currents

Table V compares values of symmetrical rms fault current measured during the field tests with values obtained using a simplified fault calculator which consists of special graph paper and template.[3] Another method of calculation[4] gave values within 2 per cent of these calculated values. It may be seen that very close agreement with measured values is obtained by use of these relatively simple methods of calculation. However, a word of caution is in order. Accuracy obtained by these methods depends on the accuracy with which fault current at the substation bus is determined. Considerable variation in this value may exist as the generating capacity of the system supplying the substation is changed. For example, on the system where the field studies were made, fault current at the substation bus could vary from 509 to 865 symmetrical rms amperes for 3-phase faults and from 613 to 916 amperes for single-phase line-to-ground faults.

It is thus evident that available fault currents near the substation are not constant for a given system. This band of possible values must be considered in coordination studies of overcurrent protective devices. The variations cited are true only for this system and may or may

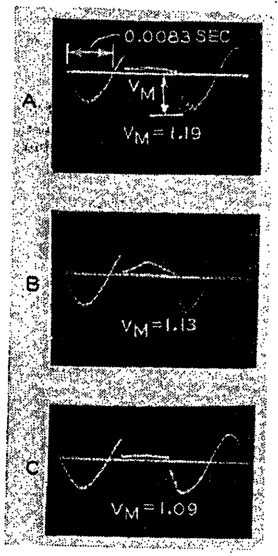


Figure 14 (left). Oscillograms obtained during transient analyzer tests on simulated field system

(A) Fault at point 1 cleared by sectionalizing recloser

(B) Fault at substation, phase C, cleared by line-to-ground fuse

(C) Fault at point 1 cleared by line-to-ground fuse

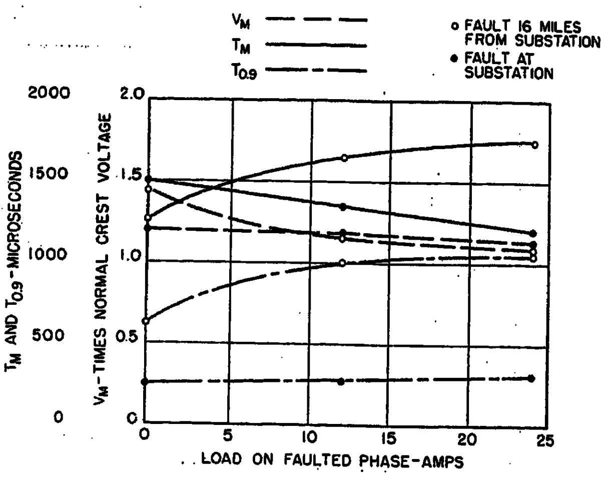


Figure 15 (right). Effect of load on recovery voltage characteristics. Transient analyzer results from simulated field system

not be typical of other rural systems, depending upon their source of power.

RECOVERY VOLTAGES

Comparison of recovery voltage magnitudes, V_M, of Tables II and III with those of Table IV shows that maximum values obtained by the field tests were less than those of the transient analyzer tests. This is to be expected since the analyzer tests were made to show the most severe conditions on a simple, unbranched, unloaded system.

In order to investigate the effect of system loading and system complexity, and to demonstrate the ability of the analyzer to reproduce accurately transient recovery characteristics, additional tests were made. The miniature system of Figure 13 was used. While it is still a considerable simplification of the actual system as shown in Figure 7, it is much more nearly representative of that system than the unbranched lines used in the original analyzer studies. Connected system load was represented as lumped resistances at the points shown on Figure 13. The resistance on the faulted phase was varied to show its effect on system recovery voltage characteristics. The results of this study were:

1. When the miniature system approximates the actual system in complexity of branching and the damping effect of system load is represented, excellent agreement between field and analyzer oscillographic measurement of transient recovery characteristics results. Figure 14 shows three of the oscillograms obtained on the transient analyzer. A tabulation listing corresponding oscillograms from the field tests and comparative measurements are given in Table VI.

2. The damping effect of system load can substantially reduce the magnitude of system recovery voltage. This effect is illustrated by the curves of Figure 15. For example, for a fault at Point 1, 16 miles from the substation, V_M is 33 per cent greater with completely unloaded lines than with a 24-ampere load.

3. Figure 15 indicates that V_M is higher for a fault at Point 1 than for a fault at the substation. In Figures 4, 5, 6 where the complexity of the system was not represented in miniature, the highest values of V_M generally occur for faults at the substation.

These results show that both system complexity and system load are important factors in determining recovery transient characteristics and should be taken into account in transient analyzer studies.

Appendix I

For a simplified representation of recovery voltage, let there be considered the circuit shown in Figure 16, where the line or other capacitance is represented by the lumped capacitance C supplied from a 60-cycle voltage source through a source inductance L. The switch across the capacitor is used to simulate the application and removal of a fault.

Initially with the switch open the current and voltage are in a steady state relationship given by

$$\frac{i(t)}{V_m}=\frac{\cos\left(\omega t+\frac{\pi}{2}\right)}{|x_l-x_c|} \qquad \text{(where } x_c>x_l\text{)}$$

where $i(t)$ is the instantaneous value of current, V_m is the crest value of the source voltage expressed as $V_m \cos \omega t$, and x_l and x_c are inductive and capacitive reactances.

Upon closing the switch and assuming the effect of the discharge of the capacitor C through the shorting switch to be negligible, the equation to be solved is

$$Li_0+\mathcal{L}\,v(t)=(pL)\,Y(p)$$

where i_0 is the initial current in the inductance at the instant of the closing of the switch. If $v(t)=V_m \cos (\omega t+\psi)$ where ψ is the angle of switching, then

$$\mathcal{L}\,v(t)=\mathcal{L}\,Re\,V_m\epsilon^{j(\omega t+\psi)}=\mathcal{L}\,V\epsilon^{j\omega t}$$

$$=\frac{V}{p-j\omega}$$

where

$$V=Re\,V_m\epsilon^{j\psi}$$

Inverting the circuit equation and solving for the current gives

$$i_f(t)=\mathcal{L}^{-1}\,Y(p)=\mathcal{L}^{-1}\frac{V}{(p-j\omega)pL}+\mathcal{L}^{-1}\frac{i_0}{p}$$

$$\frac{i_f(t)}{V_m}=\frac{\sin(\omega t+\psi)-\sin\psi}{x_L}+\frac{i_0}{V_m}$$

where $i_f(t)$ is the instantaneous value of the fault current.

If the lumped capacitance is assumed to be the equivalent of 10 miles of line and the source reactance is assumed to be of the order of 15 ohms, then the contribution of i_0 in the equation for $i_f(t)$ can be assumed negligible. On this basis the curves in Figure 16 were drawn showing the effect of the angle of incidence of the fault in the nature of the short circuit current and the occurrence of the first current zero after initiation. The time of this first current zero may be determined by equating

$$\sin(\omega t+\psi)-\sin\psi=0$$

Table VI. Comparison of Analyzer and Field Measurements

Fault Location	Miles from Substation	Type of Fault	Miles of Line Connected After Fault	Analyzer				Field Test			
				Figure	V_M	T_M	$T_{0.9}$	Figure	V_M	T_M	$T_{0.9}$
Point 1	16	Line sectionalizing	29	14(A)	1.19	1,100	270	10(A)	1.09	660	260
Substation	0	Line to ground	289	14(B)	1.13	1,200	305	10(C)	1.15	1,480	340
Point 1	16	Line to ground	289	14(C)	1.09	1,770	1,070	10(D)	1.10	1,640	880

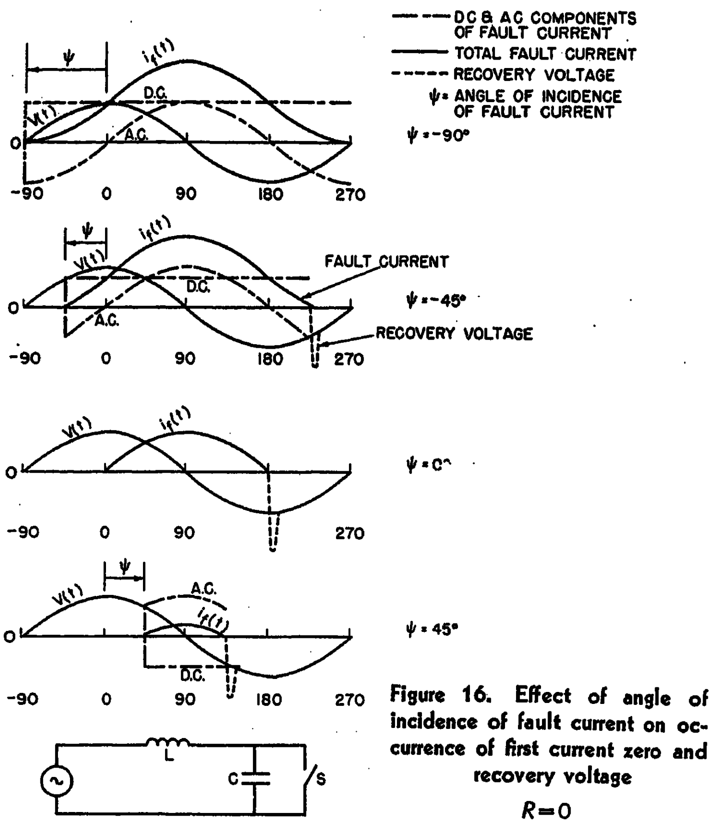


Figure 16. Effect of angle of incidence of fault current on occurrence of first current zero and recovery voltage

$R=0$

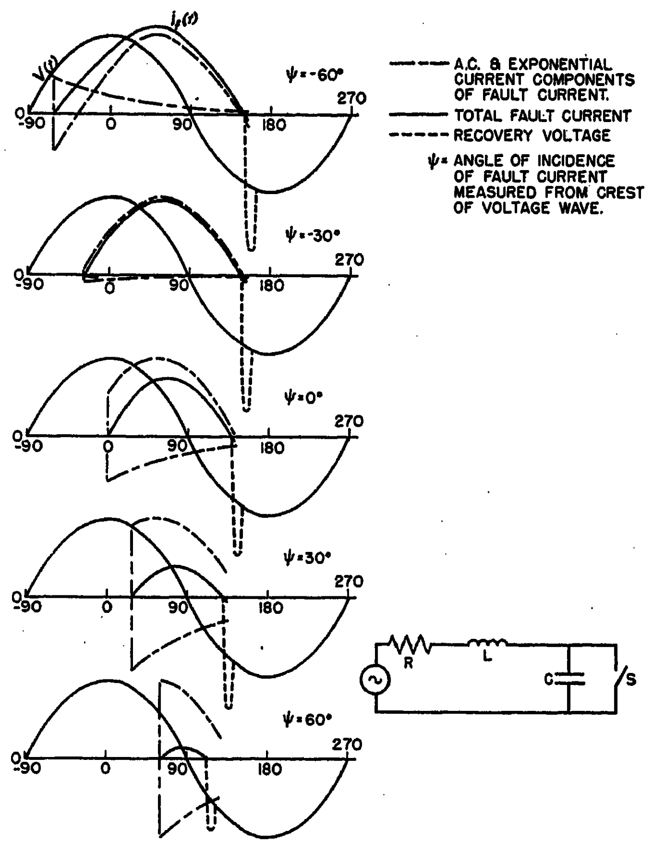


Figure 17. Effect of angle of incidence of fault current on first current zero and recovery voltage

$R=10$ ohms, $X_L=15$ ohms

This equation is satisfied for one condition with $(\omega t+\psi)=(\pi-\psi)$, or $\omega t=\pi-2\psi$.

If the fault current is assumed to stop at its first current zero, a transient voltage appears across the capacitor. This recovery voltage may be calculated by superimposing the effects of the initial current and voltage in circuit if any upon the results obtained by the closing of a switch connecting the series circuit of L and C upon the voltage source at switching angle on the voltage wave given by the instant the short circuit passes through zero. Thus, for this case of zero initial current and voltage, the spectral equation for the current is

$$Y(p)=\frac{\mathcal{L}\,v(t)}{\left(pL+\dfrac{1}{pC}\right)}$$

$$\mathcal{L}\,v(t)=\mathcal{L}\,V_m\cos\,(\omega t+\pi-\psi)$$

$$=\mathcal{L}\,Re\,V_m\epsilon^{j(\pi-\psi)}\epsilon^{j\omega t}=\frac{V}{p-j\omega}$$

where $(\pi-\psi)$ is the angle of switching on the voltage wave of the source corresponding to the instant of the first current zero following the initiation of the short-circuit current. The recovery voltage across the capacitor is

$$v_c(p)=\frac{1}{pC}Y(p)=\frac{V}{pC\left(pL+\dfrac{1}{pC}\right)(p-j\omega)}$$

$$=\frac{V}{LC}\left[\frac{1}{(p+j\Omega)(p-j\Omega)(p-j\omega)}\right]$$

where $\Omega=1/\sqrt{LC}$.

Inverting the equation and solving for the voltage gives

$$v_c(t)=\frac{V_mRe}{LC}\left[-\frac{\epsilon^{-j(\Omega t-\pi+\psi)}}{2(\Omega^2+\Omega\omega)}-\frac{\epsilon^{j(\Omega t+\pi-\psi)}}{2(\Omega^2-\Omega\omega)}-\frac{\epsilon^{j(\omega t+\pi-\psi)}}{\omega^2-\Omega^2}\right]$$

for $\Omega>>\omega$.

$$\frac{v_c(t)}{V_m}=\cos\,(\omega t+\pi-\psi)-\frac{\cos\,(\Omega t-\pi+\psi)}{2}-\frac{\cos\,(\Omega t+\pi-\psi)}{2}$$

$$=\cos\,(\omega t+\pi-\psi)-\cos\,\Omega t\cos\,(\pi-\psi)$$

The voltage at the first peak of the transient will be obtained for $t=\pi/\Omega$. Thus,

$$\frac{v_c(t)}{V_m}=2\cos\,(\pi-\psi)\qquad (t=\pi/\Omega)$$

The maximum recovery voltage in the simplified circuit occurs for the case in which the fault current was initiated at $\psi=0$ or at the crest of the voltage wave. The time to crest voltage is given by $t=\pi/\Omega$ seconds.

If it is assumed that $x_L=15$ ohms and $x_c=25{,}000$ ohms (capacitance of approximately 10 miles of single-phase line) the time to the maximum first peak of the transient of two times normal is 204 microseconds.

The results of the effect of an appreciable resistance ($R=10$ ohms) added in series with the inductance as shown in Figure 17 are given in Figure 17 for various angles of fault application. The curves are based on the fault current equation

$$\frac{i(t)}{V_m}=\frac{1}{Z}\,(\cos\,\omega t+\psi-\theta)-\epsilon^{-\frac{R}{L}t}\cos\,(\psi-\theta)$$

where

$Z=R+j\omega L$
$\psi=$angle of switching, measured from crest of voltage wave
$\theta=\tan^{-1}\omega L/R$

References

1. An Electric Circuit Transient Analyzer, H. A. Peterson. *General Electric Review* (Schenectady, N. Y.) volume 42, 1939, pages 394–400.

2. Advances in Technique of Lightning Measurements, T. Brownlee. *AIEE Transactions*, volume 68, part I, 1949, pages 515–19.

3. Quicker Way of Computing Distribution Short Circuits, John D. Sauter, G. F. Lincks. *Electrical World*, (New York, N. Y.) March 21, 1942, pages 70–72.

4. Graphical Method of Calculating Fault Currents in Distribution Circuits, F. W. Linder. *Electrical Engineering* (*AIEE Transactions*), volume 64, January 1945, pages 16–19.

5. Lightning Investigation on a Rural Distribution System, D. D. MacCarthy, D. A. Stann, D. R. Edge, W. C. McKinley. *AIEE Transactions*, volume 68, part I, 1949, pages 428–37.

6. Overcurrent Investigation on a Rural Distribution System, G. F. Lincks, D. R. Edge, W. C. McKinley, J. H. Leh. *AIEE Transactions*, volume 68, part II, 1949, pages 915–29.

Discussion

G. Fred Lincks (General Electric Company, Pittsfield, Mass.): The authors have recognized the broader scope of the problem of recovery voltages as it applies to the interrupting problems with fuse cutouts and reclosers as well as expulsion lightning arresters. Mr. R. L. Witzke in his paper Voltage Recovery Characteristics of Distribution Systems[1] centered his thinking on expulsion lightning arresters. As shown by Mr. Eason and his associates, the operation of expulsion lightning arresters and fuse cutouts at transformer installations is subject to similar rates of rise and magnitude of recovery voltages. However, when fuse cutouts or reclosers at sectionalizing points disconnect whole portions of a circuit, the recovery voltages generally are more severe.

This has a very direct bearing on the need of the industry to rationalize published interrupting ratings of distribution fuse cutouts with actual operating practices. In innumerable cases fuse cutouts have been applied where the interrupting duty greatly exceeds the published rating. Yet both the applications and the ratings are justified by actual test data, but obtained under different magnitudes and rates of rise of recovery voltage.

Therefore, it is important to determine the rates of rise and magnitude of recovery voltages existing on different types of distribution systems. Then test circuits for determining interrupting ratings of fuse cutouts and reclosers can be standardized to represent actual service conditions. When this is done, fuse cutouts and reclosers can be rated in line with safe application practices.

REFERENCE

1. VOLTAGE-RECOVERY CHARACTERISTICS OF DISTRIBUTION SYSTEMS, R. L. Witzke. *AIEE Transactions*, volume 68, part I, 1949, pages 172–87.

R. L. Witzke (Westinghouse Electric Corporation, East Pittsburgh, Pa.): This paper will be useful to the industry because it contains field-test data on distribution-circuit recovery-voltage characteristics. As most recovery-voltage data are obtained from miniature-system studies, it is highly desirable to have field data available for comparative purposes. It was gratifying to see that substantial agreement could be obtained between field and laboratory results.

The Central Electric Cooperative system, on which the tests were made, has 217 miles of equivalent 3-phase circuit. This length of circuit, with its large number of branches, and the small conductor sizes are in a direction to give low recovery voltages, as compared to what might be experienced on a less extensive system. This factor should be kept in mind in the design and application of protective devices.

The transient analyzer studies reported in this paper were made by varying the angle of incidence of the fault. Analog computer studies summarized in the paper "Voltage-Recovery Characteristics of Distribution Systems," neglected this factor as unimportant because of the frequencies involved.[1] In high-frequency circuits the initiating transient usually is damped to a negligible level in a half-cycle of a 60-cycle wave, and consequently cannot contribute appreciably to the recovery transient.

The authors have given no consideration to double line-to-ground faults. As these faults may be more severe than single line-to-ground faults, they should not be neglected in the design and application of protective devices, the performance of which is influenced by the circuit recovery characteristics.

Figure 3 of the paper shows considerable variation in the magnitude of the circuit recovery voltage with changes in source reactance, even with 2/0 conductors. This does not agree with our analog computer studies referred to earlier. With large source reactances (small transformer banks), both papers report voltages of around 175 per cent irrespective of conductor size. However, with low source reactances, the transient analyzer studies show voltages of only 125 per cent with 2/0 or number 8 conductors. The analog computer studies gave voltages of 175 per cent with 4/0 conductors, 150 per cent with 2/0 conductors, and 125 per cent with number 6 conductor. We believe the differences can be attributed to the fact that the authors considered only sending-end faults, and to high-frequency losses in the miniature systems. The analog computer studies were based on a source reactance-to-resistance ratio of at least ten at all frequencies encountered.

REFERENCE

1. See reference 1 of the preceding discussion.

W. H. Eason, I. B. Johnson, J. W. Kalb, H. A. Peterson: Mr. Witzke indicates that the tests made on the Central Electric Cooperative system involving 217 miles of equivalent 3-phase circuit (actually 42 miles of 3-phase construction and 524 miles of single-phase multibranched lines) was such as to give low recovery voltages as compared to what might be experienced on a less extensive system. While, in general, this is true, it is also true that the results obtained for the complete system were lowered somewhat by loads at the time of testing as illustrated in Figure 15 of the paper; it is also true that results also were obtained on this extensive system involving shorter portions subsequent to the clearing of the fault, such as 29 and 0 miles of line connected to the substation transformer as indicated in Table III of the paper. In these tests with less extensive system connections, recovery voltages were also relatively low, particularly for the case of 0 miles of line connected to the transformer where it had been expected that the worse recovery voltages would occur rather than those obtained which did not exceed normal crest line to neutral voltage. For this latter case, a factual evaluation and understanding of the attenuation factors producing these results have not, at present, been accomplished—but are being studied. However, we feel that the system and the results obtained therefrom, both in the field and on the analyzer, are representative of a typical rural distribution system.

Another comment is made regarding the fact that the transient analyzer results were obtained by varying the angle of incidence of the fault which was not done in Mr. Witzke's studies on the analog computer because it was assumed the initiating high-frequency transients would be damped to a negligible value and would not contribute to the recovery transient. In studying recovery transients as related to various protective devices for distribution systems, some of these could clear the fault under relatively steady-state conditions where the phase-angle relationship between current and voltage imposed by the circuit would be the defining factor in determining the current zero and thereby the clearing angle on the applied voltage which would initiate the recovery voltages. On the other hand, other protective devices could clear on the first current zero following initiation of the fault, and these would not necessarily occur at a clearing angle on the applied wave corresponding to the steady-state phase angle and would vary depending upon the point of fault initiation as shown in Figures 16 and 17 of the paper, illustrating primarily the effect of d-c offset for a simple lumped-constant series circuit of inductance and capacitance. As seen in these two figures for the simple circuit, the initial conditions of charge on the capacitor or current in the inductance at the time of fault clearing that might effect the magnitude of the recovery voltage are always zero. It was from the foregoing viewpoints on the more extensive systems studied that the angle of incidence of the fault was varied such that the angle of clearing of the fault at the first current zero would produce the highest recovery voltages. Regardless of the former, in the transient analyzer study itself it was found necessary to vary the point of fault initiation to obtain the maximum recovery voltage upon fault clearing at the first current zero following the application of the fault.

A question is raised by Mr. Witzke on our supposed disregard of double line-to-ground faults. The question was reviewed in our preliminary study and not extensively investigated for two reasons:

1. Rural distribution systems of the type studied are predominantly single-phase except for some short 2-phase and 3-phase sections near the substation.

2. Multigrounded neutral rural distribution systems should have ratios of X_0/X_1 not appreciably greater than one, and it is for those systems where this ratio is appreciably greater than one where double line-to-ground faults become more severe than single line-to-ground faults and should not be neglected.

Lastly, Mr. Witzke mentions a disagreement in the magnitude of the recovery voltages between those obtained on the analog computer and those obtained on the transient analyzer for the conditions of low source reactance and short lengths of line with different size conductors. With low source reactance, the transient analyzer gave results of the order of 125–130 per cent maximum recovery voltages for short lengths of line having conductor sizes ranging from number 8 up to 2/0. Analog computer studies gave results of 125 per cent for number 6 conductor, increasing to 150 per cent for 2/0 conductor. For the smaller size line conductors, the conclusions from the two sets of data appear to be in good agreement. For the larger size conductors approaching 2/0, the disagreement reaches the order of a 15 per cent difference. To the extent to which all the circuit factors actually are known precisely and included, it is a question as to whether or not this difference may not be considered a fair agreement.

In Mr. Lincks' discussion, he has emphasized the applicability of these results to design and test standards for fuse cutouts and reclosers as well as for expulsion-type lightning arresters. The material presented in this paper and in others dealing with other types of distribution systems should be utilized in considering revision of test standards to recognize the important factor of system transient recovery characteristics.

A Derivation of Heaviside's Operational Calculus Based on the Generalized Functions of Schwartz

J. J. SMITH
MEMBER AIEE

P. L. ALGER
FELLOW AIEE

THE RECENT work of the French mathematician Laurent Schwartz,[1] n developing a mathematical system in which differentiation is commutative with ntegration, is of great practical imporance, as it enables the operational methods of Heaviside to be verified and sefully extended. Schwartz has genralized the concept of a "function" by onsidering it to be represented by a disribution of mass, and he has thus been ble to interpret such discontinuous exressions as the Dirac function and its erivatives in ways not previously permitted by classical mathematics. These deas provide a rigorous mathematical asis for the intuitive methods of Heavide.

Differentiation and Integration Noncommutative

The beginning student of calculus arns that taking the derivative of a funcon is a "many-to-one" process. No atter what the value of the constant rm, A, is, the derivative of $f(x)$—A has e same value, $f'(x)$

$$(f(x)-A)=f'(x) \qquad (1)$$

He also learns that integration between ed limits is a "one-to-one" process. ith a given lower limit of integration, B, e equation

$$\int_B^x f'(x)dx=f(x)-f(B) \qquad (2)$$

always true.

He thus comes to realize that differention is not always commutative with (the inverse of) integration. If we first differentiate $f(x)$—A, and then integrate the result, we get $f(x)$—$f(B)$

$$\int_B^x dx\frac{d}{dx}(f(x)-A)=\int_B^x f'(x)dx=f(x)-f(B) \qquad (3)$$

If we first integrate $f(x)$—A and then differentiate the result, we obtain $f(x)$—A again

$$\frac{d}{dx}\int_B^x (f(x)-A)dx=\frac{d}{dx}\left[\int_B^x f(x)dx-A(x-B)\right]=f(x)-A \qquad (4)$$

One consequence of this state of affairs is that solving the differential equation representing any physical problem requires finding the appropriate integration constants. In complex problems, this sometimes presents real difficulties, besides being a time-consuming process.

A little study of equations 3 and 4 shows that integration will be the reverse of differentiation, if $A=f(B)$. Practically, this requires

$$A=f(B)=0 \qquad (5)$$

It will be found that differentiation and integration are always commutative if the function vanishes at the lower limit of integration.

Heaviside's Methods

Heaviside introduced his unit function, H, or

$$1=H(t,\ t_0)=1 \text{ if } t>t_0$$
$$=0 \text{ if } t<t_0 \qquad (6)$$

for convenience in treating sudden changes in the physical conditions, such as a suddenly applied voltage at $t=t_0$. This same function, however, may be employed as a "cut-off" to reduce any function arbitrarily to zero previous to a desired moment. We may write

$$H(t,t_0)f(t)=f(t) \text{ if } t>t_0$$
$$=0 \text{ if } t<t_0 \qquad (7)$$

Or, we may assume $t_0=0$, and abbreviate equation 7 to the simple form

$$Hf(x)=f(t) \text{ if } t>0$$
$$=0 \text{ if } t<0 \qquad (8)$$

Heaviside also denoted the differential operator, d/dt, by p, also using its inverse, p^{-1}, to define the integration process. This we regard as merely a convenience in writing. In this paper, we retain d/dt to emphasize that the substitution of p is purely a matter of convention.

Heaviside[2] states:

> "The reader may have observed . . . . . . that we change the order of operators at convenience, as in
>
> $$(p)g(p)1=g(p)f(p)1 \qquad (9)$$
>
> and that it goes. But I do not assert the universal validity of this obviously suggested transformation. It has, however, a very wide application, and transforms functions in a remarkable manner. Reservations should be learned by experience."

The classical objection to this statement of Heaviside is given by the following example.* Let $f(p)=p^m$, representing m successive differentiations, and $g(p)=p^{-n}$, representing n successive integrations.

Then, it is reasoned

$$p^m1=\frac{d^m1}{dt^m}=0 \qquad (10)$$

Paper 49-184, recommended by the AIEE Basic Sciences Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20-24, 1949. Manuscript submitted March 23, 1949; made available for printing May 3, 1949.

J. J. SMITH and P. L. ALGER are with the General Electric Company, Schenectady, N. Y.

* It should be noted that in this paper we consider only whole integrations and differentiations. Fractional differentiation will be considered in a subsequent paper.

$$p^{-n}1=\int^t\int\int_n\int\ldots 1\,dt^n=\frac{t^n}{(n)!} \tag{10A}$$

if we consider only values of t greater than zero, for which the unit function is unity.

From equations 10 and 10(A), if $m<n$

$$f(p)g(p)1=p^{-(n-m)}1=\frac{t^{n-m}}{(n-m)!} \tag{11}$$

Or, if $m>n$

$$f(p)g(p)1=p^{m-n}1=0$$

But, for all values of m and n

$$g(p)f(p)1=0$$

since when the differentiation is performed first, the zero answer of equation 10 is not changed by subsequent operations.

The reply to this objection is that classical mathematics leaves out of consideration the discontinuity of the unit function 1 at $t=0$, on the assumption that the derivative does not exist at such a point.

The discrepancies are cleared up if this derivative is properly interpreted, as will be seen in the discussion following equation 19. Thus, the use of the unit function, while solving the problem of making differentiation and integration generally reversible, introduces the new problem of taking discontinuities into account. Schwartz now has provided the solution for this second problem.

The Contribution of Schwartz

Schwartz states in the introduction of his 1945 paper:

> "Since the introduction of operational calculus, physicists have commonly employed certain concepts, or certain formulas, whose success has been incontestable, even though they were not justified mathematically. Thus, the function $H(t)$ of the real variable t, equal to zero for $t<0$, to one for $t>0$, is usually considered as having for its derivative the 'Dirac function,' $H'(t)=\delta(t)$, that is zero for $t\neq 0$, equal to $+\infty$ for $x=0$, and is such that, besides
>
> $$\int_{-\infty}^{\infty}\delta(t)dt=+1 \tag{12}$$
>
> "Such an 'abuse of language' is entirely incompatible with the usual concept of a function and its derivative! And what should we think of considering successive derivatives of the Dirac function!"

The essence of the Schwartz method lies in considering the Dirac function as being represented by a unit mass placed at the origin, giving it a physical reality in "mass space." By conducting the desired mathematical operations in this mass space, and then transforming the result back into function space, he avoids any direct dealing with infinities.

Schwartz considers a set of continuous infinitely differentiable functions that are zero outside of a finite interval, that is, functions $\phi(t)$, such that

$$\begin{aligned}\phi(t)&=0 \text{ if } t<-k_1\\ &=0 \text{ if } t>+k_2\end{aligned} \tag{13}$$

He multiplies the function $f(t)$, with which it is desired to deal, by $\phi(t)$, and integrates from $-\infty$ to ∞, calling the result a "functional," $f(\phi)$

$$f(\phi)=\int_{-\infty}^{\infty}f(t)\phi(t)dt \tag{14}$$

Then $f(\phi)$ is the representation of $f(t)$ in mass space, where $\phi(t)$ is any function satisfying the conditions stated above. Inversely, if the right-hand side is obtained as the representation in mass space, then the function it represents is $f(t)$.

Similarly, replacing f by f', the representation of the derivative in mass space, if f' exists, is

$$f'(\phi)=\int_{-\infty}^{\infty}f'(t)\phi(t)dt \tag{15}$$

Integrating equation 15 by parts, we obtain

$$\int_{-\infty}^{\infty}f'(t)\phi(t)dt=\left[f(t)\phi(t)\right]_{-\infty}^{\infty}-\int_{-\infty}^{\infty}f(t)\phi'(t)dt \tag{16}$$

By equation 13, however, $\phi(t)=0$ at both ∞ and $-\infty$, so that the first term on the right-hand side of equation 16 drops out, leaving

$$\int_{-\infty}^{\infty}f'(t)\phi(t)dt=-\int_{-\infty}^{\infty}f(t)\phi'(t)dt$$

or

$$f'(\phi)=-f(\phi') \tag{17}$$

The above is straightforward when $f(t)$ is continuous and $f'(t)$ exists. When $f(t)$ is not continuous, $f'(t)$ "does not exist" at the point of discontinuity. However, $df(t)$ does exist in general at this point and is equal to the jump or "saltus" of $f(t)$ at this point. In such cases Schwartz replaces the Riemann integral in equation 15 by the Stieltjes integral

$$f'(\phi)=\int_{-\infty}^{\infty}\phi(t)df(t) \tag{18}$$

and, by integrating by parts as before, it is found that

$$f'(\phi)=-f(\phi') \tag{17}$$

Thus equation 17 holds not only when $f(t)$ is continuous but also when it is discontinuous.

As an example, we may find the representation $\delta(\phi)$ of the Dirac function $\delta(t)$ in mass space. Putting H for $f(t)$ in equation 17 we have

$$\begin{aligned}\delta(\phi)=H'(\phi)=-H(\phi')&=-\int_{-\infty}^{\infty}H(t)\phi'(t)dt\\ &=-\int^{\infty}\phi'(t)dt=\phi(0)\end{aligned} \tag{19}$$

This is the same result that is obtained if $f(t)$ is put equal to H in equation 18 so that $df(t)=dH$ has the value unity (unit mass) at the origin and zero elsewhere and the integration carried out. Thus the Dirac function is represented in mass space by $\phi(0)$ and it also corresponds to a unit mass dH placed at the origin. Similarly, we find

$$-\delta'(\phi)=\phi'(0)$$

and

$$(-1)^n\delta^{(n-1)}\phi=\phi^{(n-1)}(0)$$

Schwartz then shows that $\phi'(0)$ can be interpreted as a unit doublet at the origin and gives similar interpretations for $\phi''(0)\ldots\ldots\phi^{(n)}(0)$.

It now is evident in connection with equation 11 that putting $p^{m-n}1=0$ ignores the discontinuity at $t=0$ and that its representation in mass space is $\phi^{(m-n-1)}(0)$, in which form it is available for subsequent operations.

Since engineers are better acquainted with the Riemann integral, it may be of interest to indicate how Heaviside's use of the impulse function or its equivalent, the Dirac function, would give the same result; subject, of course, to the limitation that some of the steps may not exist in the strict mathematical sense. In this case, we would write $\delta(t)$ for $f(t)$ in equation 14 and obtain

$$\delta(\phi)=\int_{-\infty}^{\infty}\delta(t)\phi(t)dt=\phi(0) \tag{20}$$

on making the usual assumption that $\delta(t)$ is zero everywhere except at $t=0$ where it is infinite in such a manner that its integral from $0-\epsilon$ to $0+\epsilon$ is equal to unity.

Application to a First-Order Differential Equation

To illustrate the application to differential equations, a simple example will be taken so that the procedure will be clear. The problem will be solved by three methods so that similarities and differences between methods will be apparent:

1. Ordinary mathematical method.
2. Heaviside method.
3. The Schwartz transformation method.

The problem to be solved is:

A voltage E is applied at the time $t=0$ to a circuit containing resistance R and inductance L in series. The current in the circuit is zero when $t<0$. Find the current when $t>0$.

ORDINARY MATHEMATICAL METHOD

In the ordinary method of solution we write

$$L\frac{di}{dt}+Ri=E \tag{21}$$

This is the basic form of a linear differential equation of the first order and first degree. The standard mathematical method gives the following solution, which involves the constants of integration C and C_1 which have to be determined.

$$i=C+C_1\epsilon^{-Rt/L} \tag{22}$$

From this, when $t=\infty$ we reason that $di/dt=0$ and hence

$$C=E/R \tag{23}$$

It then is reasoned that since energy is stored in the inductance and we cannot have an instantaneous change of energy, which would require infinite voltage, i must be the same immediately before and after $t=0$, and thus, from equation 22 and the initial conditions

$$C=-C_1 \tag{24}$$

From this

$$i=E(1-\epsilon^{-Rt/L})/R \tag{25}$$

when $t>0$

and we were given as one of the conditions of the problem that

$$i=0 \tag{26}$$

when $t<0$

In this method of solution it will be noted that we had to determine the value of i at $t=0+\epsilon$ by an additional fact, such as the energy consideration above, which was not contained in the original statement of the problem. While this may not have been difficult in the above case, it does become a problem for an equation of the nth order, where n constants of integration have to be evaluated.

HEAVISIDE METHOD

In the Heaviside method we note, first, that it is given that the voltage and current are zero when $t<0$. To ensure this we introduce the Heaviside unit function and write the current in the form iH and the voltage in the form EH, where, for convenience in writing, H is the Heaviside unit function whose value is zero when $t<0$ and is equal to unity when $t>0$, equation 8.

We therefore write the equation in the form

$$L\frac{d}{dt}(iH)+RiH=EH \tag{27}$$

and take the solution for this to be

$$iH=(C+C_1\epsilon^{-Rt/L})H \tag{28}$$

where $-\infty<t<\infty$ and the presence of H ensures that the current is zero when $t<0$. Substituting this value of iH in equation 27, two of the terms cancel out, and we obtain

$$CRH+(C+C_1\epsilon^{-Rt/L})LH'=EH \tag{29}$$

where $H'=dH/dt$. This equation is really two separate equations, one including all terms having the factor H and the other including all terms having the factor H'. By equation 12, H' is zero at all values of t except $t=0$. Thus, when $t\neq 0$ equation 29 still must be valid after the terms including H' are removed. Therefore, taking the terms involving H only gives

$$C=E/R \tag{30}$$

and taking the terms involving H' only gives

$$(C+C_1\epsilon^{-Rt/L})H'=0 \tag{31}$$

From this the coefficient of H' must be zero when $t=0$, since for this value of t, H' is not equal to zero. Hence

$$C+C_1\epsilon^{-Rt/L}=0 \tag{32}$$

when $t=0$, or

$$C=-C_1 \tag{33}$$

Substituting equations 30 and 33 in equation 28 we obtain

$$iH=E(1-\epsilon^{-Rt/L})H/R \tag{34}$$

The difficulty with the above method is, of course, that H' is not a function in the accepted sense of the word. $H'=0$ when $t\neq 0$, but when $t=0$ it must be infinite in such a way that its integral

$$\int_{-\epsilon}^{0+\epsilon}H'dt=1$$

For this latter reason, the mathematicians say "it does not exist at $t=0$." This impasse can be overcome readily by the method of Schwartz, by carrying out our operations in mass space where the counterpart of H' remains finite and interpretable. Having thus detoured around the use of H', we transfer back from the mass space to the function space and find the result checks exactly with that given, using the straight Heaviside method, as will be illustrated now.

SCHWARTZ TRANSFORMATION METHOD

Let us now consider the same problem using the methods developed by Schwartz. We start with the same equation

$$L\frac{d}{dt}(iH)+RiH=EH \tag{35}$$

Assume as before a solution of the form

$$iH=(C+C_1\epsilon^{-Rt/L})H \tag{36}$$

Then the representation of iH in mass space is, from equation 14

$$\int_{-\infty}^{\infty}iH\phi(t)dt=\int_{-\infty}^{\infty}(C+C_1\epsilon^{-Rt/L})H\phi(t)dt$$
$$=\int^{\infty}(C+C_1\epsilon^{-Rt/L})\phi(t)dt \tag{37}$$

and from equation 17, $d(iH)/dt$ is represented in mass space by

$$-\int_{-\infty}^{\infty}iH\phi'(t)dt=-\int_{-\infty}^{\infty}(C+C_1\epsilon^{-Rt/L})\times$$
$$H\phi'(t)dt=-\int_0^{\infty}(C+C_1\epsilon^{-Rt/L})\phi'(t)dt \tag{37A}$$

which, by equation 16, reduces to

$$-\int_{-\infty}^{\infty}iH\phi'(t)dt=(C+C_1)\phi(0)-$$
$$\int_0^{\infty}\frac{C_1R}{L}\epsilon^{-Rt/L}\phi(t)dt \tag{38}$$

Similarly, EH is represented in mass space by

$$\int_{-\infty}^{\infty}EH\phi(t)dt=\int_0^{\infty}E\phi(t)dt \tag{39}$$

Substituting these representations in mass space in the differential equation 35 we get

$$L(C+C_1)\phi(0)-\int_0^{\infty}C_1R\epsilon^{-Rt/L}\phi(t)dt+$$
$$\int_0^{\infty}R(C+C_1\epsilon^{-Rt/L})\phi(t)dt=\int_0^{\infty}E\phi(t)dt \tag{40}$$

This gives

$$L(C+C_1)\phi(0)+\int_0^{\infty}(CR-E)\phi(t)dt=0 \tag{41}$$

Now

$$\int_0^{\infty}(CR-E)\phi(t)dt=\int_{-\infty}^{\infty}(CR-E)H\phi(t)dt$$

is the representation in mass space of $(CR-E)H$ for all values of t, while $\phi(0)$ corresponds to a unit mass placed at the origin and zero mass elsewhere, and is the representation in mass space of the derivative of the unit function as shown by Schwartz, equation 19. Thus we have

$$CR-E=0 \tag{42}$$

Substituting this in equation 41, since $\phi(0)$ represents a unit mass at $t=0$

$$C+C_1=0 \tag{43}$$

Substituting the values of C and C_1 from equations 42 and 43 in equation 36, we find as before, in equation 34

$$iH=E(1-\epsilon^{-Rt/L})H/R \tag{44}$$

DISCUSSION OF THREE METHODS

The results obtained in equations 34 and 44 are fundamental in connection with the Heaviside operational calculus. They say that when the equation for the current is written in the form

$$L\frac{d}{dt}(iH)+RiH=EH \tag{45}$$

this equation has one and only one solution given by

$$iH=E(1-\epsilon^{-Rt/L})H/R \tag{46}$$

This comes about by recognizing that when the value of the current is substituted in the differential equation, and considering H as a function of time t, there can be no terms of the form H' or its representation in mass space on the left-hand side of the equation if there are no terms of this form on the right-hand side. This allows us to conclude from equations 29 or 40

$$C=-C_1 \tag{47}$$

In this method of solution it will be noted that it is not necessary to introduce the condition that $i=0$ when $t=0+\epsilon$ as well as when $t=0-\epsilon$, which was required in the solution by the ordinary mathematical method in equations 21 to 26. In view of the emphasis usually placed in standard mathematical procedure on the determination of necessary and sufficient conditions for a problem, it would appear that the Heaviside and Schwartz method of solution are more powerful than the ordinary mathematical method, since they do not require this added condition $i=0$ when $t=0+\epsilon$.

Application to a Second-Order Differential Equation

Before proceeding to an nth order differential equation, it may be of interest to consider a second-order differential equation from a slightly different viewpoint. As an example, we shall consider the well-known Heaviside operational expression

$$\frac{p^2}{p^2+\omega^2}H=\cos \omega t H \tag{48}$$

If we were not given the result, we could evaluate the operational expression on the left-hand side by writing it equal to iH as follows

$$iH=\frac{p^2}{p^2+\omega^2}H \tag{49}$$

This can be written in the form of a differential equation on putting $p=d/dt$

$$\frac{d^2}{dt^2}(iH)+\omega^2 iH=\frac{d^2H}{dt^2} \tag{50}$$

Here, of course, we are in trouble immediately because d^2H/dt^2 is not a "function." Let us proceed, however, in the Heaviside method to assume a solution

$$iH=(C_1+C_2 \sin \omega t+C_3 \cos \omega t)H \tag{51}$$

We then can write formally in the Heaviside manner

$$\frac{d}{dt}(iH)=(\omega C_2 \cos \omega t-\omega C_3 \sin \omega t)H+ (C_1+C_3)H' \tag{52}$$

where the coefficient of H' is the value of $(C_1+C_2 \sin \omega t+C_3 \cos \omega t)$ at $t=0$. Differentiating again

$$\frac{d^2}{dt^2}(iH)=-\omega^2(C_2 \sin \omega t+C_3 \cos \omega t)H+ \omega C_2H'+(C_1+C_3)H'' \tag{53}$$

where now the coefficient of H' is the value of $(\omega C_2 \cos \omega t-\omega C_3 \sin \omega t)$ when $t=0$.

Substituting this value in equation 50 we find

$$\omega C_2H'+(C_1+C_3)H''+\omega^2C_1H=H'' \tag{54}$$

Equating the coefficients of H, H', and H'' on both sides of this equation

$$C_1=0 \tag{55}$$

$$C_2=0 \tag{56}$$

and

$$C_3=1 \tag{57}$$

Thus, substituting in equation 51 we find

$$iH=\cos \omega t H \tag{58}$$

as the solution of equation 50, and hence we verify the value of the Heaviside operational expression, equation 48.

Let us now apply the Schwartz method in solving this problem. From equations 14 and 51 the representation of iH in mass space is

$$\int_{-\infty}^{\infty} iH\phi(t)dt=\int_{-\infty}^{\infty}(C_1+C_2 \sin \omega t+ C_3 \cos \omega t)H\phi(t)dt=\int_0^{\infty}(C_1+C_2\times \sin \omega t+C_3 \cos \omega t)\phi(t)dt \tag{59}$$

and $\dfrac{d^2}{dt^2}(iH)$ is represented in mass space by

$$\begin{aligned}\int_{-\infty}^{\infty} iH\phi''(t)dt&=\int_0^{\infty}(C_1+C_2 \sin \omega t+ C_3 \cos \omega t)\phi''(t)dt\\ &=-(C_1+C_3)\phi'(0)-\int_0^{\infty}\omega(C_2 \cos \omega t- C_3 \sin \omega t)\phi'(t)dt\\ &=-(C_1+C_3)\phi'(0)+\omega C_2\phi(0)-\int_0^{\infty}\omega^2\times (C_2 \sin \omega t+C_3 \cos \omega t)\phi(t)dt\end{aligned} \tag{60}$$

where, in the second line, the coefficient of $\phi'(0)$ is the value of $(C_1+C_2 \sin \omega t+C_3 \cos \omega t)$ at $t=0$, and in the third line, the coefficient of $\phi(0)$ is the value of $(C_2 \cos \omega t-C_3 \sin \omega t)$ when $t=0$.

Since, as we have already pointed out, d^2H/dt^2 "does not exist as a function," we are in difficulty with the right-hand side of equation 50. However, we may choose to recognize it as the second derivative of the unit function, and although "it does not exist as a function," its representation in mass space does exist and is equal to

$$\int_{-\infty}^{\infty} H\phi''(t)dt=\int_0^{\infty}\phi''(t)dt=-\phi'(0) \tag{61}$$

Now substitute the mass representations given by equations 59, 60, and 61 in the differential equation 50 and obtain

$$-(C_1+C_3)\phi'(0)+\omega C_2\phi(0)- \int_0^{\infty}\omega^2(C_2 \sin \omega t+C_3 \cos \omega t)\phi(t)dt+ \int_0^{\infty}\omega^2(C_1+C_2 \sin \omega t+C_3 \cos \omega t)\phi(t)dt =-\phi'(0) \tag{62}$$

In this equation we equate the parts under the integral sign and the coefficients* of $\phi(0)$ and $\phi'(0)$ on each side of the equation and obtain

$$\omega^2C_1=0 \tag{63}$$

$$\omega C_2=0 \tag{64}$$

and

$$C_1+C_3=1 \tag{65}$$

Hence

$$C_3=1 \tag{66}$$

as in equation 57 and, substituting in equation 51, we obtain

$$iH=\cos \omega t H \tag{67}$$

as in equation 58.

Application to an *n*th-Order Differential Equation

We may apply the above method to an nth-order equation

$$\sum_n a_n\frac{d^n}{dt^n}(iH)=EH \tag{68}$$

and assume a solution for it of the form

$$iH=\left(C_0+\sum_n C_n\epsilon^{-\alpha_n t}\right)H \tag{69}$$

where the α_n are determined in the usual manner. Substituting this in equation 68 we obtain $n+1$ equations by equating the coefficients of $H', H'', \ldots\ldots H^{(n)}$ on each side of the equation, and from these the unknown constants can be determined. The difficulties associated with the use of $H', H'', \ldots\ldots H^{(n)}$ can be avoided, of course, by using the Schwartz transformation method.

---

* The reason for equating the coefficients of $\phi(0)$ and $\phi'(0)$ separately is that $\phi(0)$ corresponds to a unit mass at $t=0$ and $\phi'(0)$ corresponds to a doublet at $t=0$.

This becomes a lengthy operation when n even exceeds two, and would involve an infinite set of equations when $n=\infty$, as it does in the solution of partial differential equations. The Heaviside expansion theorem is a very skillful way of getting around this difficulty, as we shall show now.

It has been seen that the values of the unknown constants in equations 28 or 36 can be obtained directly from the differential equations 27 or 35, and that no other assumptions have to be made in order to solve it. Since the information given in equations 27 or 35 by the introduction of H ensures that it has one and only one solution, we can assign a meaning to the equation when written in the form

$$iH=\frac{E}{L\frac{d}{dt}+R}H \qquad (70)$$

To evaluate this expression for i, we simply rearrange in the form of equations 27 or 35 and solve this differential equation as shown and arrive at the solution in equations 34 or 44. From this we can write

$$\frac{E}{L\frac{d}{dt}+R}H=E(1-\epsilon^{-Rt/L})H/R \qquad (71)$$

We may add n such terms together and get

$$\sum_n\frac{E}{L_n\frac{d}{dt}+R_n}H=\sum_n E(1-\epsilon^{-R_nt/L_n})H/R_n \qquad (72)$$

We may add all the quantities on the left-hand side of this equation to get the quotient of two operators acting on H which we may write

$$\frac{\sum_n b_{n-1}\frac{d^{n-1}}{dt^{n-1}}}{\sum_n a_n\frac{d}{dt^n}}EH=\sum_n\frac{E}{L_n\frac{d}{dt}+R_n}H$$
$$=\sum_n E(1-\epsilon^{-R_nt/L_n})H/R_n \qquad (73)$$

These three steps, in the order shown, are equivalent in reverse order to those used by Heaviside in the development of his expansion theorem. Using his terminology, replacing d/dt by p, he wrote[3]

$$iH=\frac{Y(p)}{Z(p)}EH=\sum_n\frac{Y(p_n)}{\left[p\frac{dZ}{dp}\right]_{p=p_n}}\frac{1}{p-p_n}H$$
$$=\frac{Y(0)}{Z(0)}H+\sum_n\frac{Y(p_n)\epsilon^{p_nt}}{\left[p\frac{dZ}{dp}\right]_{p=p_n}}H \qquad (74)$$

Thus, by expanding in partial fractions we break the solution of a differential equation of the nth order up into the solution of n equations of the first order, whose solution has been found already in equations 34 and 44, and thus we avoid writing a general solution for the nth degree equation and then finding the values of $n+1$ constants in equation 69.

Application to Differential Equations of Infinite Order and Partial Differential Equations

This idea can be extended to differential equations of infinite order such as, for instance, a typical form shown by[4]

$$vH=\frac{\sinh\,(l-x)\sqrt{RS\frac{d}{dt}}}{\sinh\,l\sqrt{RS\frac{d}{dt}}}EH$$
$$=E\left(1-\frac{x}{l}\right)H-\frac{2E}{\pi}\times$$
$$\sum_{n=1}^{n=\infty}\frac{\sin\frac{n\pi x}{l}\epsilon^{-(n^2\pi^2t/RSl^2)}H}{n} \qquad (75)$$

Such differential equations of infinite order arise naturally in one method of solution of the partial differential equations of mathematical physics. The introduction of H gives directly the coefficients of the functions in the resulting Fourier series or integrals, without the necessity of assuming a series with an infinite number of unknown constants and later determining these constants by methods similar to those involved in the use of Fourier's theorem. An extensive application employing this method will be found in a paper by one of the authors.[5]

In that paper are derived solutions of the differential equations of mathematical physics corresponding to Poisson's equation, the equation of heat conduction, Maxwell's wave equation, and the wave equation in a dissipative medium. These solutions are given in generalized orthogonal co-ordinates. To use them for a particular problem, it is only necessary to substitute in them the appropriate function, sine or cosine, or the Bessel functions, or the Legendre functions, and so forth, to get the final solution, which has checked in all cases tried with that given in the literature. Tables of such solutions for Green's functions for rectangular co-ordinates were presented at the AIEE winter meeting in 1949.

The method used in the Journal of the Franklin Institute paper[5] is based directly on the use of the impulse function where necessary, since the Schwartz method was not available at that time. It will be found that the use of the impulse function can be avoided by using the Schwartz method, which will lead to the same results given in that paper. Since this application of the Schwartz method is relatively easy, we refer the reader to that paper for more details as to the method of application to partial differential equations.

Application of Method to Given Initial Conditions

Since sometimes it is stated that the Heaviside method cannot be applied to problems where given initial conditions have to be met, it may be well to point out that this is not so and to indicate how such problems can be solved. Many such examples will be found worked out in chapter one of Jeffreys' book[7] using a different notation. To use it in the case of the simple example already discussed, we reformulate the problem stated on page 7 of reference 7 as follows:

> A voltage E is applied at the time $t=0$ to a circuit containing resistance R and inductance L in series. The current in the circuit is zero when $t<0$. At $t=0+\epsilon$ the current is i_0. Find the current when $t>0$.

We can write as before

$$L\frac{d}{dt}(iH)+RiH=EH \qquad (76)$$

when $t<0$ and $t>0$. It must be noted that we now restrict the validity of this equation to the region $t<0$ and $t>0$, since the introduction of the terminal condition $i=i_0$ when $t=0+\epsilon$ means that it cannot hold at $t=0$. To introduce the terminal condition $i=i_0$ when $t=0+\epsilon$, integrate from $-\infty$ to t and obtain

$$LiH+R\int_0^t iHd=EtH+Li_0H \qquad (77)$$

when $-\infty<t<\infty$. Then this equation incorporates all the given conditions of the problem and is valid from $-\infty$ to ∞.

Either the Heaviside or the Schwartz method can be used to solve it along the lines given in the previous example. In both of these methods we start with the solution in equation 28 and substitute in the integro-differential equation 77. It then will be found that by differentiating once, the equation reduces to one involving H and H' in the Heaviside method, and we obtain the values of the constants C_1 and C_2 as before, subject, of course, to the difficulties inherent in the use of H'. In the Schwartz method we get a representation in mass space consisting of two terms, one being an integral from $-\infty$ to ∞ and the other involving $\phi(0)$. By equating the coefficients of these two terms as before, we get the required result

$$iH=\frac{E}{R}(1-\epsilon^{-Rt/L})H+i_0H\epsilon^{-Rt/L} \tag{78}$$

It may be well to point out that in the Heaviside manner we find by differentiating equation 77 that this is the solution of

$$L\frac{d}{dt}(iH)+RiH=EH+Li_0H' \tag{79}$$

when $-\infty<t<\infty$. Thus equation 76 requires the addition of an impulse-function term to make it valid from $-\infty$ to ∞. Furthermore, equation 78 is the only solution of equation 79, just as equation 46 is the only solution of equation 45. There is no conflict between these statements.

Conclusion

In the paper we have emphasized the use of two concepts—the introduction of the Heaviside unit function to ensure that a given expression has the value zero in some parts of the region in which it is defined; the use of the impulse function and its derivatives which arise when we attempt to differentiate the unit function. These seem to the authors to be the ideas which make the operational methods used by Heaviside and others give the results they do.

Some difficulties are encountered in proceeding in this manner, due to the fact that the impulse function and its derivatives are not functions in the accepted mathematical sense of the word. The method of Schwartz is introduced; this permits us to take practically identical steps with those taken in the Heaviside approach. However, since the operations are carried out in mass space, no infinities are encountered and the results are put on a sound mathematical basis.

Some authors view the operational calculus as a method based upon substituting p for d/dt and then treating p as if it were a number. The point of view presented here considers that the substitution of p for d/dt is just a convenience in writing, and in order to emphasize this, no use has been made of p in this paper.

We have endeavored to focus attention on the other concept used by Heaviside, his unit function *1*, for which we have used the symbol H. It is, perhaps, unfortunate that Heaviside, after explaining his unit function and its use, then decided that for brevity's sake he would omit it from the equations. If he had written it in the equations continually, its presence would have aroused curiosity, and its importance would have been better understood. In this paper we have tried carefully to be sure that in all the equations where a function of d/dt arises, it acts on an operand which is multiplied by H. If any are found which do not conform to this pattern they are oversights.

Other authors approach the operational calculus from the point of view of the Laplace transformation. In a certain sense the Schwartz method may be regarded as a generalization of the Laplace transform

$$\int_0^\infty \epsilon^{-ax}f(x)dx$$

in which the function $\phi(t)$ replaces the exponential factor and the limits of integration are extended throughout the range of the function $\phi(t)$ rather than from zero to infinity. It goes much further than this, however, as it gives us a method of interpreting the derivative of any order of a discontinuous function. Furthermore, on account of the general character of $\phi(t)$, it can be used with many types of functions—Bessel, Legendre functions, and so forth, and their associated differential equations; whereas the Laplace transform is intimately tied in to the exponential function and its associated linear differential equation with constant coefficients of the first degree and nth order.

By the introduction of H we obtain a system in which integral and differential operations are reversible. This comes about due to the fact that when H is suitably introduced into the differential equations and the differential and integral operations are performed, there results an equation of the form

$$\sum_{n=0}^{n=p} a_n\frac{d^nH}{dt^n}=\sum_{n=0}^{n=p} b_n\frac{d^nH}{dt^n} \tag{80}$$

in the Heaviside method or an equation of the form

$$a_0\int_{-\infty}^{\infty}f(t)\phi(t)dt+\sum_{n=1}^{n=p}a_n\phi^{(n-1)}(0)$$
$$=b_0\int_{-\infty}^{\infty}f(t)\phi(t)dt+\sum_{n=1}^{n=p}b_n\phi^{(n-1)}(0) \tag{81}$$

in the Schwartz method.

From this we can conclude that the coefficients of the nth derivative of H on each side of the equation are equal in the Heaviside method, while in the Schwartz method $a_0=b_0$ and the coefficients of $\phi^n(0)$ on each side of the equation are equal, so that

$$a_0=b_0$$
$$a_1=b_1$$
$$\cdots\cdots$$
$$a_n=b_n \tag{82}$$

This is quite analogous to the case of the complex variable where if x,y,a, and b are real and we are given

$$x+iy=a+ib \tag{83}$$

we can conclude that

$$x=a,\ y=b \tag{84}$$

The methods in this paper follow very closely the ideas expressed by Heaviside in the following quotation,[6] which seems an appropriate one with which to conclude:

> "It may be remembered that I have insisted on the definiteness and fullness of the meaning of an operational solution, and that it contains within itself not only the full statement of the problem, but also its solution. No external aid is therefore required to algebrise it fully; no assumption, for instance, of a special type of solution and that the solution is the sum of a number of that type with subsequent determination of the constants required to complete the matter. The work of satisfying the imposed conditions has been done already."

References

1. Generalization de la Notion de Function, de Derivation, etc., L. Schwartz. *Annales*, l'Universite de Grenoble, volume 21, 1945, page 57. Also, *Annales des Telecommunications*, (Grenoble, France) volume 3, number 4, April 1948, pages 1–6.

2. Electromagnetic Theory (book), Heaviside. Benn Brothers, Ltd. (London, England), volume 2, 1922, page 59.

3. Electrical Papers (book), Heaviside. Copley Publishers (Boston, Mass.), volume 2, page 226, equation 163, 1925.

4. Electromagnetic Theory (book), Heaviside. Benn Brothers, Ltd. (London, England), volume 2, page 139, equation 43, 1922.

5. The Extension of the Heaviside Expansion Theorem to the Equations of Engineering and Physics in Curvilinear Orthogonal Coordinates, J. J. Smith. *Journal*, Franklin Institute (Philadelphia, Pa.), volume 238, October 1944, page 245.

6. Electromagnetic Theory (book), Heaviside. Volume 2, page 122, paragraph 280.

7. Operational Methods in Mathematical Physics (book), H. Jeffreys. Cambridge University Press (Cambridge, England), 1927.

Discussion

P. A. Abetti (General Electric Company, Pittsfield, Mass): The authors are to be commended for their derivation of Heaviside's operational calculus which, among the many attempted so far, is mathematically rigorous, physically simple, and closely related throughout to Heaviside's own work. In fact, if we consider the simple applications to first and second order differential equations, we find a complete equivalence of every step of the Schwartz method with every step of the Heaviside method. For instance, in the section in the paper on second order differential equations, the following equations are equivalent: 59 to 51; 60 to 53; 62 to 54; 63 to 55; 64 to 56; 66 to 57; 67 to 58.

It will be recognized, however, that the equations 51 to 54 are simpler in form and easier to manipulate than equations 59 to 62, which contain infinite integrals and the minus signs introduced by equation 17. On the other hand, the Schwartz transformation equations are mathematically rigorous in the mass space, while the derivatives H', H'' of the unit function "do not exist" mathematically at the point of discontinuity and are zero elsewhere, so that the legitimacy of equations 50 to 54 is extremely doubtful.

It appears that a compromise between the rigorousness of the Schwartz method and the simplicity and elegance of the Heaviside would be very useful, especially for those who are concerned with the operational methods as working tools. Such a compromise may be obtained easily by writing all steps of the solution in the Heaviside manner, but adding a special symbol, say Ⓜ or Ⓢ, whenever derivatives of the unit function appear, to indicate that the operations take place in the mass space, or in the field of the Schwartz transform, and are therefore rigorous and legitimate.

Mr. Alger and Mr. Smith also must be commended, for psychological reasons, for having written d/dt instead of p everywhere in their paper. In fact, let us consider the reactions of the layman, practical engineer or student, to the introduction of the operational calculus in his daily work, as specified by Angelini.[1]

1. Some find the operational calculus very useful in its application to practical problems, especially in the electrical field.
2. Others do not believe that it is worthwhile to employ operational methods, either because they find no occasion in which these might be useful, or else because such occasions appear so seldom in their work that they consider the effort necessary to learn these new methods of calculation out of proportion with the limited use they would have for them.
3. Others finally are disappointed and puzzled by the many different aspects under which the operational calculus is presented to them, by the lack of mathematical rigour and ensuing pitfalls of some methods, and sometimes by the opinion of some mathematicians on the subject. They renounce therefore to learn and to employ the operational methods and make use, whenever necessary, of the classical methods of analysis.

Concentrating our attention to the last group, and eventually also to the second, and especially to those who have not yet formed their opinion on the subject, the operational methods may be presented to all these groups with emphasis on either one of the following two points wherein they basically differ from the classical ones:

1. The completeness of the solution which incorporates the initial conditions and eliminates the integration constants of the classical methods.
2. The "symbolic" property of writing p (or any other letter) for the operator d/dt, and treating this symbol as any other algebraic quantity.

Any person who has used the classical methods and wrangled with the calculations of several integration constants, will appreciate the great advantages of point 1 above. Very few engineers or students instead, are favorably impressed with point 2 as such, wherein they see no practical advantages for their work, and which they sometimes distrust as "illegitimate" from the standpoint of mathematical rigour. K. W. Wagner[2] attributes to this reason the fact that the first rigorous approach to operational methods contained in two very remarkable papers[3,4] written by Giorgi as far back as 1904–05, has found such poor reception among engineers.

The only point in the paper Messrs. Alger and Smith wrote which might draw the suspicion of the layman is the appearance of the symbol $\sqrt{RSd/dt}$ in equation 75. However, the introduction of fractional differentiation and integration, as Riemann[5] defined it in 1876, will clear these difficulties and we look forward to the announced paper on this subject.

Giorgi also first realized the great importance of the impulse function, which now, together with its derivatives, finds a legitimate place in the mass space through the transformation of Schwartz.

In view of the fact that the work of Giorgi is little known in this country and was not even quoted among the 84 references of a recent paper[6] on the history of the operational calculus, its accomplishments will be reviewed here.

On the basis of the work of Pincherle[7] and other mathematicians on the properties of distributive operations, Giorgi justifies the application of the algebraic rules to the operator $d/dt = \Delta$ and establishes the limits of their validity.

The operational expression of Giorgi is

$$i(t) = \psi(\Delta)v(t) \tag{1}$$

in which the operator is a function, eventually irrational, of the symbol Δ and $v(t)$ is a "physical" function, that is, which satisfies Dirichlet's conditions. Giorgi interprets equation 1 by resolving the function $v(t)$ in its frequency components by the Fourier integral, applying to each component the operator, and obtains the results by superposition. We arrive thus at a general formula, called "Giorgi's integral," for the evaluation of operational expressions.

For practical applications, instead of using Heaviside's and Carson's "indicial admittance," the response to a unit step function H, Giorgi employs the response to a unit impulse function ΔH, that is, Green's function $g(t)$

$$g(t) = \psi(\Delta)\,\Delta H \tag{2}$$

The response to the function $v(t)$ is obtained as the sum of the responses to an infinite number of impulse functions in which $v(t)$ has been resolved

$$\psi(\Delta)v(t) = \int_{-\tau}^{+\infty} g(t-\tau)v(\tau)d\tau \tag{3}$$

Changing the variable from $(t - \tau)$ to θ in equation 3 we obtain the equivalent expression

$$\psi(\Delta)v(t) = \int_0^\infty g(\theta v(t-\theta)d\theta \tag{4}$$

Giorgi has proved, even for discontinuous functions, the relation

$$\epsilon^{h\Delta}v(t) = v(t+h) \tag{5}$$

so that equation (4) may be written as

$$\psi(\Delta)v(t) = \int_0^\infty g(\theta)\epsilon^{-\theta\Delta}v(t)d\theta \tag{6}$$

Wherefrom, since equation 6 is valid for any $v(t)$, we obtain

$$\psi(\Delta) = \int_0^\infty g(\theta)\epsilon^{-\theta\Delta}d\theta \tag{7}$$

Under certain conditions, equation 7 expresses the fact that Green's function $g(\theta)$ is the inverse Laplace transform of the operator $\psi(\Delta)$. If instead of Green's function, we write the initial admittance $A(\theta)$ in equation 7 we obtain, since $g(\theta) = \Delta A(\theta)$

$$\frac{1}{\Delta}\psi(\Delta) = \int_0^\infty A(\theta)\epsilon^{-\theta\Delta}d\theta \tag{8}$$

which is the same as the integral used by Carson (1922–26) as the foundation of his approach to operational calculus.

If we consider that these relations which correlate the principal methods of the operational calculus, were obtained as far back as 1905, seven years before Heaviside himself finished his great work and 11 before the appearance of Wagner's and Bromwich's justification of Heaviside's work, we must conclude that Giorgi's work deserves more recognition and study than it has been granted in the past 45 years.

References

1. Principles and Practical Value of the Operational Calculus Applied to Electrical Engineering, A. M. Angelini, *Elettrotecnica*, (Milan, Italy), 29 January 1942, pages 1–69.

2. Operatorenrechnung (Operational Calculus), K. W. Wagner. Johann Ambrosius Barth, (Leipzig, Germany), 1940, page 27.

3. The Symbolic Method in the Study of Variable Currents. G. Giorgi. *Atti Associazione Elettrotecnica Italiana*, (Milan, Italy) 1904, pages 65–141.

4. On the Calculation of Functional Solutions of Electrodynamical Problems. G. Giorgi. *Atti Associazione Elettrotecnica Italiana*, (Milan, Italy) 1905, pages 651–99.

5. Gesammelte Werke, B. Riemann. (Leipzig, Germany) Edition 1 (1876), page 331; Edition 2 (1892), page 353.

6. The History and Present State of the Operational Calculus as Applied in Electric Circuit Analysis, T. J. Higgins. *Electrical Engineering*, January 1949, pages 42–45.

7. Le Operazioni distributive e le loro Applicazioni all' Analisi (Distributive Operations and their Applications to Analysis), S. Pincherle, U. Amaldi. *U. Zanichelli*, (Bologna, Italy) 1901.

J. J. Smith and **P. L. Alger:** We thank Mr. Abetti for bringing out clearly the equivalence between the equations using the Schwartz method and the Heaviside method.

In writing the paper we started out with the idea of using only the Schwartz method but it became apparent that if this method alone were used the paper would not be easy for engineers accustomed to the Heaviside method to understand. We therefore adopted the compromise of using both the Heaviside method and the Schwartz method on the same problems, so it is hoped the equivalence between the steps in both methods is apparent from this procedure.

While the Schwartz method is simple to use when one is accustomed to it and is more rigorous, nevertheless, it does entail considerably longer expressions than those involved in the Heaviside method. Here, Mr. Abetti makes a good suggestion. He says, why not write, for instance, equation 29 as follows:

$$(C+C_1\epsilon^{-Rt/L})LH'+CRH=EH \quad Ⓜ \quad (29)$$
$$\text{or} \quad Ⓢ \quad (29)$$

where the symbol before the number of the equation means (1) the mass space equivalent Ⓜ or (2) the equivalent representation of Schwartz Ⓢ is to be understood. This equation can be translated into the Schwartz terminology by noting that since H' is zero everywhere except when $t=0$, the equation can be written putting $t=0$ in the exponential

$$(C+C_1)LH'+CRH=EH$$

and the mass space representation of this equation is then given by

$$L(C+C_1)\phi(0)+CR\int_{-\infty}^{\infty}H\phi(t)dt=$$
$$E\int_{-\infty}^{\infty}H\phi(t)dt$$

which is equation 41.

In the more general case of the nth order derivative, we have

$$\frac{d^n}{dt^n}(f(t)H)=\sum_{p=0}^{p=n}f^{(p)}(0)H^{(n-p)}$$

in the Heaviside method. Whereas in the Schwartz method taking the representation of $f(t)$ H in mass space we have

$$\int_{-\infty}^{\infty}\frac{d^n}{dt^n}[f(t)H]\phi(t)dt$$
$$=(-1)^n\int_{-\infty}^{\infty}f(t)H\phi^{(n)}(t)dt$$
$$=(-1)^n\int_{0}^{\infty}f(t)\phi^{(n)}(t)dt$$
$$=\int_{0}^{\infty}f^{(n)}(t)dt+\sum_{p=1}^{p=n}(-1)^{p-1}f^{(p)}(0)$$
$$\phi^{(n-p)}(0) \quad (9)$$

By comparing these two formulas, any equation written in the Heaviside notation can be rewritten readily in the Schwartz notation by replacing

$f(t)H$ by $\int_{-\infty}^{\infty}f(t)H\phi(t)dt$

$f(0)H'$ by $f(0)\phi(0)$

$f(0)H^{(n)}$ by $(-1)^{n-1}f(0)\phi^{(n)}(0)$

With regard to the symbol $\sqrt{RSd/dt}$ in equation 75, it will be noted that both the numerator and denominator are odd functions of $\sqrt{RSd/dt}$ and thus this term cancels out leaving functions of RSd/dt so that no fractional differentiation is involved in this particular example. However, as mentioned in a footnote, we shall discuss fractional differentiation in a subsequent paper.

With regard to Mr. Abetti's reference to Giorgi's work, there is much to be said in favor of using the response of a system to a unit impulse, rather than the response to a unit function (indicial impedance). The resulting expressions are often much simpler and more symmetrical and in many cases can almost be written down on sight. A close examination of the superposition theorem will show that the differentiation involved in it is really used to derive the response to an impulse function from the indicial impedance and the subsequent integration corresponds to the summation of the effects of a large number of such impulse functions to simulate the given function whose reaction it is required to determine. Of course, the main objection to its use up to now has been the "nonexistence" of the impulse function in the mathematical sense. The introduction of the Schwartz method should rectify this and justify the very elegant derivation of the Laplace integral given by Mr. Abetti. We have given an even simpler derivation in a paper just completed basing it similarly on the properties of the unit function and its derivative.

In connection with this nonexistence in the mathematical sense of H', $H^{(n)}$, and so forth, and which also leads Mr. Abetti to point out "the legitimacy of equations 50 to 54 is extremely doubtful," there may be a possibility of handling such entities in the Heaviside manner without resorting to the Schwartz method. This involves consideration in the Stieltjes manner of the differentials dH, $d^2H,\ldots d^nH$ which exist, rather than the derivatives H', H'', $\ldots$ $H^{(n)}$ which do not exist. It is, perhaps, premature to raise hopes in this respect, although the authors hope to write up some ideas on this in the near future. In the meantime, it might be well if students of engineering were introduced to Stieltjes integrals and functionals so as to be able to study Heaviside's work from these points of view.

Heating of Transformers Under Short-Circuit Conditions

V. M. MONTSINGER
MEMBER AIEE

G. H. HALSEY
ASSOCIATE AIEE

THIS PAPER discusses the effect of turn insulation on the temperature rise and the relative aging of this insulation during the heating and cooling periods. It is shown: that the absorption of heat by the insulation cannot be neglected in accurate calculation of temperature rise; that most of the aging occurs during the cooling period; and that the allowable temperature limit should depend upon the rate of cooling.

This paper gives a formula for calculating temperature rise that allows for the effect of insulation. This formula is easier to use than those given in the ASA C57 Standards for Transformers, Regulators and Reactors.[1] It is pointed out that the present formulas in the standards are cumbersome to use and do not factor the absorption of heat by turn insulation. It is also pointed out that additional work is necessary before any revision of the ASA standards can be undertaken.

General Considerations

For many years, the AIEE transformer standards, and, in more recent years the ASA standards, have specified that short-circuit temperature rises be calculated without allowing for heat storage capacity of the winding insulation. Furthermore, while the standards have made no definite statement about it, it has been the general understanding that the duration of the short-circuit and the ultimate temperature reached are the most important factors to be considered in determining the life of the insulation.

When a short circuit is applied to a winding, all the heat is stored in the copper at the very beginning. As the temperature increases, heat is transferred to the insulation and later to the oil adjacent to the insulation. The insulation may comprise both turn-to-turn and layer-to-layer insulation.

The maximum temperature that is important is that temperature existing in the insulation when the aging starts to be a measurable amount. This is illustrated in Figure 1, in which it is assumed that the temperature of the copper reaches 250 degrees centigrade before any heat is absorbed by the insulation, at which time the short circuit is removed. The cooling takes place in two stages: firstly, the conductor insulation absorbs its share of the heat very rapidly, reducing the temperature of the copper to, say, 220 degrees centigrade in three to five seconds. Secondly, the cooling from this point on takes place by dissipation of the heat to the oil at a relatively slow rate, in terms of minutes instead of seconds, during which time most of the aging takes place. For practical purposes then, the temperature during the slow-cooling period is far more important than the maximum copper temperature attained up to the time when the slow rate of cooling starts in.

The ASA Standards C57.12.050-.051[2] have two limitations under short circuit. One limitation (mechanical) is that the magnitude of the symmetrical current must not exceed 25 times normal current. The other limitation (thermal) is that the maximum copper temperature must not exceed 250 degrees centigrade when using class *A* insulation.

This paper is concerned only with the thermal limitation for transformers using class *A* insulation. The authors will show:

1. That turn-to-turn insulation should be factored when calculating the storage of heat in transformer windings.
2. That, for a given maximum temperature, the aging of the insulation resulting from a short circuit depends on the rate of cooling rather than on the duration of the short circuit.
3. That, to produce the same thermal effect, the maximum temperature should vary with the rate of cooling.

Tests to Determine Temperatures During Heating and Cooling Cycle

To obtain heating and cooling data, many tests were made on transformer windings of the disk-coil type having three sizes of conductors with different amounts of turn insulation, as shown in Table I.

The tests were made with the coils in both horizontal and vertical positions and in oil at temperatures ranging from 24 to 75 degrees centigrade. These tests provided two kinds of data: firstly, a comparison of the tested and calculated rises (based on all heat stored in the copper) during the heating period; and secondly, the thermal capacity of the turn insulation that applied during both the heating and cooling periods, under loads ranging from moderate overloads (run until conditions became constant) to short-circuit conditions until temperature rises as high as 200 degrees centigrade were reached.

HEATING PERIOD

Figure 2 is typical of tests made on the three sizes of conductors. This illustra-

Table I

Test Series Number	Conductor Size, Inches	Turn Insulation—Both Sides, Inches	
		Paper*	Cotton Wrap
1	0.435×0.095	0.012	0.007
2	0.450×0.075	0.024	0.007
3	0.500×0.100	0.066	0.007

* 0.003 Manila paper.

Paper 49-189, recommended by the AIEE Transformer Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20–24, 1949. Manuscript submitted March 14, 1949; made available for printing May 5, 1949.

V. M. MONTSINGER and G. H. HALSEY are with the General Electric Company, Pittsfield, Mass.

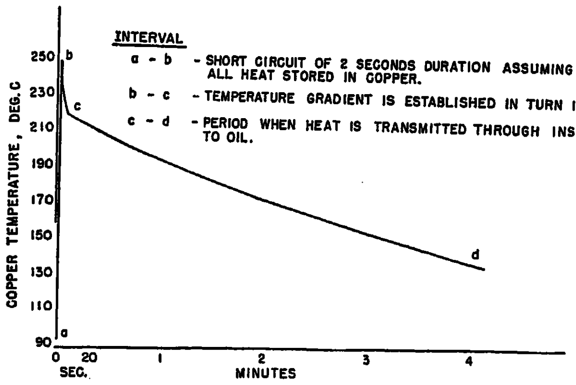


Figure 1 (above). Heating and cooling of typical oil-immersed transformer winding when subjected to short-circuit

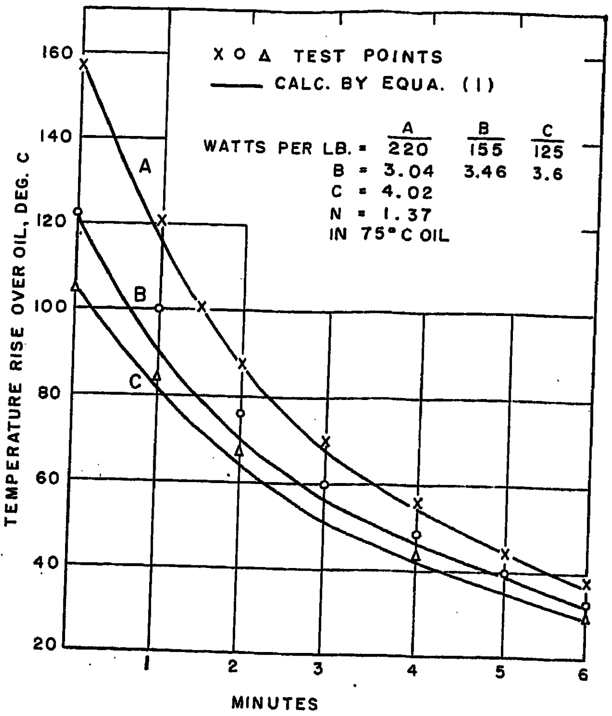


Figure 3 (right). Cooling curves of winding having conductor 0.450 by 0.075 inch, *P.024-C.007*, two sides, insulation

tion shows curves of tested and calculated temperature rises during 40-second short circuits when 400, 500, and 550 amperes were forced through the 0.450- by 0.075-inch conductor with *P.024-C.007* insulation in 30-degree-centigrade oil. It will be noted that the solid lines representing the test values fall below the dashed lines representing the calculated values assuming all the heat stored in the copper. For example, at the end of ten seconds duration, the percentage of generated heat that remained in the copper was as shown in Table II.

Results of similar tests reported in Figures 1 and 2 of reference 4, made on 0.435- by 0.095-inch *P.012-C.007* insulated conductor, are seen in Table III.

It appears safe to assume that at least 20 per cent of the total heat generated will always be stored in the insulation of an average winding.

Cooling Period

Figures 3 and 4 show a comparison of tested and calculated cooling of windings having 0.450- by 0.075-inch *P.024-C.007* conductors in which the temperature rise at the start of the cooling ranges from approximately 100 to 200 degrees centigrade. The cooling was calculated by the following equation

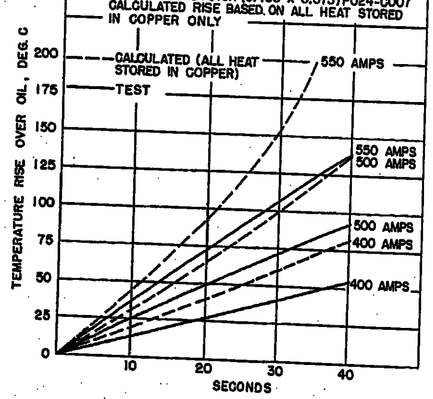


Figure 2. Tested and calculated temperature rises during 40-second duration in 30-degree-centigrade oil

$$\theta=\theta_0\left[1+(n-1)\frac{T}{\tau}\right]^{\frac{-n}{n-1}} \tag{1}$$

where

θ = temperature rise of winding, degrees centigrade, at any time, T
θ_0 = temperature rise of winding at start of cooling period
n = exponential power of ultimate temperature rise (θ_0^n) versus loss
T = time after start of cooling—minutes
$\tau=\dfrac{\theta_0 C}{W_0}$
C = specific heat of copper plus insulation
W_0 = watts per pound necessary to maintain a steady-state temperature rise of θ_0

After the current density of the copper to maintain the temperature rise, θ_0, is determined, W_0 can be calculated from

$$W_0=KD^2 10^{-6}$$

where

K = 2.16 for copper at 25 degrees centigrade
D = amperes per square inch

Based on the limited data available, the authors have used for several years the thermal capacity of turn insulation by volume as one-half that of copper, that is

$$C=2.96+1.48\frac{A_i}{A_c} \tag{2}$$

where

A_i = cross section of insulation
A_c = cross section of copper

The value of 2.96 for copper corresponds to a specific heat of 0.0935 calorie per gram per degree centigrade. As the specific heat of copper increases slightly with temperatures, the value of 3.04, instead of 2.96, was used in AIEE Standards Number 32.[3] Also, when the Standards for Neutral Grounding Devices, were under preparation, the data at that time indicated that "by volume" the thermal capacity of turn insulation was 86 per cent that of copper. That is,

$$C=3.04+2.62\frac{A_i}{A_c} \tag{3}$$

A thorough study of all tests made on the three different amounts of turn insulation previously mentioned, including both the heating and cooling cycles, indicates that the thermal capacity of turn insulation is by volume approximately 67 per cent (or 2 A_i/A_c) that of copper.

Rounding out the 2.96 and 3.04 values to 3.00 which is easily remembered, the equation which best fits the test results is

$$C=3.00+2.0\frac{A_i}{A_c} \tag{4}$$

Table II

Amperes	Per Cent Heat Retained in Copper in First Ten Seconds
400	65
500	78
550	80

Table III

Amperes	Per Cent Heat Retained in Copper in First Ten Seconds	
	Figure 1	Figure 2
400	70	60
500	70	72
550	68(500 amperes)	70

which allows 67 per cent "by volume" for the turn insulation. See Table IV for comparison of tested and calculated values of temperature rises using equations 3 and 4.

According to equation 1, the rate of cooling is determined largely by the time constant which ranges from approximately 3 to 15 minutes for most transformer windings at normal operating temperatures. When windings attain short-circuit temperatures, the time constant is reduced, since the steady-state temperature rise does not vary directly with the loss. For example, equation 8 of Appendix II of ASA C57.32[1] states that the time constant varies as the 0.25 power of the ratio of the temperature rise at rated load to any higher rise. That is, if the coil rise over oil at rated load is 20 degrees centigrade when the rise over oil under short circuit reaches (250 degrees–75 degrees oil temperature =) 175 degrees centigrade, the time constant will be decreased to $(20/175)^{0.25}=0.58$ of that at 20 degrees centigrade rise. Therefore, time constant of 15 minutes at rated load will be 15 by 0.58 = 8.7 minutes, or approximately one-half that at rated load.

Aging of Insulation During Heating and Cooling Periods

Now, having discussed both the heating and cooling during a short-circuit cycle, we can now determine what percentage of the total aging takes place during the heating cycle.

As shown previously,[5] the aging of insulation during an abrupt rise in temperature and slow cooling can be calculated by integrating the area of the heating and cooling curve either by using the equation

$$A = te^{XT_2} \tag{5}$$

where

A = aging units
t = time expressed in seconds, minutes, or hours
X = constant (0.0865 when the aging doubles for each 8 degrees centigrade increase in temperature)
T_2 = maximum temperature, degrees centigrade

or, by forming a trapezoid the area of which is approximately the same as the temperature area, and using the equation

$$A = t\left[\frac{e^{XT_2}-e^{XT_1}}{X(T_2-T_1)}\right] \tag{6}$$

When $X=0.0865$, equation 6 reduces to

$$A = t\left[\frac{\log_{10}^{-1}(0.0375\ T_2-0.0375\ T_1)}{0.0865\ (T_2-T_1)}\right] \tag{7}$$

where

T_2 = maximum temperature of trapezoid
T_1 = lower temperature of top line of trapezoid. The values of T_1 should be such that the aging is negligible for T_1 and lower values
t = time corresponding to length of top line of trapezoid

Figure 5 shows curves constructed of two heating periods of five and ten seconds, also, three cooling curves for windings having 2-, 4- and 8-minute time constants (at 250 degrees centigrade).

As the aging with which we are concerned here is the ratio of the aging during the heating period to the aging during the cooling period, time, t, can be expressed in any convenient units. Letting $T_2=175$ degrees centigrade and $T_1=100$ degrees centigrade for the 5-second heating period, t is approximately 2.1 seconds.

By equation 7

$$A = 2.1\left[\frac{\log_{10}^{-1}(0.0375\times175-0.0375\times100)}{0.0865(175-100)}\right] = 1.2\times10^6 \text{ units}$$

Since the heating curve is almost a straight line, the aging during the 10-second heating period is approximately 2.4 by 10^6 units.

Referring to Figure 5, it will be seen that the lapse of time from the point on the triangle *a b c*, when the coils having a time constant of two minutes reach 100-degree rise on the 5-second heating period and decrease to 100-degree rise on the cooling period, is 59 seconds. Experience has shown that the integration of triangle *a b c* gives approximately the same results as the integration by parts of the entire heating and cooling area *a b d*. The aging during this complete cycle can also be calculated by equation 7, that is

$$\text{Aging} = 59\left[\frac{3{,}630{,}000-5600}{0.0865(175-100)}\right] = 32.9\times10^6 \text{ units}$$

Therefore, the aging during the 5-second heating period is

$$\frac{1.2\times10^6\times100}{32.9\times10^6} = 3.65 \text{ per cent of the total aging}$$

The percentage of aging for coils with time constants greater than two minutes

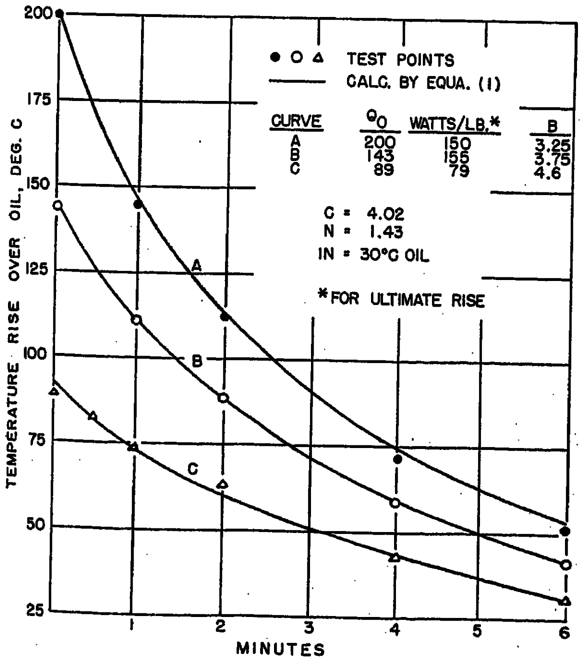


Figure 4 (left). Cooling curves of winding having conductor 0.450 by 0.075 inch, P.024-C.007 insulation

Figure 5 (below). Heating and cooling curves of windings with 2-, 4-, and 8-minute time constants at 250 degrees centigrade or approximately 4, 8, and 15 minutes at 95 degrees centigrade

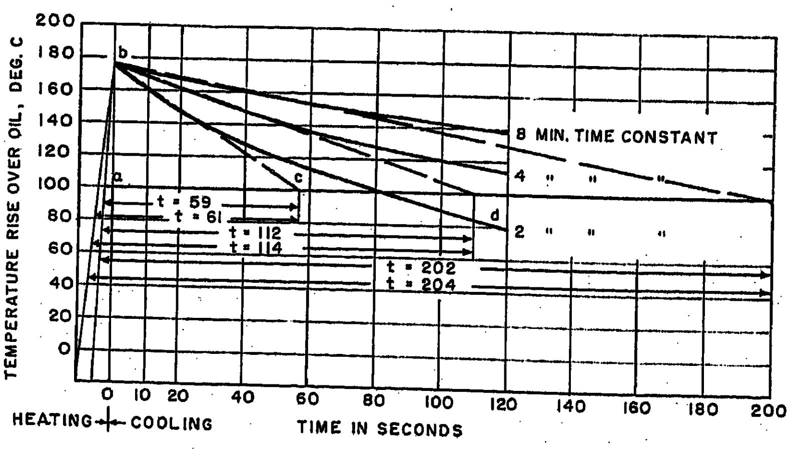


Table IV. Summary of Heating-up Data Taken on Coils Having Three Sizes of Conductors

Conductor	Degrees Centigrade Temperature Rise Versus Time in Minutes									
	2 Minutes		5 Minutes		10 Minutes		Ultimate θ_u		Watts Per Pound at	
0.435×.095-inch *P.012-C.007*										
Horizontal coils										
Amperes	140	200	140	200	140	200	140	200	140	200
Test Rise	15.0	30.0	27.0	50.5	34.5	63.0	38.0	68.0	31	68
Calculated A* Rise	14.1	29.6	26.1	51.7	34.3	64.1	—	—		
Calculated B** Rise	13.5	28.4	25.3	50.4	33.8	63.5	—	—		
0.450×.075-inch *P.024-C.007*										
Horizontal coils in 30-degree oil										
Amperes	140	200	140	200	140	200	140	200	140	200
Test Rise	8.0	21.5	18.5	40.0	29.0	55.5	38.0	65.0	23	50
Calculated A* Rise	9.9	20.6	20.1	40.0	29.6	55.4	—	—		
Calculated B** Rise	9.2	19.2	19.0	38.0	28.4	53.8	—	—		
Vertical coils in 25-degree oil										
Amperes	100	140	100	140	100	140	100	140	100	140
Test	8.5	21.0	17.0	34.0	25.0	46.5	30.0	53.0	22	47
Calculated A*	9.2	18.9	17.9	35.4	25.2	47.1	—	—		
Calculated B**	8.5	17.6	17.0	33.7	24.4	46.0	—	—		
0.500×.100-inch *P.066-C.007*										
Horizontal coils in 70-degree oil										
Amperes	140	250	140	250	140	250	140	250	140	250
Test Rise	8.0	27.5	17.0	53.5	25.0	75.0	31.5	88.0	21.5	80
Calculated A* Rise	7.6	27.0	15.7	52.8	23.5	73.9	—	—		
Calculated B** Rise	6.8	24.3	14.4	48.8	22.2	70.6	—	—		
Vertical coils in 70-degree oil										
Amperes	140	250	140	250	140	250	140	250	140	250
Test Rise	8.5	24.0	17.0	52.5	25.0	72.0	30.5	85.5	22	76
Calculated A* Rise	7.7	25.7	15.8	50.6	23.4	71.3	—	—		
Calculated B** Rise	6.9	23.2	14.5	46.8	22.1	68.0	—	—		

* Based on $C=3.00+2.0\ A_i/A_c$. ** Based on $C=3.04+2.62\ A_i/A_c$.

will, of course, be less than 3.65 per cent because the cooling is at a slower rate.

Using the curves shown in Figure 5 and calculating the aging as illustrated, Table V shows the aging during the heating period in percent of total aging for coils having 2-, 4-, and 8-minute time constants and for 5- and 10-second heating periods.

From Table V, it is seen that for heating periods up to ten seconds practically all the aging under short-circuit conditions occurs during the cooling period, which depends on the time constant of the windings. From a thermal standpoint, the duration of a short-circuit of any reasonable period can therefore be neglected.

Method of Specifying Temperature Limits

When AIEE Standard Number 32 for Neutral Grounding Devices was under revision during 1946 and 1947 by the AIEE Subcommittee on Protective Devices (Mr. J. E. Clem, Chairman), it became evident from the data given in reference 4 that the temperature limits during a fault of one minute's duration or less should be a function of the rate of cooling after the fault had been removed rather than the length of time during the fault. This was especially true for transformers having a wide range of cooling rates and time constants. For example grounding transformers may have either well-ventilated coils and rapid cooling rates with normal time constants or they may have practically no cooling ducts resulting in very slow cooling rates and large time constants.

Table V

Coil Time Constant, Minutes	Aging During Heating Period in Per Cent of Total Aging, for Heating Periods of	
	Five Seconds	Ten Seconds
2	3.65	7.05
4	1.92	3.77
8	1.06	2.1

Based on these considerations, the subcommittee established a table of allowable temperature rises versus minimum cooling rates for the first minute as shown in Table VI taken from Table 32.11B of AIEE Standard Number 32.

So far as is known, this is the first time that the heating period has been disregarded and the permissible maximum temperatures based on rate of cooling.

While the time constants of power-transformer windings that carry continuous load do not vary over the wide range that applies to grounding transformers, the time constants and rates of cooling do vary sufficiently to justify establishment of a range of permissible temperature limits.

Variation of Temperature Limit with Time Constant

The following steps are made to see how the temperature limit may vary for different time constants to produce approximately the same aging during a short circuit.

Table VI

Time Constant, Minutes	Time, *t*, Seconds	Relative Aging
2	59	1.0
4	112	1.9
8	202	3.42

In Figure 5, the relative aging for all three time constants will be proportional to the time, *t*, since T_2 and T_1 have the same values for all three curves.

For the 5-second heating and three cooling periods, the aging is as in Table VI.

Based on the aging doubling for each eight degrees increase in temperature, to produce the same aging in the three coils would require: a difference of 7.5 degrees centigrade between coil number *1* and coil number *2;* and a difference of 14 degrees centigrade between coil number *1* and coil number *3*. For a coil having a time constant of 16 at 250 degrees centigrade, the temperature limit should be approximately 25 degrees centigrade less than for coil number *1*.

Based on these data, if we assume that 250 degrees centigrade is a safe maximum temperature for all windings having a time constant of two minutes or less the figures given in Table VIII can be drawn up.

The temperature limit under short-circuit conditions for power transformers has been 250 degrees for the past 30 or 40 years. As pointed out previously, this temperature has been based on all heat being stored in the copper only. There is good reason to believe that a temperature of 250 degrees is seldom if ever reached under short circuit; firstly, because some of the heat is stored in the conductor insulation; secondly, full voltage is not maintained under short-circuit unless the

Table VII. Temperature Rise Versus Rate of Cooling of Neutral Grounding Devices

Table 32—2.11A—AIEE Standard Number 32. Starting Temperature, 75 Degrees Centigrade

Maximum Temperature Rise, Degrees Centigrade	Minimum Cooling—First Minute, Degrees Centigrade
125	30.0
117	15.0
109	7.5
101	3.8
93	1.9
85	0.9
77	0.5

Table VIII

Approximate Winding Time Constant in Minutes		Maximum Temperature to Produce Approximately Same Aging, Degrees Centigrade
At 250 Degrees Centigrade	At 95 Degrees Centigrade	
2 or less	3.5 or less	250
4	7.0	242.5
8	14.0	236
16	28.0	225

kilovolt-ampere capacity of the transformer in question is small in comparison with the source of power; and thirdly, the protective relay generally comes into action before the time limit is reached. If an allowance is made for the storage of heat in the conductor insulation, the question naturally arises as to whether 250 degrees is the proper value or should it be reduced to obtain approximately the same temperature as has been attained in the past. For example, if we assume that the temperature rise of the windings during short circuit is, say, 80 per cent of the calculated value based on all heat stored in copper only, this means that if the initial temperature is 95 degrees, the actual temperature attained would be (250−95) 0.8+95=219 degrees centigrade instead of 250 degrees centigrade.

Formula for Calculating Storage of Heat

Equations 22.105(a) and (b) given in the ASA Standards C57.22[1] do not include the effect of insulation in the storage of heat. Furthermore, these equations are cumbersome to use, especially 22.105(a).

The equation given in paragraph 32–3.11A of AIEE Standard Number 32 allows for storage of heat by the conductor insulation. This equation, however, expresses the copper loss in watts per pound instead of amperes per square inch which term is more convenient to use. With zero eddy loss and when expressed in amperes per square inch this equation reduces to

$$\theta = T_0\left[\log_{10}{}^{1}\left(\frac{6D^2t10^{-11}}{C}\right)-1\right] \qquad (8)$$

where

θ = temperature rise in degrees centigrade
T_0 = starting temperature absolute (234.5 $+\theta_0$)
θ_0 = starting temperature in degrees centigrade
D = amperes per square inch
t = time in seconds
C = specific heat of copper plus insulation
$C=3.00+2.0\dfrac{A_t}{A_c}$
A_t = cross section of turn insulation
A_c = cross section of copper conductor

In equation 32–3.11A of AIEE Standard Number 32 the eddy loss is lumped in with the I^2R loss watts per pound. This assumes that the eddy loss varies directly with the absolute temperature the same as I^2R varies.

In ASA equation 22.105(b), the eddy loss varies inversely with the absolute temperature.

If an empirical eddy-loss factor is put in equation 8, very good agreement is obtained with ASA equation 22.105(b) over a wide range of amperes per square inch and per cent eddy loss. With an eddy-loss factor introduced, equation 8 becomes

$$\theta = T_0\left[\log_{10}{}^{-1}\left(\frac{6D^2Et10^{-11}}{C}\right)-1\right] \qquad (9)$$

where

$$E=\left(1+\frac{e}{100}\right)^{0.8}$$

e = eddy loss in per cent of I^2R at 75 degrees centigrade

Table IX. Comparison of Results

Initial Temperature = 95 Degrees Centigrade

Amperes Per Square Inch	Time, Seconds	Eddy Loss, e, Per cent	Comparison of Copper Temperatures Calculated By: AIEE Number 32 Equation 32.3.11A*	Equation 9 in Paper	ASA Equation 22.105(b)
	2	0		123.0	123.0
30,000	2	20		128.1	128.5
	2	40		132.8	133.5
	2	0		147.5	147.5
40,000	2	20		157.1	156.0
	2	40		165.3	164.5
	5	0		127.0	127.0
20,000	5	20		131.8	132.5
	5	40		137.2	137.5
	5	0	170.5	170.5	170.5
30,000	5	20	189.0	184.2	183.5
	5	40	217.0	197.7	195.0
	5	0	241.8	241.8	241.8
40,000	5	20	280.0	270.0	264.5
	5	40	322.0	298.7	284.5
	10	0	161.5	161.5	161.5
20,000	10	20	177.4	173.7	172.5
	10	40	193.8	185.1	182.5
	10	0	264.0	264.0	264.0
30,000	10	20	311.0	297.4	290.5
	10	40	361.0	332.3	315.5

* Using $C=2.96$ instead of 3.04 for copper only.

Table IX gives a comparison of the results obtained by three equations to show the effect of eddy loss without factoring the turn insulation:

1. 32–3.11A from AIEE Standards Number 32.
2. Equation 9 in paper.
3. 22.105(b) from ASA Standard C57.22.

A comparison of the values shown in Table IX shows:

1. With zero eddy loss, the three equations give the same results.
2. With eddy loss present and for temperatures up to 250 degrees centigrade, the values calculated by equation 9 give results quite close to those given by ASA equation 22.105(b).

Since equation 9 is easy to use, it is recommended that it replace ASA equation 22.105(a) which is cumbersome to use and does not allow for the absorption of heat by the insulation.

Effect on Transformer Design of Changing Short-Circuit Temperature Rules

The question naturally arises as to the effect on transformer design of changing the short-circuit temperature rules. The present limiting temperature of 250 degrees centigrade calculated on the basis of all heat stored in the copper is easily met in present-day designs, except in very rare cases. However, if the copper densities are increased as improved methods of cooling are developed or by other design requirements, more accurate calculation of temperatures resulting from short-circuit conditions may be required.

Future Work

The data presented in this paper were obtained on disk types of windings. Other factors which may materially affect the rate of cooling are additional insulations not present in disk windings, such as layer insulations between multiconductor layers and windings wound directly on cylinders. Future investigations also should determine the effect of storing heat in the oil flowing up through tall windings where the oil temperature may be increased greatly for an appreciable time before it leaves the coil stack following a short circuit.

While these factors will probably have a small effect on the storage of heat, they may have a decided effect on the rate of cooling. When these factors have been investigated and reported, the question of revising the ASA standards to put them on a more logical basis, should be considered by the AIEE Transformer Committee.

References

1. AMERICAN STANDARDS FOR TRANSFORMERS, REGULATORS, AND REACTORS. American Standards Association (New York, N. Y.), C57, 1948.

2. AMERICAN STANDARD FOR DISTRIBUTION, POWER, AND REGULATING TRANSFORMERS, AND REACTORS OTHER THAN CURRENT-LIMITING REACTORS. American Standards Association (New York, N. Y.), C57.12, 1949.

3. NEUTRAL GROUNDING DEVICES. AIEE Standard Number 32, 1947.

4. TEMPERATURE LIMITS AND SHORT-TIME OVERLOADS FOR OIL-INSULATED NEUTRAL GROUNDING REACTORS AND TRANSFORMERS—PART II, V. M. Montsinger, J. E. Clem. *AIEE Transactions*, volume 65, 1946, pages 966–73.

5. TEMPERATURE LIMITS FOR SHORT-TIME OVERLOADS FOR OIL-INSULATED NEUTRAL GROUNDING REACTORS AND TRANSFORMERS, V. M. Montsinger. *Electrical Engineering* (*AIEE Transactions*), volume 57, January 1938, pages 39–44.

Discussion

V. V. Mason (The Hydro-Electric Power Commission of Ontario, Toronto, Ontario, Canada): For estimating the loss of life due to a short time overload the authors give their equation 5

$$A = te^{XT_2} \qquad (1)$$

For the more elevated temperature where the rate of loss of life doubles for each rise of 8 degrees centigrade, this formula holds, but only for constant T_2. For varying temperatures, the integral form must be used, namely

$$A = \int e^{XT\,dt} \qquad (2)$$

In the authors' equation 6, an approximation is made to the above integral by assuming that the cooling curve is a straight line. This approximation, gross as it may seem at first glance, is probably not so bad in most practical cases if cleverly made. However, it is sometimes difficult to decide just how clever an approximation one has made.

The purpose of this discussion is to introduce a new type of graph paper which we call "loss-of-life" paper. Use of this paper avoids the uncertainty attendant upon making the approximation used by the authors. This loss-of-life paper has a linear time scale and a nonlinear temperature scale, such that the distance along the Y-axis is proportional to the rate of loss of life. Thus the area under any curve representing a temperature cycle is proportional to the loss of life for that cycle.

An additional advantage which accrues from the use of this paper is that account may easily be taken of the fact that the rate of deterioration halves for smaller and smaller temperature decrements as the temperature falls towards 95 degrees centigrade. That is, we may take into account the nonlinearity of the curves of per cent loss of life versus hot-spot temperature when plotted on semilog paper as in Figure 4 of Appendix 1 of the American Standards for Transformers, Regulators, and Reactors. It was the rate of loss of life figures calculated from these American Standards Association curves which were used in the preparation of this special graph paper.

Figures 1 and 2 of this discussion show samples of this type of paper. Figure 1 is for moderate overloads going up to a maximum of 131 degrees centigrade, while Figure 2 is for more severe overloads going up to 183 degrees centigrade.

This loss-of-life paper not only avoids the approximations otherwise necessary but also makes the procedure almost entirely graphical. This is especially so if it is used in conjunction with the transient heating and cooling papers which have been described in previous literature.[1,2]

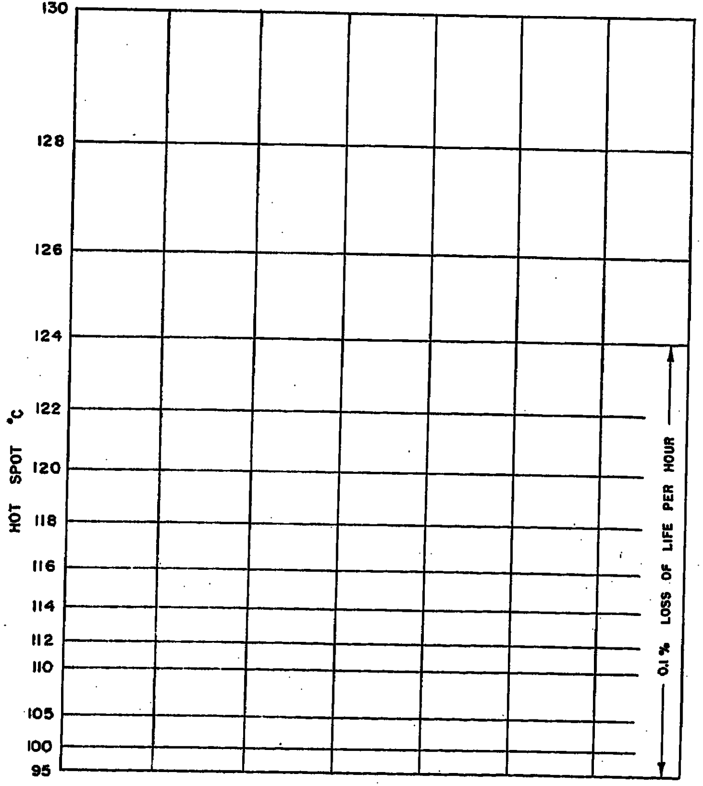
REFERENCES

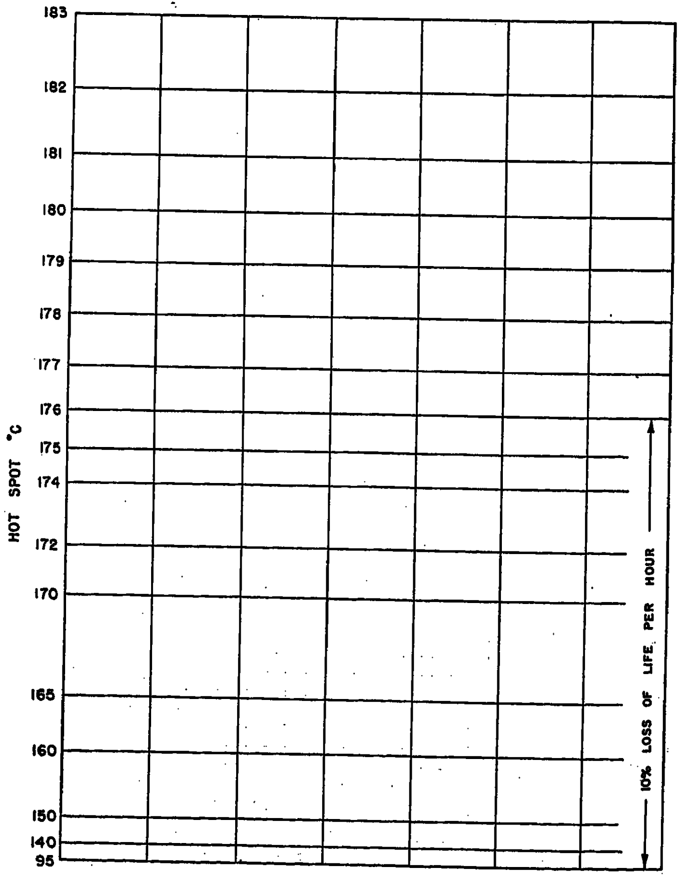
1. SIMPLIFIED METHOD OF CALCULATING TRANSFORMER TEMPERATURE RISE UNDER VARIABLE LOADS, W. C. Sealey. *Allis Chalmers Electric Review* (Milwaukee, Wis.), December 1939, pages 11–15.

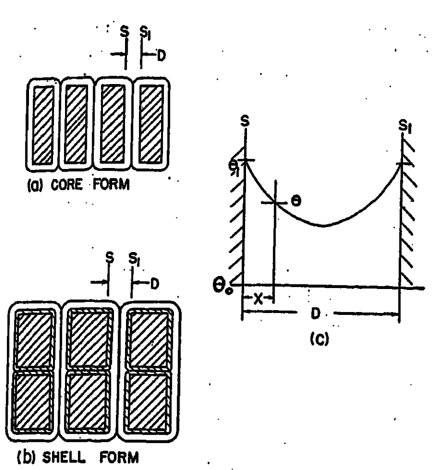
2. SIMPLIFIED GRAPHICAL METHOD OF COMPUTING THERMAL TRANSIENTS, Paul Narbutovskih. *AIEE Transactions*, volume 66, 1947, pages 78–81.

Figure 1 (below). Transformer loss-of-life paper with moderate overload

Figure 2 (right). Transformer loss-of-life paper with severe overload







(a) CORE FORM

(b) SHELL FORM

(c)

Figure 3. Arrangement of copper and insulation in core and shell form transformers

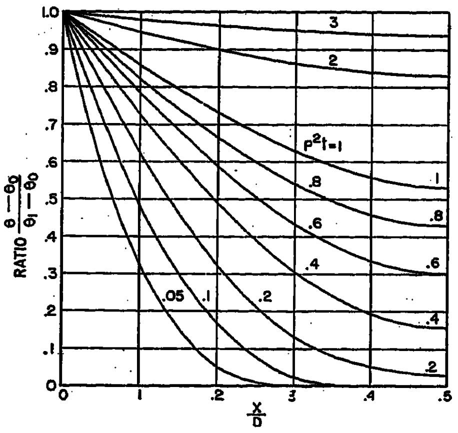


Figure 4. Transient temperature rise in insulation between conductors

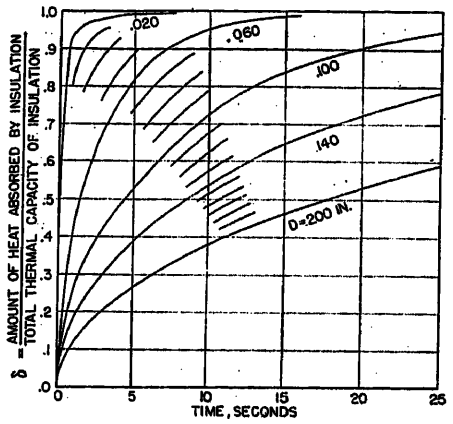


Figure 5. Heat absorbed by conductor insulation under transient conditions

Paul Narbutovskih (Westinghouse Electric Corporation, Sharon, Pa.): In their paper the authors have brought up for consideration by industry and the utilities a subject which has been maturing for some time. The basic thesis—the relation of the permissible winding temperatures in transformers under very short time overloads to the duration of the cooling period rather than the heating period—is fundamentally sound, and has been recognized for a number of years, although until now it has not been proposed for inclusion into the industry standards for transformers. Our knowledge of the insulation life under exposure to high temperature is still imperfect. So long, however, as the available knowledge has been applied to the formulation of permissible overloads on transformers, it can be applied to the formulation of permissible practices for short circuit conditions. From this point of view the paper is undoubtedly a step in the right direction.

The inclusion of the thermal capacity of insulation into calculation of the windings temperature under short-circuit conditions is likewise entirely sound. The quantitative effect of the insulation, however, may merit further consideration, particularly in view of the fact that on Figure 2 of the paper the calculated temperature rise for 550 amperes appears to be erroneously too high. A correction of this curve will increase the per cent heat stored in copper to about 95 or 97 per cent instead of the 80 per cent quoted in the paper for the first 10 seconds. A further insight into the effect of the insulation upon the final temperature reached by the copper and the insulation can be gained by considering the diffusion of heat from copper into the insulation under transient conditions.

Consider Figure 3 of the discussion, which shows a cross section of a typical conductor and insulation arrangement for core and shell form transformers. Upon occurrence of a short circuit, a certain amount of heat is rapidly imparted into copper, raising its temperature. During the heating period and during the first part of the cooling period the heat from the copper diffuses into the insulation, the rate of this depending upon time, thermal diffusivity, and the thickness of the insulation. Intuitively, one may expect that on extremely rapid temperature rises all heat will be stored in copper alone, the insulation temperature remaining unchanged. In the next moment insulation immediately adjacent to the copper assumes the same temperature as the copper, the temperature of the remaining part of the insulation being unchanged. Then gradually, as the heat diffuses into the insulation, its temperature increases, while that of copper decreases. Simultaneously, the process of heat loss by convection to oil begins.

When the duration of the short circuit is finite, it appears that the two processes, that of heat generation in the copper, and the heat diffusion into insulation, are simultaneously present, the result of which will be that the copper will never attain the temperature corresponding to all heat stored in copper. However, if the thermal transient in the insulation is of long duration compared to the duration of the short circuit, the insulation will progressively play a decreasingly smaller part as the duration of the short circuit decreases. Hence, in the calculation of the copper temperature the effect of the thermal capacity of insulation must be related to the duration of the short circuit.

The required quantitative relation may be obtained by considering heat diffusion from two parallel surfaces, S and S_1, whose temperature was suddenly changed from θ_0 to θ_1, into a slab of insulation, included between them of thickness D, whose initial temperature is θ_0, Figure 3(C) of the discussion. It can be readily appreciated that this corresponds closely to the insulation included between turns, or between layers in cylindrical windings. Under this condition the transient temperature within the slab is given by a differential equation

$$\frac{\partial\theta}{\partial t}=\alpha\frac{\partial^2\theta}{\partial x^2} \qquad (1)$$

where

θ = temperature at time t minutes and at point distant x inches from surface S

$\alpha=\frac{K}{\rho c}$, is the thermal diffusivity of the insulating material, square inches, divided by minutes

K = thermal conductivity of insulation, $\frac{\text{watts}}{\text{inch}\times\text{degrees centigrade}}$

ρ = density of insulation, pounds per cubic inch

c = thermal capacity, $\frac{\text{watt minutes}}{\text{pounds, degrees centigrade}}$

Solution of equation 1, satisfying the necessary boundary conditions, is

$$\theta=\theta_1-(\theta_1-\theta_0)\frac{4}{\pi}[\epsilon^{-p^2t}\cdot\sin qx+$$
$$\epsilon^{-(3p)^2t}\cdot\sin 3qx+\epsilon^{-(5p)^2}\cdot\sin 5qx+\ldots \qquad (2)$$

Where

$$p=\frac{\pi\sqrt{\alpha}}{D},\quad q=\frac{\pi}{D}$$

Equation 2 is presented graphically, on Figure 4 of the discussion, as a family of curves.

Under the transient temperature distribution the effective thermal capacity of insulation is reduced by a factor δ, whose value is equal to the ratio of the mean and the maximum temperatures obtained from any one of the curves, Figure 4. Figure 5 of the discussion gives values of factor δ, as defined above, calculated for the following values of thermal conductivity, capacity, and density which apply reasonably well to oil-soaked manila paper insulation

$$K=0.0042\ \frac{\text{watts}}{\text{inches, degrees centigrade}}$$

$$\rho=0.035\ \frac{\text{pounds}}{\text{cubic inches}}$$

$$c=18\ \frac{\text{watt minutes}}{\text{pounds, degrees centigrade}}$$

As can be seen from the curves of Figure 5 of the discussion, the heat penetration into insulation is quite rapid for relatively thin insulation. Thus for a heating period of five seconds, and for insulation thickness less than 0.050 inch, the insulation is more than 90 per cent effective in absorbing the heat from the copper. This is not so for heavier insulation, for which the time lag in absorbing the heat from the copper may be quite considerable. Curves of Figure 5 may be used in the calculation of the copper temperature rise, if factor δ from Figure 5 is introduced into equation 4 of the paper

$$c=3+2\delta\frac{A_i}{A_c} \qquad (3)$$

Incidentally, the thermal capacity of insulation implied in this equation agrees well with the information available to the writer.

Equation 3 above, combined with a modified equation 32–3.11A of the AIEE Standard Number 32 gives a rational and a conservative basis for calculating the temperature rise of copper and adjacent insulation under short circuit conditions. This appears to be preferable to an assumption that 80 per cent, or what it may be, of the temperature rise calculated by equation 32–3.11A occurs under all conditions. In regard to equation 32–3.11A and its kin, involving a misleading and somewhat ancient antilogarithm notation, it is suggested that it be modernized into a directly usable exponential form

$$\theta = T_o\left[10^{\frac{W_o t}{mCT_o}} - 1\right]$$

or

$$\theta = T_o\left[\epsilon^{\frac{W_o t}{CT_o}} - 1\right] \qquad (4)$$

for equation 32–3.11A, and

$$\theta = T_o[\epsilon^{At} - 1] \qquad (5)$$

with

$$A = (1.38)10^{-10}\,\frac{D^2E}{C} \qquad (6)$$

for equation 9 in the paper. In the above ϵ is the base of natural logarithms.

V. M. Montsinger and G. H. Halsey: The paper was intended to present two important facts for further consideration. First, temperature rises under short-circuit conditions are considerably less than those calculated on the basis of all heat being stored in the copper. Second, the winding time constant governs the rate of cooling and the amount of aging occurring in the insulation. An empirical allowance is used for the insulation in equation 4 to fit the test points for heating periods up to ten minutes. As must be expected, the formula is limited in usage to the range of insulations and to the boundary conditions of the test. A rigorous mathematical solution must contain a number of factors that are not immediately apparent. Some of these are: thermal contact resistance between the insulation and the copper; thermal conduction from the insulation through a stagnant layer of oil next to the insulation; and heat transfer rates across these two boundaries that are functions of temperature.

A solution embodying some of these features has been made and was thought to be too involved to be easily used. The solution presented by Mr. Narbutovskih may be found in various textbooks[1] for the assumed boundary condition that the inner face of the insulation in contact with the copper reaches ultimate temperature at time $t=0$. As a result of this assumption, one calculates a more rapid heat flow into the insulation than actually exists. The insulation temperature will not rise as fast as indicated in Figures 4 and 5 of his discussion. Effects of the process of heat transfer through the stagnant oil layer and subsequent convective heat transfer to the main body of oil are included in the empirical formula of the paper.

The paper suggests that since most windings with normal turn insulation reach only 80 per cent of the temperature rise calculated on the basis of all heat stored that this figure be used in setting a new temperature limit of 220 degrees centigrade instead of 250 degrees centigrade and that future calculations be made on a more rational basis.

We note that Mr. Narbutovskih has suggested the use of the exponential form of equation 32–3.11A of AIEE Standard Number 32, and we recommend the use of amperes per square inch instead of watts per pound for the convenience of the designer and to be sure that eddy losses are included properly. When watts per pound is used, the figures must be corrected for the decrease of eddy loss with temperature. The principal objection one may have to the empirical factor for eddy loss as expressed by E in equation 9 is that the formula is approximate and not mathematically correct. Reference to Table VIII will show that the results obtained are accurate enough for all practical purposes.

We wish to thank Mr. Narbutovskih for pointing out an error in Figure 2 of the paper. The 550-ampere curves should have been labeled for 600 amperes. References made to 550-ampere tests in Tables II and III should be changed to 600 amperes. The exponent in equation 1 should be changed to $-1/(n-1)$. In Figures 3 and 4, the letter B should be changed to τ and the watts per pound value of 155 in Figure 3 should be changed to 255.

The loss-of-life paper suggested by Mr. Mason should prove helpful in future computations.

REFERENCE

1. HEAT TRANSMISSION, W. H. McAdams. McGraw-Hill Publishing Company, New York, N. Y., second edition, 1942, pages 29–30.

Standardization of Reactor Ratings

F. H. KIERSTEAD
MEMBER AIEE

J. L. THOMASON
MEMBER AIEE

REACTORS are used widely for numerous applications in power and other equipment and they are accepted apparatus for limiting fault currents in power systems. Repetitive manufacturing offers economies which can best be realized in reactors when definite physical sizes are frequently duplicated. Before the physical sizes can be duplicated, it is necessary to standardize the ratings of reactors. There are several items of ratings which normally appear on the nameplates, and some of the most pertinent items such as voltage, current, and inductances will be discussed further. While it is advisable to standardize the ratings of all types of reactors that are made in quantity, for illustrative purposes only current-limiting reactors will be considered here. The advantages obtained from the standardization of other types of electric equipment are largely applicable to reactors.

Voltage Classes

The voltage rating of a current-limiting reactor establishes two features: the insulation strength for voltage stress; and the mechanical strength for short-circuit stress.

The latest issue of the Report of the Joint Committee of the Edison Electric Institute and the National Electrical Manufacturers Association "Preferred Voltage Ratings for A-C Systems and Equipment"[1] establishes the nominal system voltages and their range at substations and on transmission systems. These, then, are the voltages to which reactors are subjected in the general application. Hence, these voltages should establish the voltage ratings of current-limiting reactors as listed in Table I.

Current Classes

The continuous current ratings of current-limiting reactors have been established by past usage. Table II shows the present standards for circuit breakers, current transformers and reactors, and also, the recommended reactor current ratings. Included are current ratings that would be established by the preferred series of numbers, 60 per cent and 25 per cent steps; and it is recommended that the use of the 60 per cent steps be emphasized to reduce the number of standard sizes. It is recommended that current classes other than those tabulated be considered specials. However, it is recommended that the specials be placed in preferred sizes as 12 per cent and 6 per cent steps. The short-time current rating of current-limiting reactors is well established by ASA C57.16–1948[2] by the maximum allowable temperatures of 350 degrees centigrade, class *B*, or 250 degrees centigrade, class *A*, for maximum current for five seconds. For neutral grounding devices AIEE Standard 32[3] gives rated time and temperatures rise for short-time ratings. The rating of current-limiting reactors could be matched with the continuous and 5-second rating of circuit breakers for the minimum cost of this natural combination. Further study will be made on this.

Inductance Unit—The Ohm

The reactor is fundamentally an inductance coil and the fundamental unit of inductance is the henry. However, system engineers like an easier applied unit such as the ohm or per cent *IX*. For standardization purposes, fractions of the henry would be best as the rating in ohms is directly proportional to the frequency. If the reactor is rated in per cent *IX* it will have an added variable of circuit volts which may vary from the standard classes. Since a large percentage, possibly 95 per cent, of the current-limiting reactor applications are at 60 cycles, it is recommended that current-limiting reactors be rated in ohms at 60 cycles.

The relation between per cent *IX* and ohms at 60 cycles is given in Figure 1 which shows the relation

$$\text{Ohms} = \frac{\text{per cent } IX}{100} \times \frac{\text{circuit volts}}{\text{amperes } \sqrt{3}}$$

In Figure 1 the reactor amperes as ordinates and the ohms as abscissas are plotted on log-log paper. Two other variables are needed if the reactor is to be rated as per cent *IX;* these are shown as circuit volts and per cent *IX*. Four examples are given on Figure 1. Example 1 calls for 0.100 ohms and 300 amperes and the heavy lines indicate their intersection

Table I. Voltage Classes, Volts 60 Cycle

Nominal System Voltage*	Recommended Reactor Voltage Ratings: Nominal Voltage Rating	Recommended Reactor Voltage Ratings: Maximum Design Voltage Rating
600	600	625
2,400	2,400	2,750
4,160	4,160	4,760
4,800		
7,200	7,200	8,250
12,000		
12,470	12,470	13,700
13,200		
14,400	14,400	15,500
23,000	23,000	25,800
27,600	27,600	31,000
34,500	34,500**	38,000**
46,000	46,000†	48,300†
69,000	69,000	72,500
115,000	115,000	121,000
138,000	138,000	145,000
161,000	161,000	169,000
230,000	230,000	242,000
288,000	288,000	302,000
360,000	360,000	378,000

* From latest issue of Report of Joint Committee of the Edison Electric Institute and the National Electrical Manufacturers Association "Preferred Voltage Ratings of A-C Systems and Equipment"[1] up to and including 230,000 volts.

** Maximum for outdoor air insulated.

† Maximum for enclosed air insulated.

Paper 49-190, recommended by the AIEE Transformers Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20–24, 1949. Manuscript submitted March 21, 1949; made available for printing May 5, 1949.

F. H. KIERSTEAD and J. L. THOMASON are with the General Electric Company, Pittsfield, Mass.

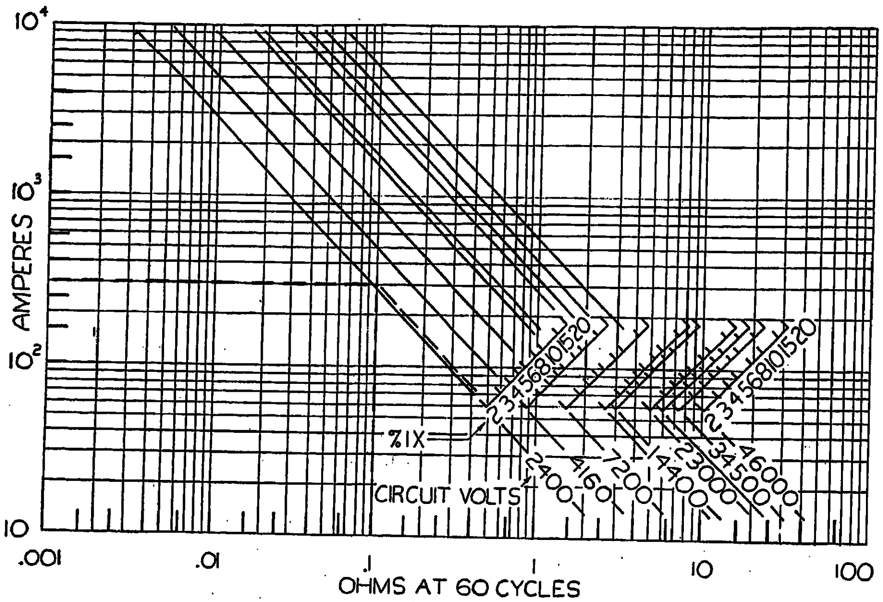


Figure 1. Reactor ratings, ohms and amperes by preferred sizes

$$\text{ohms} = \frac{\text{Per cent IX}}{100} \times \frac{\text{circuit volts}}{\text{amperes } \sqrt{3}}$$

Examples:

	Ohms	Amperes	Circuit Volts	Per cent IX
1.	0.100	300	2,400	2.2
2.	10.00	400	46,000	15.1
3.	0.250	2,000	14,400	6.0
4.	0.100	1,200	7,200	2.9

point. To obtain the per cent *IX* at 2,400 volts project this ohm-ampere intersection point parallel to the voltage line until it intersects the per cent *IX* line—in this case 2.2 per cent *IX*. Other circuit volts and per cent *IX*'s can be secured by interpolation. The kilovolt-ampere per phase is equal to $I^2X/1{,}000$; lines could be placed on Figure 1 to show this relation, but a small graph would become crowded. Figure 1 shows how logically the preferred series of numbers establishes both ohms and amperes.

Inductance Sizes—Preferred Steps in Ohms

It is recommended that the inductance values of current-limiting reactors be expressed in terms of ohms at 60 cycles by sizes according to the preferred series of numbers, ASA-Z-17, 1-1936.[4] Table III shows a range of values which amply cover most applications. The use of the 60 per cent steps should be encouraged. Experienced system engineers would probably find that ratings in the 60 per cent and 25 per cent steps could generally be used and that other ratings could be considered as special cases. However, the special cases should be handled in a logical manner as by 12 per cent and 6 per cent preferred sizes. It might be helpful to note that in the preferred series of numbers, 60 per cent steps means that between two consecutive steps there is a difference of 60 per cent, for example, 100 amperes and 160 amperes. Furthermore, the 60 per cent steps correspond to the five series (that is, 5 steps between decimal units such as 10 and 100), 25 per cent to the 10 series, 12 per cent to the 20, and 6 per cent to the 40.

Rating Target

To show the ease with which current-limiting reactors with standard ratings could be applied and ordered, a typical rating target is shown in Table IV. The standard ratings in ohms and amperes are given. The minimum enclosure space required and other data could be tabulated. A separate sheet may be required for each circuit voltage class. This rating target shows that in comparison with an infinite number of special rated (or tailored-made) reactors that with preferred sizes the number of ratings become reasonable in the practical range of per cent *IX* of 3 to 10. On Table IV the X's indicate six ratings of ohms for each current, and for the nine ratings of current a total of 54 standard sizes of reactors for this voltage class. For the 10 voltage classes recommended in Table I for air insulated reactors, there would be 540 standard sizes of reactors. However, the lower and higher ratings are infrequently used, and thus, the number of frequently used standard sizes would probably reduce to 5 by 5 by 5 = 125.

Specifications for Current-Limiting Reactors

Current-limiting reactors should meet the following specifications:

1. Withstand the dielectric test in the rated insulation voltage class.
2. Withstand the short-circuit force in the rated circuit voltage class.
3. Not exceed allowable temperature rise for continuous and short-circuit ratings.
4. Meet the tolerance for variations in in-

Table II. Continuous Current Ratings, Amperes 60 Cycles

ASA C37.6–1945[5] Table 3 Circuit Breakers	ASA C57.13–1948[6] Table 13.017 Current Transformers	ASA C57.16–1948[2] Part 16.007 Reactors	ASA Z-17.1–1936[4] Preferred Numbers	Recommended Reactor Ampere Ratings: Preferred Sizes, 60 Per Cent Steps	Recommended Reactor Ampere Ratings: Additional Sizes if Required, 25 Per Cent Steps
	100	100	100*	100	
			125		125
	150	150	160*	160	
	200	200	200		200
		250	250*	250	
	300	300	315		315
400†	400	400	400*	400	
		500	500		500
600	600	600	630*	630	
800	800	800	800		800
			1,000*	1,000	
1,200	1,200	1,200	1,250		1,250
	1,500	1,600	1,600*	1,600	
2,000	2,000	2,000	2,000		2,000
			2,500*	2,500	
3,000	3,000		3,150		3,150
4,000	4,000		4,000*	4,000	

† NEMA 46-116-1946[7] (Part SG6-70) lists the same currents for circuit breakers except omitting the 400 amperes.

* 60 per cent steps (or five series), all are 25 per cent steps (or ten series).

Table III. Reactance Classes in Ohms at 60 Cycles by Preferred Numbers 0.001 to 100 Ohms

60	25	60	25	Per Cent Steps 60	25	60	25	60	25
0.00100	0.00100	0.0100	0.0100	0.100	0.100	1.00	1.00	10.0	10.0
	0.00125		0.0125		0.125		1.25		12.5
0.00160	0.00160	0.0160	0.0160	0.160	0.160	1.60	1.60	16.0	16.0
	0.00200		0.0200		0.200		2.00		20.0
0.00250	0.00250	0.0250	0.0250	0.250	0.250	2.50	2.50	25.0	25.0
	0.00315		0.0315		0.315		3.15		31.5
0.00400	0.00400	0.0400	0.0400	0.400	0.400	4.00	4.00	40.0	40.0
	0.00500		0.0500		0.500		5.00		50.0
0.00630	0.00630	0.0630	0.0630	0.630	0.630	6.30	6.30	63.0	63.0
	0.00800		0.0800		0.800		8.00		80.0
0.01000	0.01000	0.1000	0.1000	1.000	1.000	10.00	10.00	100.0	100.0

Table IV. Rating Target for Current-Limiting Reactors

Class: 14,400 Volts, 60-Cycles, 3-Phase, 0.10 to 10.0 Ohms

Ohms 60 Cycles Per Cent 60	Steps 25	Amperes 100	160	250	400	630	1,000	1,600	2,500	4,000
0.100	0.100								X	X
	0.125								X	X
0.160	0.160							X	X	X
	0.200							X	X	X
0.250	0.250						X	X	X	
	0.315						X	X	X	
0.400	0.400					X	X	X		
	0.500					X	X	X		
0.630	0.630				X	X	X			
	0.800				X	X	X			
1.00	1.00			X	X	X				
	1.25			X	X	X				
1.60	1.60		X	X	X					
	2.00		X	X	X					
2.50	2.50	X	X	X						
	3.15	X	X	X						
4.00	4.00	X	X							
	5.00	X	X							
6.30	6.30	X								
	8.00	X								
10.0	10.0									

Table V. Reactor—Enclosure Size Combinations

Type of Reactor	Enclosure, Minimum Dimensions Width	Depth	Height	Reactor Nominal Circuit Volts	Reactor, Maximum Kilovolt-Amperes
1. Current-limiting, self-cooled	—	—	—	600	—
	—	—	—	2,400	—
	—	—	—	4,160	—
	—	—	—	7,200	—
	—	—	—	12,470	—
	—	—	—	14,400	—
	—	—	—	23,000	—
	—	—	—	27,600	—
	—	—	—	34,500	—
	—	—	—	46,000	—
2. Current-limiting, fan-cooled	—	—	—	—	—

ductance as established by ASA C 57.16–1948[2] as minus 3 per cent plus 7 per cent, or in effect ±5 per cent. When the standardization is developed so that frequent duplications are manufactured, the actual variations will probably be less.

Standard Physical Sizes of Reactors and Enclosures

After the ratings of reactors have been standardized, the physical sizes should be standardized. Experience should establish the most practical designs to keep at a minimum material, floor space, heating, and so forth for the lowest cost. The definite trend toward the use of enclosures would allow a recommended practice to be set up between enclosure and reactor sizes.

It may be desirable to standardize the reactor enclosure sizes with the sizes of other metal-clad equipment. However, three items should be pointed out. First, in the past, metal-clad enclosure sizes have fluctuated from time to time. Second, if metal-clad enclosures are going to be standardized, the sizes should be established by definite steps (as six inches) in depth, width, and height, or the sizes should be established by the series of preferred numbers. Third, it must not be overlooked that reactors dissipate heat and that clearances must be provided for air circulation.

Enclosure sizes could be established by minimum height and inside dimension for maximum reactor voltage and kilovolt-amperes as suggested in Table V. Adequate strike distance and working space inside should be provided. Special sizes will probably be required occasionally.

Advantages of Reactor Standardization

Some of the advantages of reactor standardization are:

1. Acceptance of preferred ratings should eventually result in: reduced costs; shorter delivery time; better use of materials as tests and service on the same size of reactor will point to improvements.

2. Standardizing on ohms on a 60-cycle basis rather than on per cent *IX* will be very helpful in promoting preferred ratings.

3. The use of preferred sizes (as on a rating target) will allow reactors and enclosures to be designed and used in logical sizes. Enclosure sizes and designs can then be made to harmonize with other metal-clad equipment.

4. Voltage and current ratings will be in agreement with accepted standards.

References

1. Preferred Voltage Ratings for A-C Systems and Equipment. *Report*, Joint Subcommittee of Edison Electric Institute and National Electrical Manufacturers Association (New York, N. Y.), May 1949.

2. Current-Limiting Reactors, *C57.16–1948*. American Standards Association (New York, N. Y.), 1948.

3. Neutral Grounding Devices. *AIEE Standard Number 32*, 1947.

4. Preferred Numbers, *Z17.1–1936*. American Standards Association (New York, N. Y.), 1936.

5. Schedule of Preferred Ratings for Power Circuit Breakers, *C37.6–1945*. American Standards Association (New York, N. Y.), 1945.

6. Instrument Transformers, *C57.13–1948*. American Standards Association (New York, N. Y.), 1948.

7. Power Circuit Breaker Standards. National Electrical Manufacturers Association (New York, N. Y.), *Publication Number 46–116*, 1946.

Discussion

J. A. Elzi (The Commonwealth and Southern Corporation, Jackson, Mich.): There is no question as to the desirability of standardization of certain types of reactors which are used on power systems; particularly those used on distribution circuits to protect feeder voltage regulators, distribution equipment in underground vaults, and those used to limit fault currents and thus reduce shock to the system and circuit breaker duty. However, some of the recommended standard values given in this paper should be given further consideration.

Voltage Classes

It seems that since the EEI-NEMA preferred voltage report[1] stops at 230,000 volts, it is undesirable to include the higher voltages which still have not been standardized. In fact, for the type of standardization discussed in this paper, that is, reactors that are used in quantity, it appears that the standardization should terminate with the 23,000- or 34,500-volt rating.

Current Classes

The authors mention that continuous current ratings of current limiting reactors have been established by past usage and in Table II a comparison is made between present American Standard Association preferred reactor current ratings and the current ratings of circuit breakers and current transformers. It is not felt that the comparison is parallel. The latest ASA circuit breaker tables[2] start with a continuous current rating of 600 amperes and a large number of standard reactor ratings would be below this value. In the case of the current transformers, the current ratings are influenced by the requirements of instruments and relays and do not necessarily indicate the requirements for reactors. The applications where reactors would be used in numbers would be on distribution circuits and the current ratings chosen should be such as to meet the requirements of such circuits. It would seem very important to co-ordinate the reactor current ratings with the preferred current ratings of feeder voltage regulators. There is some evidence that the current ratings of regulators need revision, but it is felt that the current ratings of the regulators and reactors should be co-ordinated.

The authors recommend the use of ratings based on a preferred series of numbers. This is probably a good method of arriving at the steps in general, but from practical considerations it is thought that for purposes of standardization it would be advantageous to avoid such numbers as 160 amperes, 315 amperes, and so forth. For practical purposes these could just as well be 150 amperes, 300 amperes, and so forth.

Inductance Unit

The authors' recommendation that the ohm be used as the standard unit to express inductance would, it seems, be detrimental rather than helpful in promoting preferred ratings. Application engineers are interested in the percentage drop introduced by reactors and in most cases would have to convert ohms into per cent in making their calculations. The per unit or per cent method is widely used for circuits and other apparatus, and we feel that the use of ohms for standard reactors would be very unsatisfactory. It is true that for certain types of reactors the use of ohms is desirable, but these would fall into a minor class and need not affect general standardization.

Standard Physical Sizes

The authors have an excellent point in recommending standardizing of physical size. It appears that this should be entirely practicable and would certainly be welcomed by the users.

Reference

1. See reference 1 of the paper.

2. Preferred Ratings for Power Circuit Breakers, *C37-6-1949*. American Standards Association (New York, N. Y.), 1949.

N. E. Dillow (General Electric Company, Schenectady, N. Y.): The authors have shown that there are many advantages to be gained from reactor standardization. However, when the question of standardization is raised in any line of apparatus the question arises as to the limitations in the application of the standardized line. It therefore appears desirable to consider the proposals made by the authors from the application viewpoint.

The nominal voltage ratings for reactors listed in Table I of the paper are chosen from the listings in the EEI-NEMA report. Considering system application, current-limiting reactors will generally be installed where they are subjected to voltages tabulated in the EEI-NEMA report as the range of the tolerable zone for voltages at substations and on transmission systems. The values listed in Table I of the paper as the maximum design voltage rating agree with the maximum of the tolerable zone. Hence, the proposal should adequately meet the application requirements as to voltage ratings.

Current-limiting reactors generally are applied in series with circuit breakers and current transformers. It is for this reason, probably, that the authors have listed the present American Standard Association current ratings for these equipments in addition to the recommended reactor ampere ratings. There is reasonable correspondence among the columns, and from an application viewpoint, the ampere ratings listed in Table II of the paper as the preferred sizes (60 per cent steps) appear to offer sufficient choice of current ratings.

By habit, application engineers have considered the inductive size of a reactor as a per cent reactance. This follows easily from the calculations usually employed to determine the reactance to limit the short-circuit currents or to balance loadings on parallel circuits. However, the conversion of per cent quantities to basic ohmic values also is a step familiar to the application engineer as he must use such conversions in calculations involving wires and cables. Hence, such a proposal to rate reactors in ohms should be acceptable.

Consider an example for the application of the standardized ratings proposed in the paper: assume a 13.2 kv feeder, rated at 5,000-kva capacity. It is desired to limit the 3-phase short-circuit current in the feeder to 100,000 kva maximum. A short circuit on the bus is 250,000 kva. Specify the standardized reactor ratings.

Calculations:

$$\text{Equivalent bus reactance} = \frac{5{,}000}{250{,}000} = 2.0 \text{ per cent}$$

(5,000 kva base)

$$\text{Desired feeder reactance} = \frac{5{,}000}{100{,}000} = 5 \text{ per cent}$$

Reactor reactance required = 3 per cent
Base ohms (5,000 kva, 13.2 kv) = 34.85 ohms
Reactor ohms = 1.05
Reactor current = 219 amperes

Specify reactor as follows:

Nominal voltage rating = 14.4 kv (Table I)
Continuous current rating = 250 amperes (Table II)
Reactor reactance = 1.00 ohms (Table III)

These ratings are compatible with the application. The reactance specified is actually nearer the calculated value than the accuracy of the usual data justifies. In addition, manufacturing tolerances allowed would permit almost as large a variation as exists between the specified and calculated value.

The proposals of the paper would not place any limitation on the general application of current-limiting reactors.

F. H. Kierstead and J. L. Thomason: In also advocating the standardization of reactor ratings but with some controversial suggestions, J. A. Elzi has undoubtedly brought up points which other engineers and members of the AIEE, NEMA, and EEI may wish to consider. The standardization of reactor ratings will be satisfactory to all concerned only if first, there are enough ratings to cover the fields of applications and if second, there are few enough ratings so that economies may be realized.

Regarding circuit voltages, the discusser recommends a top limit of 23,000 or 34,500. Some such limit within a frequently used range would certainly form a practical starting point in the standardization program.

Regarding the current ratings of reactors, the problem again seems to be basically economics:

First, it would appear that if all apparatus carrying the same current had the same current rating, the cost of the whole group would be a minimum. Second, it is undoubtedly more economical to supply certain apparatus with a higher current rating than needed if this rating is a frequently used standard. Third, the use of current ratings based upon the 60 per cent steps by the series of preferred numbers gives a logical and wide choice of ampere ratings. In the future, as standards are changed, it is recommended that all apparatus be given ampere ratings according to the preferred series of numbers; subdivisions then would be logical and orderly. During or before this transition period it will probably be necessary to retain ampere ratings in the well-established "round number" classes; economies will result when sizes are frequently duplicated.

Regarding the inductance unit, the ohm is considered best for the reasons set forth in the paper. All applications can be calculated on a percentage basis. Then the reactor can be ordered on a standard ohm rating.

The standardization of the physical features of reactors is desirable but is largely dependent upon first achieving standardization of the ratings of the reactors.

Mr. Dillow has analyzed the standardization of reactor ratings from the application viewpoint. He has shown that there are several advantages and no limitation by such standardization.

Use of Thyrite in Power Transformers

J. R. MEADOR
MEMBER AIEE

ELECTROSTATIC shielding long has been recognized as an effective means of controlling the impulse-voltage distribution in transformer windings. Under certain conditions, the same, or a better, result can be achieved by the use of Thyrite.*

The development of Thyrite[1] was reported in 1930. This ceramic-bonded inorganic material, strong, stable, and possessing precise electrical characteristics, is characterized by its nonlinear resistance; it passes a current proportional to a relatively high exponent of the applied voltage. Developed as a material for lightning arresters, its versatility is attested by its many important applications. One of these is the part it takes in the internal protection and "shielding" of power-transformer windings.

The increase in complexity of power transformers, particularly from a circuit standpoint, has been so gradual as to be almost imperceptible. A comparison between the modern transformer and its 25-year-old counterpart, however, reveals startling differences. The simple 2-winding machine with a ten per cent tap range, typical of the vast majority of transformers formerly produced, now represents an ever-diminishing share of power-transformer production. On the other hand, the enormous increase in the use of autotransformers, regulating transformers, load-ratio-control transformers, and transformers with complicated winding arrangements and wide tap ranges has introduced to the designer new problems of winding protection against overvoltages. Thyrite, immersed in the transformer oil and shunting portions of the windings, has, in the past ten years, achieved a prominent position in the solution of these problems.

Although the use of Thyrite in transformers, as described in this paper, has proved eminently successful for effectively controlling overvoltages within the transformer, this advance does not eliminate the necessity for normal protection consisting of modern lightning arresters connected from line to ground in the station, and overhead ground wire shielding of the station structure and at least 2,500 feet of connected transmission lines adjacent to the station.

Thyrite Characteristics

Thyrite used in transformers is normally in the form of standardized disks, either $1^1/_2$ inches thick and 3 inches in diameter, or 3/4 inch thick and 6 inches in diameter (Figure 1). Its high mechanical strength, about 23,000 pounds per square inch in compression, and its electrical and thermal stability under pressure or vibration make for trouble-free performance.

The volt-ampere characteristic of transformer Thyrite is

$$I = KE^N$$

where I and E are the instantaneous values of current passed and voltage applied, respectively, and K and N are constants depending on the disk dimensions and constituents. The constant K can be controlled over a wide range of values.

The exponent N is usually of the order of 3.5, so that an elevenfold current increase through the Thyrite causes only a doubling of the voltage across it. Since the volt-ampere relationship is independent of time, it applies equally to alternating, direct, or impulse voltages and currents, as long as instantaneous values are considered.

While Thyrite can be used continuously at 110 degrees centigrade, and for short periods at 150 degrees centigrade, the limit maintained in transformers is the same as the winding hot spot temperature; that is, 95 degrees centigrade.

Application in Transformers

Thyrite is used within transformers to perform one or more of three important functions:

1. To limit the impulse voltage across the portion of the winding to which it is applied
2. To control the impulse-voltage distribution in the portion of winding across which it is connected.
3. To establish and fix the potential of momentarily or continuously "floating" windings. This application is not discussed in this paper.

In the first case, a portion of winding is shunted by a stack of Thyrite disks and the nonlinear character of the material is such that at normal power-frequency operating voltages, its resistance is sufficiently high so as to have no measurable effect on the transformer. When carrying high surge currents, however, its resistance falls and limits the voltage drop across it and across the connected portion of the winding to a predetermined safe level.

In some instances, additional protection is needed, and the Thyrite is called upon to serve its second function. It is a well-known fact that a transformer winding is a rather intricate network of inductances interturn and intercoil capacitances, and capacitances to ground. At normal operating frequencies, the capacitances can be disregarded and the winding considered as a nearly pure inductance with a uniform linear distribution of voltage throughout its length. Under impulse conditions, the capacitances become prominent and alter the voltage distribution. Thus, intermediate parts of the Thyrite-shunted winding may develop high voltage gradients even though the voltage between the ends of the

---

* Registered trade-mark of General Electric Company.

Paper 49-192, recommended by the AIEE Transformers Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20-24, 1949. Manuscript submitted February 10, 1949; made available for printing May 2, 1949.

J. R. Meador is with the General Electric Company, Pittsfield, Mass.

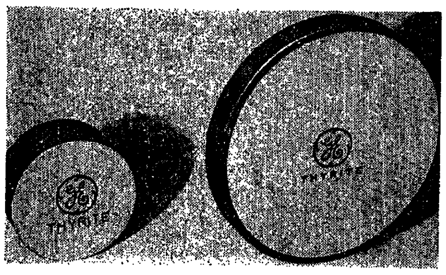


Figure 1. Thyrite disks: 3 inches in diameter and $1\frac{1}{2}$ inches thick; and 6 inches in diameter and 3/4 inch thick

winding is controlled by the Thyrite. If, now, intermediate points of the winding are connected to intermediate points of the Thyrite stack, the voltage distribution in the winding can be made to approach its normal power-frequency linearity. This follows from the fact that the Thyrite stack behaves as a pure resistance which is quite low under high transient voltages compared with the impedance of the shunted winding, and consequently it divides the voltage uniformly along the Thyrite and winding.

Before going on to a detailed examination of some of the applications, it is well to distinguish briefly between the employment of Thyrite in transformers and in lightning arresters. Gaps are not used in series with the Thyrite disks in the transformer; hence, the resistance of the Thyrite must be high enough at normal power-frequency operating voltage to limit the heat loss to a value easily dissipated into the oil. In valve-type lightning arresters, which utilize gaps to seal the power-frequency follow current after discharge of an impulse through the arrester, a lower Thyrite resistance at normal operating power voltage is permissible, since the Thyrite valve element is disconnected from the circuit except during, and immediately following, an impulse discharge.

Thyrite Across Taps in Transformers

Almost all transformers have taps in one or more windings for changing the turn ratio. In a simple 2-winding transformer with, say, four $2\frac{1}{2}$ per cent taps, the taps introduce no particular problem to the designer, since the only winding structures that are used are those that can be tapped. A line of standardized ratio adjusters is normally available to cover the usual range. The problem of the number and type of ratio adjusters to provide is a simple question of economics. It is obviously impractical to design a new ratio adjuster for every transformer. It is equally impractical to expect one or two ratio-adjuster designs to fill economically the needs of all transformers.

Ratio adjusters basically must meet two requirements:

1. The insulation between adjacent taps and across the tap range must withstand the continuous voltage to which it is to be subjected.
2. The insulation between adjacent taps and across the tap range must withstand the impulse voltage that appears in the portion of the winding to which it is connected.

Thus, a ratio adjuster normally has a double rating; (a) the voltage at which it can be continuously operated; (b) and a safe withstand impulse strength between its parts.

The method of application of the first rating is obvious. Its selection from the standpoint of the second rating depends on the insulation level of the winding, the percentage of the winding connected across the ratio adjuster, and the impulse-voltage distribution in the winding. Unless these conditions are known and met, there can be no assurance that the adjuster application is correct.

In very-high-voltage transformers or for abnormally large tap ranges, there is often a choice between using a high-voltage ratio adjuster in the normal way or using a lower-voltage ratio adjuster and shunting all or part of the tap range with Thyrite.

The choice is made on the basis of over-all economy. If a certain adjuster meets rating but is inadequate on rating, it is sometimes to advantage to shunt all or part of the taps with Thyrite and thereby bring the tap-range impulse requirements down to the rating of the adjuster.

Sometimes, because of some unusual design requirement, the impulse-voltage distribution in the tap winding may be difficult to calculate with a sufficient degree of accuracy. In such cases, the predictability of the Thyrite-shunted taps may justify the use of Thyrite.

The two usual types of tap connections and the shunting Thyrite are shown in Figure 2(A) and 2(B).

A typical example of the quantitative effect of the Thyrite shunt across the tap range is shown by the test points plotted in Figure 3. The 10.5 per cent tap range is shunted with Thyrite. By changing the number of disks, the impulse voltage across the taps can be held to almost any desired level as shown by the curve. In general, the Thyrite is selected to permit the tap range to have its normal share of the applied impulse voltage (that is a 10 per cent voltage for a 10 per cent tap range).

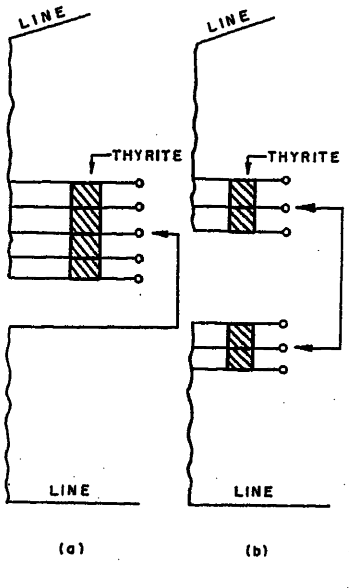
Autotransformers

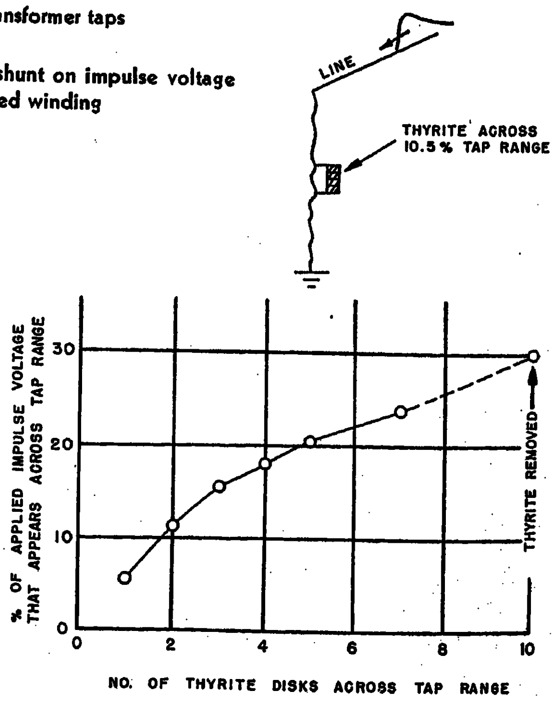
Thyrite is particularly useful in autotransformers. To explain these applications, it is necessary to mention briefly some of the rather exacting demands of autotransformer designs. From an insulation standpoint they are as follows (refer to Figure 4):

1. High-voltage (*HV*) taps may be for ten per cent of the terminal voltage, but if the co-ratio is low (such as in a 138-kv grounded wye to 115-kv grounded wye), the taps may be 30 to 50 per cent of the series winding.

Figure 2 (below). Thyrite across transformer taps

Figure 3 (right). Effect of Thyrite shunt on impulse voltage across taps in unshielded winding





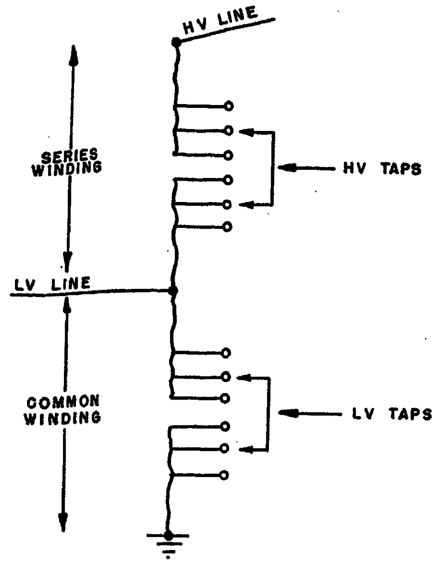


Figure 4. Autotransformer with taps in common and series windings

2. Although only a small percentage of the turns from the high-voltage terminal to ground may be in the series winding, a large percentage of the voltage appears across the series winding when an impulse voltage is applied to the high-voltage terminal. This is caused by the relatively low impedance from the low-voltage (*LV*) terminal to ground which results from connecting the low-voltage terminal to a transmission line or other similar load.

3. It is often desirable to place taps in the common winding immediately adjacent to the low-voltage terminal, rather than in the middle of the common winding as was shown in Figure 4.

4. The series winding may have an impulse voltage applied to either of its terminals. While this condition is the same as that in any delta-connected winding, nevertheless it introduces special problems when it occurs in combination with some or all of the aforementioned requirements.

The role of Thyrite in helping to meet these demands will be discussed in detail.

An example of items 1 and 2 is shown in Figure 5. An autotransformer rated 132-kv grounded wye to 62.7-kv grounded wye has taps for changing the voltage from 115.5 kv to 141.9 kv. While this range is 20 per cent of rated voltage, the taps are 33 per cent of the series winding. Impulse-voltage distribution tests were made for two different conditions: with the low-voltage terminal grounded; and with 630 ohms connected from the low-voltage terminal to ground. In both cases, the ratio adjuster was set on the minimum tap, and the impulse was applied to the high-voltage terminal. Under these conditions, the impulse voltages measured were as follows:

Resistance, Ohms, Low-Voltage Terminal to Ground	Per Cent of Applied Voltage Across Series Winding	Per Cent of Applied Voltage across Ratio Adjuster and Tap "Break"	
		With Thyrite	No Thyrite
630	80	31.5	51
0	100	29	63

The two conditions of "loading" the low-voltage terminal represent a wide range of service conditions. The Thyrite shunt across the taps holds the tap-range voltage to a relatively constant, normal magnitude. It should be added that the series winding in this case was well shielded electrostatically. The improvement brought about by the Thyrite would have been much greater in an unshielded winding since the tap voltage without Thyrite would have been much greater.

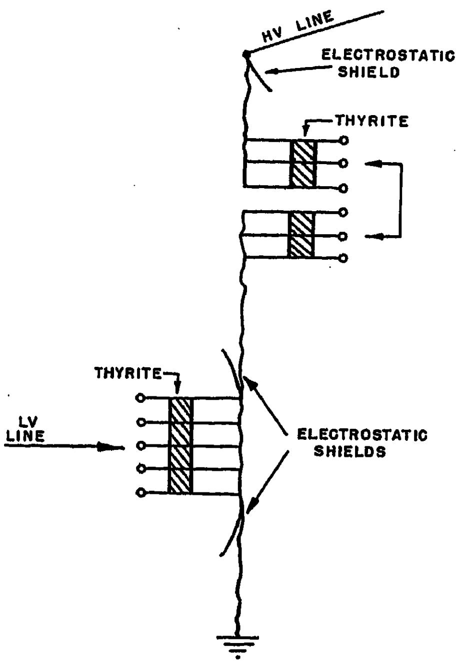


Figure 6. Autotransformer having common-winding taps located at the low-voltage line

Figure 4 shows taps located in the center of the common winding. Placing these taps immediately adjacent to the low-voltage line is a safe procedure if the taps are shunted with Thyrite as shown in Figure 6. Without a Thyrite shunt, this is a hazardous practice because of the high transient voltages that may exist across the tap coils, tap leads, ratio adjusters, and so on.

Furthermore, if the low-voltage line voltage is such that electrostatic shields are used, the Thyrite shunt permits the shields to be fully effective when connected at the extremities of the tap range as shown in Figure 6. When an impulse voltage is applied to the low-voltage terminal, the charging current to the shields is furnished through the Thyrite instead of through the series capacitance of the tap portion of the winding.

An extreme example of autotransformer taps is shown in Figure 7. In this case, two systems of essentially the same voltage were connected by this auto-

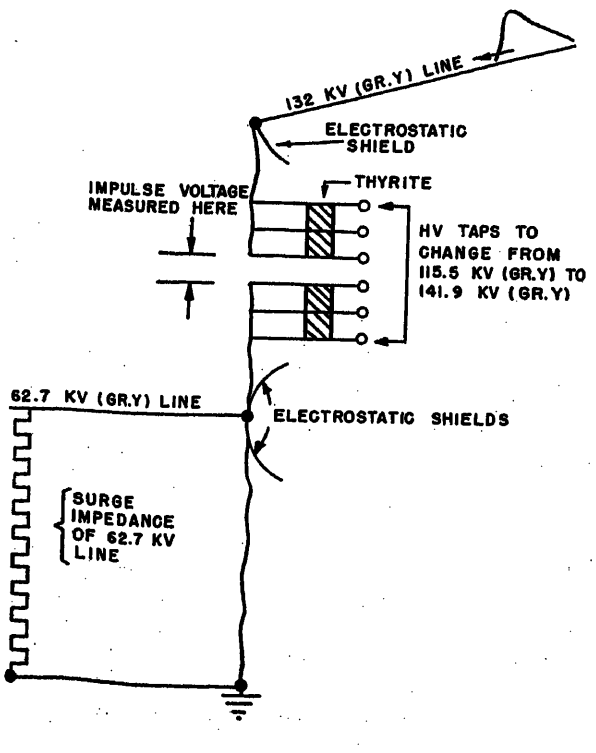

132 KV (GR.Y) LINE
ELECTROSTATIC SHIELD
IMPULSE VOLTAGE MEASURED HERE
THYRITE
HV TAPS TO CHANGE FROM 115.5 KV (GR.Y) TO 141.9 KV (GR.Y)
62.7 KV (GR.Y) LINE
ELECTROSTATIC SHIELDS
SURGE IMPEDANCE OF 62.7 KV LINE

Figure 5. (left). Autotransformer tested for voltage across high-voltage taps with impulse applied to 132-kv line

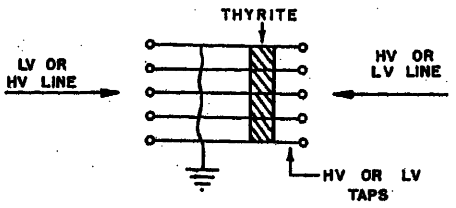


Figure 7 (right). Autotransformer for equal high-voltage and low-voltage tap range

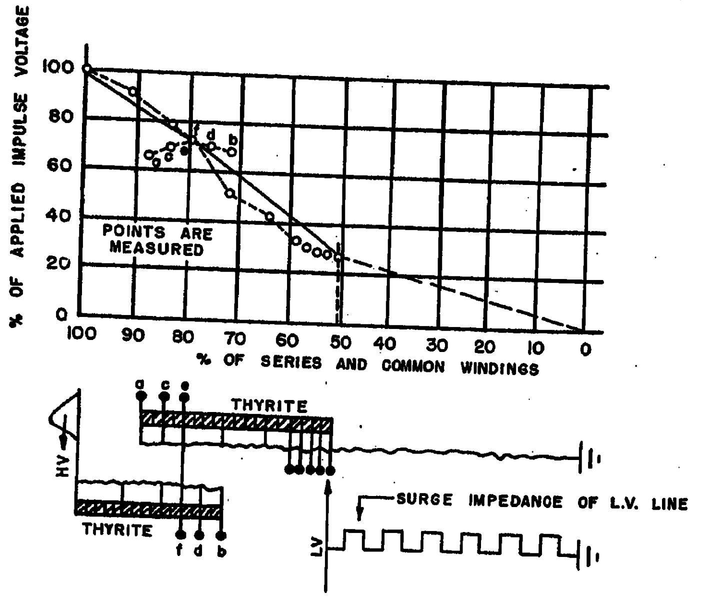


Figure 8. Initial impulse-voltage distribution in Thyrite-shielded series winding

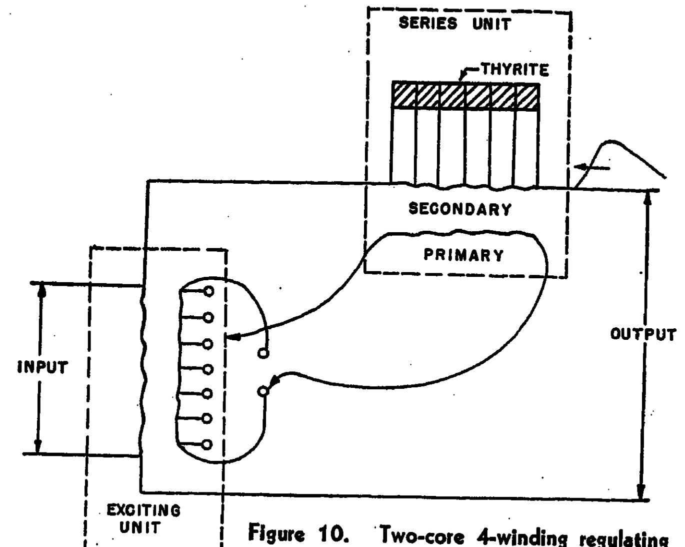


Figure 10. Two-core 4-winding regulating transformer

transformer with the expectation that power might flow in either direction. The problem here is similar to that illustrated by Figure 6. With a Thyrite shunt, taps can be placed at any location in a winding without fear of transient-voltage trouble.

Item 4 in the autotransformer design requirements concerns the impulse-voltage distribution in the untapped portion of the series winding. As has been shown, the major portion of the impulse voltage reaching the high-voltage terminal may appear across the series winding. Consequently, in medium- and high-voltage transformers, the series winding usually must be shielded.

In general, an electrostatic shield is used to nullify the ground capacitance of a winding so as to achieve a reasonably uniform impulse-voltage distribution in the winding. In this way, harmful concentrations of voltage are avoided. Shields are very effective and their application is straightforward in transformers of normal proportions.

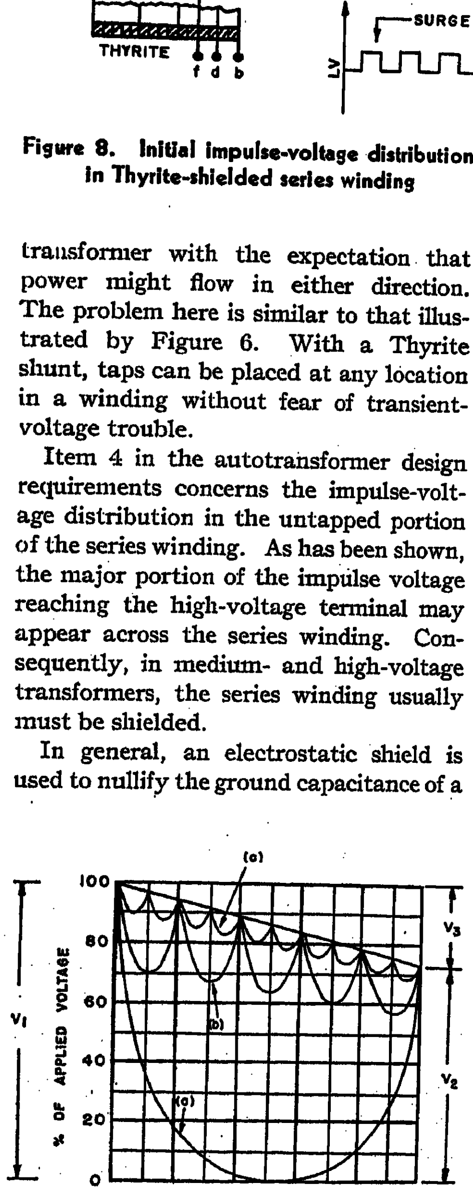


Figure 9. Effect of number of interconnections on initial impulse-voltage distribution in Thyrite-shielded series winding

Curve (A)—no interconnections
Curve (B)—four interconnections
Curve (C)—nine interconnections

Occasionally, the untapped portion of a series winding is so small physically or contains such a small number of turns that conventional electrostatic shielding is inadequate to maintain a reasonable impulse-voltage distribution. This condition is aggravated by an abnormally large tap range. Optimum shielding designed for one tap connection may leave much to be desired for a widely different tap connection.

Figure 8 shows how Thyrite has been used to advantage for such a condition. The conventional shields are omitted from the series winding, and the entire winding (including the taps at the low-voltage terminal) is shunted by Thyrite. By connecting the Thyrite and the winding together at numerous points, essentially straight-line initial voltage distribution is obtained throughout the winding in the circuit for all tap connections. In this case, with a surge impedance from low-voltage terminal to ground corresponding approximately to the surge impedance of the low-voltage line, some 70 per cent of the applied voltage appears across the series winding, although the turns in the series and common windings are about the same. It will be noted that the "overhanging taps" (with tap *e* connected to tap *f*) in the series winding that are not in the circuit all are held at essentially the same potential.

An interesting problem relating to the "shielding" of series windings by Thyrite concerns the necessity for interconnections between the windings and the Thyrite. It may be asked why the same result can not be obtained using an unshielded series winding and a lightning arrester connected across the terminals of the winding. The arrester will definitely limit the surge voltage appearing between the high-voltage and low-voltage terminals. It may, however, have little, if any, effect on the internal voltages (between turns and coils and between taps) in the winding. Figure 9 shows what happens.

Curve (A) of Figure 9 shows the initial impulse-voltage distribution in a series winding when an impulse voltage V_1 is applied to the high-voltage terminal and the impulse voltage across the winding is limited to voltage V_3 by Thyrite or an external lightning arrester connected from the high-voltage to the low-voltage terminal. The effect is similar to that resulting from simultaneously applying voltages V_1 to the high-voltage terminal and V_2 to the low-voltage terminal. The charging currents from winding to ground are not nullified, and the center of the winding may be at essentially zero voltage at the same instant that the terminals of the winding are at their maximum voltages. The actual voltage gradients near the ends of the winding may be very large if no electrostatic shielding is used.

Curve (B) of Figure 9 shows how the initial distribution is changed by providing four intermediate connections between the winding and the Thyrite. Curve (C) of Figure 9 shows a typical initial distribution when a larger number of interconnections are made. There is a very substantial reduction in internal stresses in the winding. These curves are calculated assuming a wave front that reaches crest voltage in zero time. For finite wave fronts, the dips in curve (C) largely will disappear.

Series Winding of Regulating Transformers

The circuit of a conventional 2-core 4-winding regulating transformer is shown in Figure 10. The series winding bucks or boosts the input voltage and derives its excitation from taps in the exciting winding.

The maximum operating voltage across the series winding is usually 10 per cent of the input voltage. In a high-voltage regulating transformer, the primary winding of the series winding usually is designed for a rather low voltage to keep down the size of the tap-changing equipment. In many cases, the primary and secondary windings of the series transformers have approximately the same number of turns.

As has been pointed out for autotransformers, when an impulse voltage is applied to the high-voltage terminal, the majority of the voltage appears across the series winding. The same phenomenon occurs in a regulating transformer. It is well known that electromagnetic transfer of impulse voltages occurs between transformer windings, the induced voltage being in the ratio of the turns in the two windings in many cases.

Referring again to Figure 10, assume that the input voltage is 115 kv wye and the maximum series-winding voltage is 11.5 kv wye (6.64 kv phase voltage). Also, assume that the primary winding of the series transformer is 6.64 kv phase voltage, which is a reasonable voltage for tap-changing purposes. (In other words, the turn ratio of the series transformer is one to one).

When an impulse is applied to the output terminal of a magnitude consistent with the protective equipment on a 115-kv system, the bulk of the voltage may appear across the series winding. This voltage, under some conditions, will be transferred into the low-voltage winding at the turn ratio of one to one. That is, the bulk of the impulse voltage that can appear on a 115-kv system will be transferred into the low-voltage tap-changing circuit of the regulating transformer. The exact amount of the voltage depends mainly on the surge impedance of the input line, the leakage reactance of the transformer, the tap connections being used, and the length of the applied impulse wave.

It is obviously impractical to design the primary circuit for such high voltages. The solution is to limit the voltage across the series winding by shunting the winding with Thyrite or with an external lightning arrester. In high-voltage circuits, this brings up the same problem as that encountered in autotransformers and illustrated by Figure 9. Thyrite across the winding, connected at a proper number of intermediate points, not only limits the voltage appearing across the winding, but also distributes the surge voltage uniformly across the winding. (Under some conditions it is desirable to use Thyrite across the winding for voltage-distribution purposes, and, in addition, to shunt the winding with an external lightning arrester.)

Precautions Observed in Use of Thyrite

Thousands of Thyrite disks a year are used in power transformers in the manner described in this paper. None has failed to function properly in service. This 100 per cent service record is largely due to the care used in the Thyrite applications.

After manufacture, every Thyrite disk is tested for resistivity and stamped with a marking that is keyed to a volt-ampere curve. The transformer designer selects the disks needed from these curves. Before the disk assembly is mounted in the transformer, it is tested for resistivity. The induced voltage test on the completed transformer provides the final test.

Successful application of Thyrite in transformers requires that the same attention be paid to proper supporting and bracing of the Thyrite as is given to the winding and insulation of the transformer.

When a portion of winding is shunted by Thyrite, three thermal requirements are met: the number of disks in series is such that with continuous operating voltage across the Thyrite a temperature of 95 degrees centigrade is not exceeded; the Thyrite loss during induced voltage test (usually 3.46 times normal voltage or less) is within the heat storage capacity of the disks; and the energy input into the Thyrite is within its heat storage capacity during a system fault, when abnormal voltages may appear across a winding (notably, the series winding of an autotransformer).

All of these requirements are subject to calculation and are the basis for the selection of the disks. The insulation between turns, coils, taps, and so on, of the winding is determined from the expected impulse voltage across the Thyrite. Thyrite disks may be mounted in either vertical or horizontal stacks with ventilation provided by means of metallic spacers between disks.

Impulse Tests on Thyrite Shunted Windings

The detection of failures on commercial impulse tests has reached a high degree of precision, particularly since the development of the neutral-current method[2] of wave comparison. Turn-turn failures, where one turn represents only 0.1 per cent of the winding, can be detected by this means.

Failure detection consists of measuring the impulse current flowing through the transformer neutral, by cathode-ray oscillograph, on the initial 50 per cent of full wave and comparing it with the current produced by the 100 per cent full wave after the chopped wave impulses have been applied.

Failure is indicated by a difference in the wave shape. The wave changes because the failure has short-circuited a portion of the winding, and, consequently, the circuit constants of the winding have changed.

When Thyrite shunts part of a winding, the equivalent circuit of the transformer contains a resistor across that part of the circuit. Thyrite, however, does not have a constant resistance. Consequently, the equivalent circuit of the transformer is different for applied impulses of different crest values. Therefore, the neutral-current wave shape changes slightly for different applied voltages; that is, the initial 50 per cent impulse produces a different wave shape from the final 100 per cent impulse.

A new technique has been developed to handle circuits of this kind. Fortunately, as higher impulse voltages are applied to the line terminal, the change in circuit constants produced by the change in Thyrite resistance is gradual and the change in the neutral-current wave shape is gradual. Thus a trend is observable and departures from this trend show failure.

The test technique consists of applying initial full waves of 50, 70, and 100 per cent followed by the chopped waves and ending with a 100 per cent full wave. Comparison of the first two initial waves of 50 and 70 per cent with the initial 100 per cent wave shows whether failure occurred on the 100 per cent wave. Following the chopped waves, the final 100 per cent full wave is compared with the initial 100 per cent wave. Experience has shown this method of failure detection to be very effective.

Conclusions

Thyrite, a nonlinear resistance material, has many effective uses in power transformers. It is another means for controlling transient voltages in windings. It is used to supplement and in some cases to replace electrostatic shielding.

Thyrite has easily determinable and precise electrical characteristics. Its mechanical properties are excellent. As used in transformers, it has very high surge-current capacity, and since no gaps are used, the problem of interrupting follow current does not exist. The low-frequency loss in the Thyrite has little, if any, measurable effect on the transformer characteristics.

Thyrite is particularly useful in autotransformers and regulating transformers where normally certain insulation and transient-voltage problems are more acute than in simple 2-winding power transformers.

The remarkably good service record of Thyrite, which has been used in hundreds of transformers during the past ten years, is a result of careful electrical and mechanical application.

References

1. THYRITE, A NEW MATERIAL FOR LIGHTNING ARRESTERS, **K. B. McEachron.** *AIEE Transactions*, volume 49, April section, 1930, pages 411–17.

2. PROGRESS IN IMPULSE TESTING OF TRANSFORMERS, **J. H. Hagenguth.** *AIEE Transactions*. volume 63, 1944, pages 999–1005.

Discussion

W. H. Eason (General Electric Company, Pittsfield, Mass.): Mr. Meador points out that Thyrite possesses "precise electrical characteristics." Because of its nonlinear resistance characteristics, this material has found many other important applications in the central-station, industrial, and electronics fields, notably as discharge resistors for inductive circuits and in voltage stabilizing circuits. Engineers using Thyrite in these applications generally will find that it is necessary to allow electrical tolerances of ±30 per cent in applied voltage at a given current. Mr. Meador's statement and this fact do not at first glance appear to be in agreement and may require further elaboration.

A specific Thyrite disk does possess precise electrical characteristics and is stable over reasonable ranges of temperature and humidity. There are many variables which enter into the complex manufacturing processes involved in producing Thyrite. Each affects the ultimate electrical characteristics to some degree. Elaborate precautions taken during manufacture reduce these variations to a minimum, but their number is such that it is not possible to predict the ultimate electrical characteristics closer than the ±30 per cent indicated. However, dictates of design in circuits utilizing Thyrite generally permit such tolerances.

Every Thyrite disk is tested during manufacture to determine its electrical characteristic. The tolerance may be considerably narrowed by dividing a given "batch" of disks into narrower classes during production testing.

The use of Thyrite in transformers is large enough and the requirements are sufficiently diversified so that various combinations of classes of disks may be selected to give the required characteristics and still utilize the entire production.

The same procedure has been followed in special Thyrite disks made for other high production applications. A case in point is a discharge resistor for protection of large generator fields, which utilizes 20 disks connected in parallel. During manufacture these disks are segregated into three resistance ranges and designated as class *A*, *B*, or *C*. In assembling the resistor, care is taken to use as many class *A* as class *C* disks, so that the over-all electrical characteristics are those of 20 class *B* disks; the tolerance thus is effectively reduced by 67 per cent.

Extremes in the Insulation Co-ordination of Oil Insulated Transformers

F. J. VOGEL
MEMBER AIEE

THE margins obtained and degree of co-ordination between transformers and other insulation in transmission systems depends largely on the possibilities inherent in the insulation design of the transformer. Recently some units have been designed which take full advantage of the impulse characteristics of insulating materials as compared to their 60-cycle strengths. These designs are core form, or at least have concentric coils, and it is likely that they might be considered to have maximum impulse characteristics compatible with present 60-cycle test requirements. It is believed that these characteristics affect, and should be given consideration, in the setting of future required impulse levels and tests, and perhaps in the 60-cycle tests which are required. The basic insulation levels only include full-wave values and it is believed that this is not sufficient for transformers. This matter affects the cost and the service reliability of transformers. It is believed that besides the basic insulation level, either a steep front or other type of test should be included.

Design Considerations and Test Data, 69-Kv Transformers

To illustrate some of the problems confronting both the system and transformer designer, one important design consideration and the results of some tests made in the development of a possible new line of transformers are described.

It is believed that irrespective of present standards, the major insulation, and the turn-to-turn and coil-to-coil insulation should be about equal in impulse strength.

Present rules do not require this relationship, but investigation shows that the saving using a relatively weak winding is small. The designs referred to in this paper were made so that the winding strength is in excess of the insulation strength to ground. Tests to check the design have been made on a 1,000-kva 34.5-kv transformer, a 667-kva 69-kv transformer, and a 69-kv potential transformer. The results of the tests on the 667-kva transformer, which are representative, are shown in Tables I and II. With the exception of the failure to ground on X_1, no failures were found, as X_2 showed no signs of failure. The tests on the 1,000-kva 34.5-kv unit were somewhat similar except that the gap length and voltages were reduced. The 69-kv potential transformer tests were similar to those above. The 667-kv transformers did not fail to ground with a 60-cycle voltage of 160 kv on the high-voltage winding.

Analysis of Results

Table III shows the actual demonstrated strengths with present American Standards Association (ASA) and National Electrical Manufacturers Association (NEMA) requirements.

Let us analyze these tests. A margin, so that there will be practically no shop failures, is necessary. The extreme right-hand column would not be safe to use as guaranteed values. Margins used differ and are those deemed reasonable by the manufacturer. The margin ensures freedom from injury on test except for defective materials or assembly.

The chopped wave tests under present circumstances do not prove very much, at least, with these designs. If the bushing is flashed over, it indicates that the windings will stand more than the bushing and this characteristic may be desirable. The full-wave test made with the over-size bushing emphasizes the correlation between 60-cycle strength and impulse strength for the core-type construction. If full-wave co-ordination and tests only were required, it is obvious that a large decrease in insulation would be possible, at least in the ratio of 450/410, or nearly one voltage class.

The steep-front tests indicate a larger margin between test and requirements in terms of voltage than actually exists, due to the effect of time and the fact that to meet 580 kv, considerable variation in test conditions requires designing to meet 620 kv or more, with a margin besides.

The question then arises, is the steep front test and NEMA value desirable? In the case of the design of some bushings, flashover would occur, likely porcelain breakage, and possibly even fire. Other bushings have much higher steep front strength. Unless some type of gap is used which will protect both the transformer and bushing, it may not be desirable. But there are two other points of view.

Suppose the user wishes to depend alone on gap protection. It seems likely that a very good degree of protection to the transformer and bushing can be obtained at the expense of some gap flashovers and outages. One user does.

Suppose also that the operator uses line type lightning arresters. Lines of the above ratings are not always well protected. Several cases of bushing flashover and damage even when lightning arresters and ground wire protection were used have occurred. In one case the lightning arrester and transformer were both badly damaged and the operator was extremely irate over what he felt to be a case of poor transformer design. Hence,

---

Paper 49-195, recommended by the AIEE Transformers Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20–24, 1949. Manuscript submitted November 15, 1948; made available for printing May 2, 1949.

F. J. Vogel is Professor of Electrical Engineering, Illinois Institute of Technology, Chicago, Ill.

The test data presented here are by the courtesy of the Allis-Chalmers Manufacturing Company, and were taken in the course of development work done both in their Pittsburgh and West Allis works.

Table I. Impulse Tests on 667-Kva 69-Kv Transformer, High-Voltage Winding

Type of Test	Oscillogram	Rod Gap, Inches	Number of Tests	Average Kilovolts
Reduced front waves			2	181
Steep front waves		14½	3	648
Chopped waves			2	418
Full waves			4	350
Steep front waves	*2393-2*	14½	25	663
Steep front waves	*2408-1a*	15½	25	746
Steep front waves	*2409*	16½	13*	833
Full wave**	*2410-1a*		2	354
Full wave			15	355
Full wave			15	380
Full wave			15	402
Full wave			15	423
Full wave			15	450
Full wave	*2422-12c* *2422-12d*		2†	472

* Bushing porcelain broken.

** Oversize bushing installed.

† Transformer winding failed to ground—no failure found in turns or coils.

Table II. Impulse Tests on 667-Kva 69-Kv Transformer, Low-Voltage Winding, 7.2 Kv/ 12.470 Kv Star

Type of Test		Oscillogram	Rod Gap, Inches	Number of Tests	Average Kilovolts
Reduced FW	X_2			2	62
Steep front	X_2		3	3	175
Chopped	X_2			2	136
Full wave	X_2			2	117
Steep front	X_2		3	25	178
Steep front	X_2		3½	25	206
Steep front	X_2		4	25	232
Steep front	X_2	*2419-10 B*	4½	25	248
Full wave	X_1			15	110
Full wave	X_1			15	121
Full wave	X_1			15	132
Full wave	X_1	*2412*		3	141
Full wave	X_2			15	107
Full wave	X_2			15	120
Full wave	X_2			15	131
Full wave	X_2			15	140

Table III. Comparison of Actual and Required Strengths

Winding	Type of Test	NEMA or ASA Standard	Kv Required	Kv Withstood
High Voltage	Chopped wave	ASA	410	418*
	Full wave	ASA	350	450
	Steep-front wave	NEMA	580	833**
	60 cycle	ASA	140	160
Low Voltage	Chopped wave	ASA	130	136
	Full wave	ASA	110	140
	Steep-front wave	NEMA	195	248
	60 cycle	ASA	34	

* Not breakdown tests but to ensure standards were met.

** Winding stood up, but bushing flashed over.

Table IV. Steep Front Tests on Model

Number of Tests	Gap Length, Inches	Kilovolts, Average
25	24	800
25	27	850
25	30	1,020
25	33	1,120
25	34½	1,070
50	36	1,100
25	37½	1,100
27	39	1,260
29	40½	1,200

all things equal, it would probably have been better had this transformer been relatively as strong as the one in Table III. In the case of an extremely high rate of current rise, the voltage across a lightning arrester can resemble a steep-front wave, which can be higher at the transformer than at the lightning arrester by as much as 10 to 15 per cent, and will also be longer in time duration. A transformer protected by modern lightning arresters at the terminals, and with strengths in line with Table III would withstand lightning except in extremely rare cases. Further, even in the case of lightning arrester damage or removal, a very considerable margin over most line disturbances would remain, particularly if the gap were added.

If the industry believes that steep front strength is not necessary, the advantages just given may not be obtained. In that case, full advantage of a decrease in insulation within the unit to meet the requirements could not be obtained without decrease in 60-cycle test requirements or destroying the co-ordination within the winding.

To sum up, it would seem desirable, for the 69 kv or lower voltage systems, not to design transformers to depend entirely on the normal published characteristics of lighting arresters, due to the varying degrees of lightning protection on these lines. If this is true, these transformers should have impulse strengths at least in line with present NEMA and ASA rules, both steep front and full wave. It is believed that the use of full waves as a test and standard alone would result in a lower strength of transformers, and there would be some failures, whereas it is understood that practically no lightning failures of transformers designed in the last 15 years now occur.

Design Considerations and Test Data on 138-Kv Transformers

The same design consideration as to major and coil insulation came up with regard to the higher voltage transformers, and it is believed that the windings should be stronger than the insulation to ground. With this in view, tests on some models and on large complete units were made.

Tests on transformer insulation for full and chopped waves have been reported in other papers, and would indicate that 138-kv transformers[1,2] should be able to stand from 810 to 860 kv with a factor of safety, in the order of that for the 60-cycle test. In order to see what values might be obtained with steep-front tests, the tests shown in Table IV were made with a wave rising at a rate of 1,000 kv per microsecond for gap lengths up to 33 inches.

Above 33 inches, some rounding of the applied wave was experienced, and experience shows that the test severity is reduced. No failure, however, occurred on these tests in the winding or to ground, which would indicate that deterioration, due to testing should not be greatly feared in this particular design. The model withstood 25 full waves at both

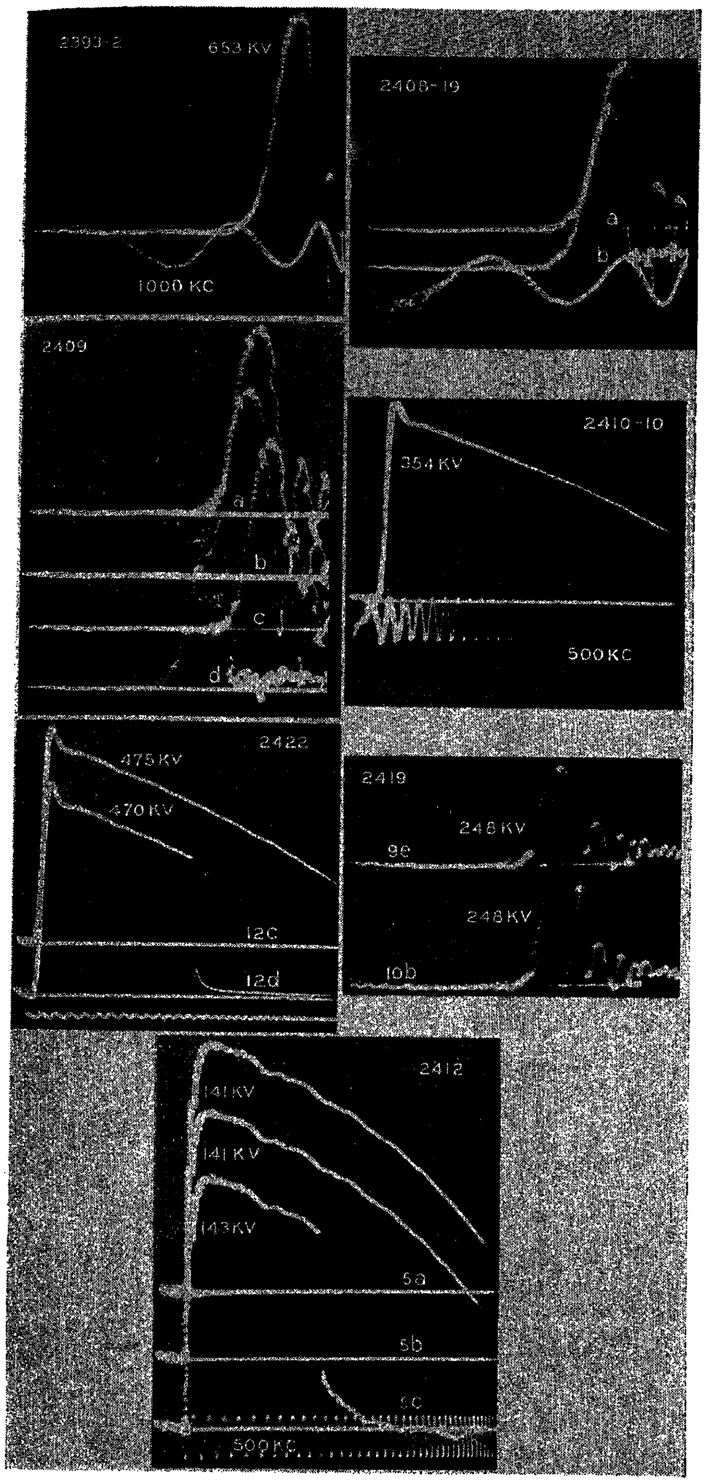


Figure 1 (left). Impulse tests on 667-kva 69-kv transformer

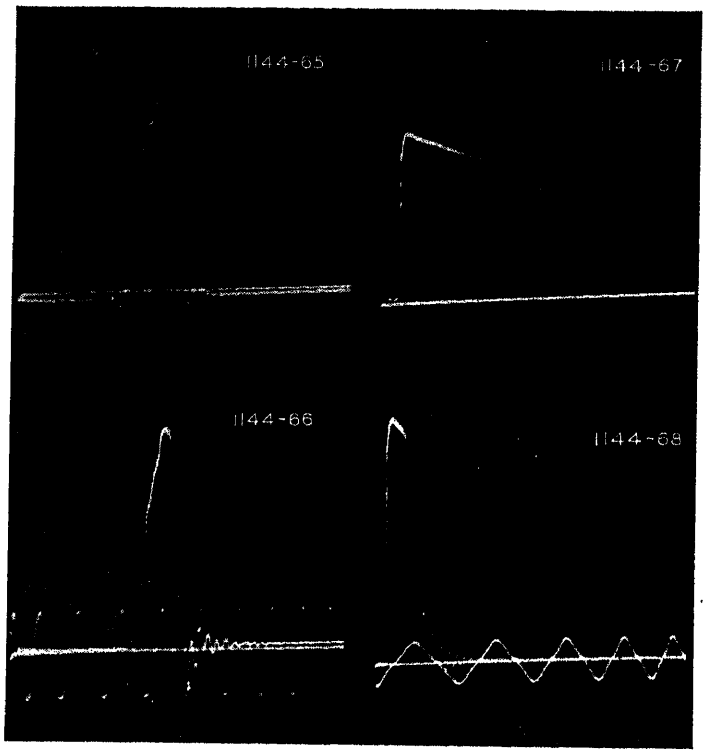


Figure 2 (above). Oscillogram of test on 138-kv transformer insulation

650 and 675 kv after these tests were made. A record of the steep front impulse test at 33 inches gap length on the model is shown on oscillogram *1144-65*.

Tests on a large 138-kv transformer which had to be rebuilt were made. Tests, not to failure, but which were under the "design level" at which failures might be expected were made. The results of these tests are shown in Table V. The required values versus the test values are shown in Table VI. The values given in these tables might be studied as before. If full waves of 650 kv only were required, probably some reduction in 60-cycle test voltage could be made, since it is believed that the 925-kv wave could have been held as a full wave provided the bushing had not flashed over. If a reduction in 60-cycle strength is not made, the winding strength alone could be reduced, and the unit would not be internally co-ordinated. Co-ordination is highly desirable.

If we retain present levels, the user who depends on gap protection alone is reasonably safe. Further, if a lightning arrester is used and is subject to undue currents or rates of rise, some transformer failures will be averted.

Present failure rates are extremely low, which indicates that excessive currents or rates of rise are very seldom met, or that some steep front transformer strength exists. Some years ago, transformers had two thirds their present full-wave levels, no lightning arrester protection and no steep front strength and relatively few failures occurred. Perhaps, therefore, the number of failures would not be changed if the full-wave strength and test of transformers were barely met, when lightning arresters are used.

Lines are now designed where there are practically no lightning failures. Stations can be so well protected that very low currents result. Perhaps the lightning arrester characteristics can be entirely relied upon in transformer design, and then it would seem definite that steep front strength is unnecessary, and reductions in 60-cycle strength and steep front strength could be made provided the standards would permit.

The following proposals might be made:

1. For the full waves now specified, make a new 60-cycle test somewhat lower and drop the chopped-wave test and reduce the steep-front test.
2. Retain approximately the present full-wave and steep-front test.
3. Have a full-wave test only.

Conclusions

Test results have been shown which indicate the impulse strengths of transformers compatible with the 60-cycle insulation strength. This information is useful in considering the highest values obtainable for use in co-ordination studies.

Depending on the degree of co-ordination, it is possible to reduce some of the insulation tests of transformers. It is believed that this subject should be considered in two groups for oil insulated power transformers. These are transformers from 15 to 69 kv and from 115 kv up.

It would be recommended that the standards for the first group be changed as follows: Add the NEMA steep-wave tests to the ASA standards, and eliminate the chopped waves. If acceptance tests were to be required by the standards, like the present 60-cycle tests, only the full-wave tests, consisting of full waves as specified now, might be used. The requirements for the second group are not as clear. It would seem that a reduction in 60-cycle strength, and in the NEMA steep-front test could be made, dropping the chopped-wave test, and retaining the present full-wave tests. Those using gap protection only would require the present higher grade of insulation.

Table V. Tests on Transformer*

Type of Test	Number of Tests	Gap Length, Inches	Oscillogram	Kilovolts
Steep front	7	30½		
Steep front	10	32	*1144-66*	1,090
Chopped waves	5			875
Chopped waves	4		*1144-68*	925
Full wave	1		*1144-67*	650

* No failures occurred on these tests.

Table VI. Comparison of Required and Test Values, 138-Kv Insulation

Type of Test	NEMA or ASA Standard	Kilovolts Required	Kilovolts Withstood	Gap Length, Inches	
				NEMA	Actual
Steep front	NEMA	960	1,090	29*	32
Chopped wave	ASA	750	925		
Full wave	ASA	650	650		

* The equivalent gap length for 960 kv and 0.96 microsecond is not given, but is estimated to be in the order of 27 to 29 inches.

References

1. Factors Influencing the Insulation Co-ordination of Transformers, F. J. Vogel. *AIEE Transactions*, volume 52, June section, 1933, pages 411–16.

2. Dielectric Strength of Transformer Insulation, P. L. Bellaschi, W. L. Teague. *Electrical Engineering* (*AIEE Transactions*), volume 56, 1937, pages 164–77.

Discussion

J. R. Meador (General Electric Company, Pittsfield, Mass.): Mr. Vogel has written a paper on a subject that deserves a great deal of thought and discussion. The question of what types of impulse waves to use in test, and what their relative voltage levels should be, is very important to the industry. It is this importance that leads me to question some of the actual and implied conclusions.

In testing a developmental transformer, coil stack, or insulation structure, it is obviously desirable to get as much data as possible. We have to be careful, however, to distinguish between proof-testing a new proposed design and getting data for a revision of existing standards.

This is exemplified by the maximum voltages applied to the high-voltage winding of the 667-kva transformer, which were as follows:

Full wave, 472 kv—failed
Steep wave, 833 kv—did not fail
Chopped wave, 418 kv—did not fail
60-cycle, 160 kv—did not fail

These tests demonstrate what this particular transformer will withstand on the different types of tests but they certainly do not show what the strength of the transformer is, except on the full wave. Even the full-wave strength may have been affected, for example, by incipient damage during the steep-wave tests.

None of the tests show the 60-cycle breakdown strength. How can it be concluded then that the present ratio of 60-cycle test to full-wave test should be changed? The same applies to all of the other relative voltage levels.

I have one comment on the author's various proposals for possible future tests. It concerns the substitution of steep waves for the present chopped waves. Without discussing the merits of the change so far as stress on the transformer is concerned, there is one argument that should not be overlooked. It is well known that internal winding failures, such as those that might be caused by chopped waves or steep waves, will often regain much of their original strength if permitted to soak in oil for a short period. It is essential therefore that the final full wave (and neutral current) oscillograms be taken as soon as possible after the application of the steep tailed waves. This is easy to do in the case of the chopped wave. When steep waves are applied, a change in connection of the impulse generator is often required before the remaining tests are made. In this case the interval may be long enough to prevent detection of the failure.

E. R. Whitehead (Illinois Institute of Technology, Chicago, Ill.): Mr. Vogel's paper illustrates the feasibility of designing modern power transformers to meet the NEMA steep-front impulse tests with suitable margins, and recommends that future standards for transformers of 69 kv and lower voltage rating include these requirements as design, if not acceptance, tests. For the higher voltage classes of 115 kv and above the possible reduction in winding strength is pointed out.

I should like to comment in support of Mr. Vogel's view that transformers up to and including the 69-kv ratings be designed to meet steep-front tests at or near the present NEMA levels even if the steep-front tests are not included in the standard acceptance tests. I believe it is well understood that factors of line impulse insulation, ground wire protection (or lack of it), structure footing resistances, and the type and location of lightning protective devices are much more variable on the lower voltage than on the higher voltage systems for which the best system design practices are more easily justified economically. Experience indicates that this variability contributes to the relative importance of steep front waves of magnitude substantially greater than that established by the standard tests on lightning arresters.

For the higher voltage classes, perhaps 115 kv and above as suggested, it seems logical to take advantage of the factors of good ground wire protection, low structure footing resistance and transformer-mounted lightning arresters and establish winding strengths more directly dependent upon the protective levels set by the lightning arresters. The cost of providing adequate insulation is a factor of greater importance in the high voltage classes than in the lower.

It has been the writer's experience over many years of association with impulse testing of transformers that defects in material or workmanship are detected much more frequently than are errors in design. Based on this experience, it might be concluded that steep-front design tests should be made to check each new design, and that full-wave tests be employed as the normal acceptance tests.

Wm. L. Teague (Westinghouse Electric Corporation, Sharon, Pa.): In his paper, Mr. Vogel has presented some interesting data. His comparisons of the relative severity of steep-front, chopped-wave, and full-wave tests are based on a few specially designed units. An evaluation of the relative severity of tests with the three types of waves also can be made by considering the results of commercial impulse tests on a large number of transformers.

The results of impulse testing 500 power transformers over a period of 15 months were analyzed. All of these transformers were designed to withstand front-of-wave, chopped-wave, and full-wave tests. Although some of the designs were for special levels and some of the tests varied from standard, these variations were relatively small and kept at a constant proportion for the three types of waves. Of the 500 units tested approximately 300 were given front-of-wave and full-wave tests; 100 were given chopped-wave and full-wave tests; and the remaining 100 were subjected to front-of-wave, chopped-wave and full-wave tests. This would mean that all 500 units were subjected to full-wave tests, 400 to front-of-wave tests, and 100 to chopped-wave tests. The faults revealed by the three types of tests expressed as a percentage of the total units that received the test were essentially the same. Hence, in the commercial impulse testing of power transformers in large numbers the front-of-wave, the chopped-wave, or the full-wave test was equally searching in its effect.

The results of these tests also indicate that the volt-time characteristics of transformer insulation as defined by NEMA front-of-wave tests and ASA chopped- and full-wave tests are approximately correct.

Since both the front-of-wave test and the chopped-wave test stress the turns, coils, and major insulation at the line end of the winding, it is not necessary or economical to apply both types of waves in the commercial testing of transformers. Either type of wave should be sufficient to verify that the windings will withstand flashovers and have the required short-time strength level. Of the three types, the full wave comes nearest to being a complete test. It constitutes a reasonable test on the line end of the winding up to full-wave levels and demonstrates the dielectric strength between interior parts of the winding and to ground where voltage stresses may result from oscillations. Hence, in the commercial impulse testing of transformers some form of chopped wave and the full wave are essential. The trend in impulse testing should be toward simplifying and streamlining the tests.

Philip Sporn and I. W. Gross (American Gas and Electric Service Corporation, New York, N. Y.): This paper apparently suggests the reduction of severity of dielectric tests on transformer insulation, and bases the suggestion largely on "results of some tests made in the development of a possible new line of transformers." The author seems to imply that there is a relation between the 60 cycle and impulse strength of transformers. This is a philosophy which we thought was entirely abandoned over 15 years ago based on experience with designs prevalent at that time. The basis for the 60-cycle test values for transformers which has been unchanged for more than 30 years has given such a good account of itself under service conditions, that any attempt to change it at this time should be made only after the fullest consideration of the fundamentals of transformer design and the expected over-voltages in service. Of course, this assumes that impulse requirements will be independently handled.

As to impulse strength and test values. the present basic insulation levels are based on so solid a theoretical foundation and on so much practical experience that no attempt should be made to change them except on clear indication of the soundness of the new course.

When the impulse testing of transformers was being developed nearly 20 years ago on a commercial basis, various ideas were held on the required severity. As fundamental analysis progressed and more became known about the types of impulse surges to which transformers in service could be subjected, and as the impulse design problem in transformers was better understood, the thinking in our own company developed along these lines: It was believed that transformers in service could be subjected to three different types of impulse or surges of lightning origin: first, a full-wave impulse without alteration by reason of gap flashover or lightning arrester action; second, a chopped wave where the chop took place on the wave tail; and third, a chopped wave where the chop took place on the front of the wave. The reasons why such surges could reach a transformer are now too well known to require further comment. The initial impulse testing of transformers used the full wave, supplemented by a chopped wave which was representative of conditions obtaining when the bushing (air gap) flashed over.

For the third condition, that is, flashover on the front of the wave, we requested and received commercial impulse tests on transformers with a steep front in the order of 1,000 kv per microsecond, flashover taking place on the front.

The ability of transformers as far back as 15 years ago to withstand such waves was amply demonstrated by commercial factory tests, and the successful operation of such transformers under impulse conditions, on our own system at least, has given ample justification for continuing these types of tests up to the present time. It is not clear from the author's comments whether he questions the ability of transformers built within the last 15 years or so of withstanding all three types of wave—full wave, chopped wave, and fast front. Neither is it clear why he believes the transformers on which he has reported impulse tests are superior to those which have been built within the past 15 to 18 years.

The general pattern of impulse testing today is for the transformer to be made to withstand a full wave of the basic impulse insulation class together with a chopped wave, the chop taking place on the tail at 115 per cent of the full wave, and a fast front test with the crest value being in the order of 150 to 200 per cent of the full-wave value, depending on the transformer voltage class. As mentioned before, present-day transformers are capable, as demonstrated by commercial tests, of withstanding these test values.

We are in thorough agreement with the author that if we required only a full-wave test on the transformer insulation it would result in a lower impulse strength of transformer insulation. If, as he predicts, and we concur therein, some transformer failures would develop if the impulse tests were limited to full waves, such a proposal should be considered all the more carefully in the light of the over-all economic penalties attendant upon failure. It does not seem to us that with the existing background such a procedure would be at all attractive.

The author states that it is believed "the windings should be stronger than the insulation to ground." Further amplification of this statement is certainly desirable. We consider a transformer as a unit, and whether it fails between turns or to ground is beside the point. A failure is a failure, and if there is a service interruption by reason of a failure, it is of little concern whether that failure took place between turns or sections, or to ground.

There does seem to be some point in eliminating the so-called chopped-wave test and substituting therefor the front-of-wave test. After all, the three tests which are now required on our system transformers merely determine three points on the volt time co-ordinates of the transformer insulation curve. To omit the chopped-wave test (not fast-front test) could probably be tolerated without any appreciable sacrifice of transformer quality.

The proposal of the author that the acceptance test of a transformer so far as impulse is concerned be limited to the full-wave test would leave us in the position where we know nothing about the short time impulse strength of the transformer. We certainly do not want to face this situation now or in the future.

There seems to be some implication in the author's paper that he favors the use of an air gap as a protective device for a transformer. If the transformer is built with insulation sufficiently above the test level of the gap, there certainly is no objection to this, provided the user is willing to live with the service outages that are inherent in such protection. However, we are firmly convinced that the fundamental insulation requirements of a transformer should be such that it will perform successfully without service interruption when protected by the modern lightning arrester which has given such a high degree of reliability as proved by experience obtained in service during recent years.

P. L. Bellaschi (Consulting Engineer, Sharon, Pa.): This discussion has reference to the economics of power transformers, especially from the application end. First, in regard to the insulation: "Does the present structure of tests provide a balanced and economical design in modern transformers?" My answer is "yes." These are the principal reasons. The volt-time characteristics embodied in present ASA impulse tests and NEMA front-of-wave tests are fundamentally sound, economically attainable, and well co-ordinated with available methods of protection. Whether the chopped wave is retained or dropped would not change the picture fundamentally. Presumably if the chopped wave is dropped, the front-of-wave would be substituted in its place in the tests.

On 230-kv stations designed with overhead ground wire over the station structure and extended for at least one-half mile on the line, as is now the practice, the modern lightning arrester provides co-ordinated protection to the full-wave strength of transformer insulation, for lightning arrester discharge current well up to the values so far experienced in these stations. The higher voltage, front-of-wave transformer tests in present standards provide for the necessary strength to withstand the stresses such as can result from short-time effects due to lead drops, and so forth, as are entailed from rapid impulses. Similar conclusions regarding present full-wave and short-time tests apply even more so for the low and medium voltage substations, for in these applications higher discharge currents prevail and lead drops can assume relatively greater importance.

Looking to the future, to the rationalization process in course, we shall soon be dealing with reduced insulation more and more, especially in applications to high-voltage grounded systems. In fact on well-grounded systems at the higher voltages, 230 kv and above for instance, we shall be dealing quite commonly with 75 per cent and even 70 per cent insulation levels.

In speaking of well-grounded systems, I have reference to systems in which dynamic overvoltages to ground on the unfaulted phases do not exceed 65 to 70 per cent line-to-line voltage or even lower. That is, where X_0/X_1 is less than one and R_0/X_1 is under a quarter or thereabout. Equally import, overvoltages resulting from load dropping and runaway of hydro machines, as well as surges due to restrikes of circuit breakers, all are within proper and acceptable limits by design and operation.

Under these conditions on these systems, 75 per cent and possibly 70 per cent lightning arresters can be safely applied. In these applications the basic insulation levels of transformers especially would be proportioned accordingly with corresponding economies, not to mention other advantages such as in the improved electrical characteristics of the equipment proper and relative reduction of physical size. These practices are currently in full course of development and mark another milestone in the economies attainable, in keeping with sound engineering, through the process of rationalization of co-ordination and protection of station insulation.

F. J. Vogel: My paper as written was intended to raise questions regarding present insulation standards in the design of transformers, particularly as to whether; first, the relations of steep-front full-wave and 60-cycle tests are correct; second, if the present levels up to 69-kv class are desirable; third, if steep front values are required; and last, if reductions in the test values for the higher voltage classes are in order.

With respect to the first question, it seemed to be the opinions of both Mr. Teague and Mr. Bellaschi that they are. Mr. Meador questioned it on the basis of lack of test data, referring to the fact that the 60-cycle breakdown value was not given. However, its insulation to ground was very similar to that referred to in many previous papers, where the impulse ratio was about two, which would indicate that the 60-cycle strength would not be greatly in excess of the 160-kv cited. Please note that the value of 472 kv was obtained using oversize bushings and it is apparent that if no 60-cycle tests were required, and full-wave impulse tests only were required, a large decrease in insulation clearances and materials could have been made.

The second question is answered only by Mr. Whitehead, who agrees with me that the present levels for transformer insulation up to the 69-kv class are desirable without change.

The question regarding steep front wave strength is not so directly answered and has a number of ramifications. Both the discussions of Mr. Whitehead and Mr. Sporn and Mr. Gross indicate that that feature is required. I would like to point out that the ability to meet the chopped wave does not imply ability to meet steep front waves, although the converse is likely. This means that the basic insulation levels alone do not ensure an adequate transformer, and that the full waves and steep front waves probably do ensure the maximum values. The questions of difficulty in applying steep front tests, and even failure detection, were brought up by both Mr. Meador and Mr. Teague. I think further discussion of this is necessary.

With respect to the last question, there does seem to be agreement that reductions for the higher classes are possible, on the basis of lightning arrester applications. Mr. Whitehead and Mr. Bellaschi are in agreement, and I know that the practice is being given serious consideration elsewhere.

The discussion by Mr. Sporn and Mr. Gross would seem to merit some further reply. I wish to thank them for a most constructive discussion. My reply to their discussion is as follows: They raised the question as to a relation between 60-cycle and impulse strength of transformers. Fifteen years ago there was no relation between the 60-cycle and impulse strength of transformers. It was just a little bit like the question of the members of the human race. You could say that all men were members of the human race but you could not say that the human race were all men. Likewise transformers are composed of major insulation to ground and winding insulation. The impulse strength to ground was found to bear a fairly definite relation to the 60-cycle strength. If the winding insulation were made equal to it then it also would bear this fixed relation, and this fixed relation would be the best which you could obtain compatible to the 60-cycle strength. I have no objection to the present basic insulation levels as applied to operating voltages, particularly up to 69 kv. I question very much that they are based on a very exact foundation, but they certainly have given a good practical account of themselves.

I am in complete agreement with the discussors' thinking with respect to the three different types of impulse. That there will be full waves is beyond question and that the voltage may be high enough to cause a flashover adjacent to the transformer also seems to be a possibility. This may definitely take place on the wave tail or on the front of the wave.

It is very likely that the paper was not clear with respect to proposals made by me, but the fact is that I intended to raise questions rather than to give my viewpoints. I believe that full-wave and either chopped- or steep front-wave tests should be given to nearly all transformers. It was proposed by some members in the Transformer Committee that chopped waves might be eliminated and full waves be used only. It is clear from the discussion that we both agree that full waves alone are inadequate.

There are a number of other questions which can be lumped together. Were the transformers 15 years ago just as good? Why should the winding be stronger than the insulation to ground? Why favor the use of some form of limiting gap as a possible protection device? The answers to these questions are as follows:

In two papers which I presented recently, methods of obtaining the strength between coils and in determining the difference in effect between steep wave-front tests and chopped-wave tests were derived. So far as I know this is the first line of transformers which were laid out using these data. A study of the values given in the paper show that very high impulse strengths were obtained and that broad gaps sufficiently long to avoid trouble with switching surges could probably be used.

The NEMA steep front tests as at present constituted are less severe than the tests using the gaps specified by one user and which can easily be made based on the performance of these transformers.

It is true that the transformer's insulation strength so far as the user is concerned is a unit. However, if the transformer winding is strong enough, the transformer's maximum strength as determined by the major insulation is obtained. I believe that a slight margin in the winding above this should be taken. Another reason is that there is an additional stress in the coil structure due to the fact that it is excited. Further, there is little or no extra cost in making the winding and major insulations equal.

There are two reasons why the use of a gap as an auxiliary protection device might be desirable. One is that for one reason or another the lightning arrester may not do its duty; or the lightning arrester may not be used for economical or other reasons. Another reason is that tests made by the author on several occasions have shown that with man-made lightning, it is possible to obtain excessive voltages even with lightning arresters present. I do not think that this is any argument against the use of lightning arresters since they prevent outages and possibly give an extra margin of protection. Nevertheless if there is a gap which can ordinarily be protected by the lightning arresters, and will always protect the transformer, I see no objection to it.

A Method for Designing Pulse Transformers

H. S. KIRSCHBAUM
ASSOCIATE AIEE

C. E. WARREN
ASSOCIATE AIEE

THE RAPID development in recent years of circuits and techniques employing short-duration pulses of voltage and current has created considerable interest in the use of transformers as coupling devices under pulsed conditions. The pulse transformer will perform one or more of the following functions with a minimum of distortion to the transmitted pulse:

1. Impedance transformation.
2. Voltage and current transformation.
3. Phase inversion.
4. D-c isolation of two or more circuits.

This paper will describe a method for designing pulse transformers, coupling linear resistive source and load impedances with either step-up or step-down turns ratio, nonphase inverting. The design procedure will be concerned largely with controlling wave form on the front and top of the output pulse. Other features of the wave form, transformer size, rating, and efficiency may be adjusted within certain limitations.

Some Fundamental Considerations

In order to design a pulse transformer to give a specified performance in a particular circuit, it is necessary to determine the transformer parameters which significantly affect the over-all performance of the circuit. Since the pulse transformer is required to transmit wave forms which are essentially transient in character, it is evident that the distributed capacitances of the transformer will play an important part in determining the transformer performance. In fact, a rigorous approach to the problem would involve a consideration of the electric and magnetic fields within the transformer in terms of Maxwell's equations. This approach, however, is much too general and cannot be handled in cases of practical interest. The first step in the simplification of the problem, therefore, is to approximate the fields in terms of lumped circuit parameters.

Figure 1 (A) shows a simplified transformer illustrating the several physical quantities usually considered in pulse transformer theory. These quantities may be classified in terms of the magnetic and electric field energies as follows:

1. Magnetic energy stored in the mutual flux field Φ_m, within the transformer core.
2. Magnetic energy stored in the leakage flux fields Φ_1 and Φ_2. This energy is usually considered as residing in the space between primary and secondary windings.
3. Electrostatic energy stored in the capacitance network of the transformer. The capacitance network illustrated consists of the capacitances of the individual turns to the core, capacitance between adjacent turns on the same winding and capacitance from primary to secondary turns.

The simplified transformer shown in Figure 1 (A) is still too complicated for convenient analysis and is usually simplified by means of the following assumptions:

1. The energy stored in the capacitance between adjacent turns on the same winding is small and therefore these capacitances are usually neglected.
2. The energy stored in the distributed winding-to-core and winding-to-winding capacitances is considered to be stored in equivalent capacitances connected to the coil terminals. See Figure 1(B). A primary and secondary terminal of like polarity are at the core potential either conductively or pulsewise.

In Figure 1 (B), the equivalent capacitances C_{pw} and C_{sw} will store the same energies respectively as would be stored in the distributed primary and secondary winding-to-core capacitances of the actual transformer. The capacitance C will store the same energy as would be stored in the distributed primary-to-secondary capacitance of the winding. The equivalent capacitances as defined are usually calculated[1] on the assumption of a linear distribution of voltage along the coil. A detailed analysis of circuits similar to that of Figure 1(A) shows that the assumption of linear voltage distribution is in error during the initial part of the transient period. However, experience has shown that for the usual circuit applications, the higher modes of oscillation in the transformer are well damped and do not affect over-all circuit performance. In some cases the initial nonuniform distribution of potential along the coil may overstress the turn-to-turn insulation of the coil.[2]

A Pulse Transformer Equivalent Circuit

The circuit of Figure 1(B) may be replaced by the equivalent circuit of Figure 2(A) where L is the transformer leakage inductance and the shunt branch is a circuit taking into account the pulse excitation properties of the transformer core. The circuit of Figure 2(A), in turn, may be replaced on a one-to-one, primary basis by the equivalent circuit[3] of Figure 2(B).

The design procedure described in this paper is based on the transformer equivalent circuit of Figure 2(B). This circuit differs from others[1,4] which have been used as a basis for pulse-transformer design in that the capacitance, nC, shunting

---

Paper 49-198, recommended by the AIEE Communication and Electronics Committees and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Swampscott, Mass., June 20–24, 1949. Manuscript submitted February 2, 1949; made available for printing May 3, 1949.

H. S. KIRSCHBAUM and C. E. WARREN are with The Ohio State University, Columbus, Ohio.

The work described in this paper was performed under contract between the Air Materiel Command, Watson Laboratories, Cambridge (Mass.) Field Station, Cambridge, and the Ohio State University Research Foundation, Columbus.

The authors wish to acknowledge the assistance of Neal Smith and Leo Botwin in obtaining and preparing the data used in this paper.

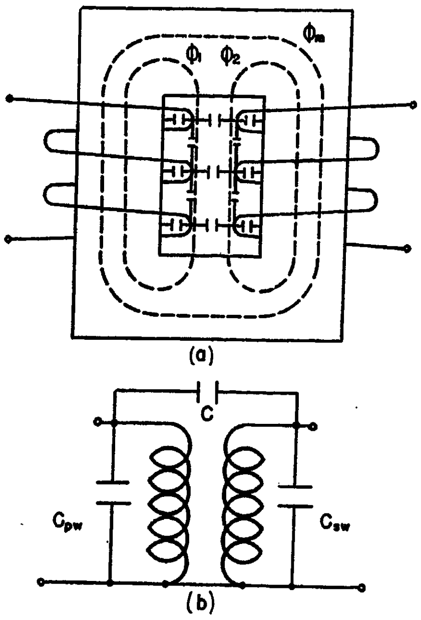


Figure 1. (A) Schematic diagram of transformer showing capacitance network and magnetic fluxes

(B) Simplified transformer circuit showing reduction of capacitance network to equivalent terminal capacitances

the leakage inductance, L, has been retained as an essential part of the circuit. The circuit is not unique or complete, but it has been found to be adequate for the design of transformers with turns ratios, n, in the range of about 1/10 to 10, but not in the vicinity of n equals one. In some cases, the design of unity turns-ratio transformers requires consideration of higher modes of oscillation and, therefore, a more complicated equivalent circuit.

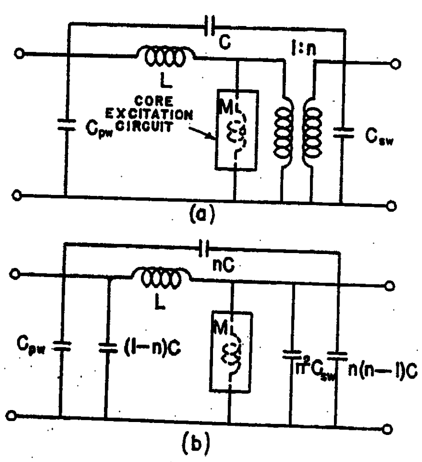


Figure 2. (A) Transformer equivalent circuit before reduction to one-to-one primary basis
(B) Transformer equivalent circuit reduced to one-to-one primary basis

The circuit parameters in the equivalent circuit of Figure 2(B) may be related to the design parameters of the transformer by simple analytic expressions. It has been found that a wide range of practical designs may be realized in terms of a simple 2-layer concentric-coil winding; therefore, only this type of winding design will be described in this paper. The design equations and properties of other types of windings are described in reference 1. The leakage inductance, L, and effective interwinding capacitance, C, of the 2-layer, concentric-coil winding are given in the following equations:

$$L=b\frac{4\pi N_p^2U}{d}\left(\Delta+\frac{a_1+a_2}{3}\right)10^{-9}\text{ henries} \qquad (1)$$

$$C=\frac{0.0885\,kdU}{3\Delta}10^{-12}\text{ farads} \qquad (2)$$

The shunt core-excitation circuit shown in Figure 2(B) may assume a variety of forms[5] depending on the problem under consideration. For the purposes of this paper, the core-excitation circuit will be considered as a single shunt inductance, M, related to the design parameters by the following equation:

$$M=\frac{4\pi N_p^2A}{10^9l}\mu_e\text{ henries} \qquad (3)$$

The effective pulse permeability, μ_e, usually is determined experimentally for a particular core material and takes into account eddy-current and hysteresis effects, in addition to the usual excitation ampere-turns. Pulse permeability depends on pulse duration, maximum flux density change, lamination thickness, and the magnetic and resistive properties of the core material. A complete discussion of core-excitation phenomena during pulse excitation is long and involved and the reader is, therefore, referred to reference 1 for a more detailed treatment of this subject.

Equivalent Circuits for the Step-Up Transformer

The complete primary-basis equivalent circuit of the step-up pulse transformer, coupling resistive primary and secondary impedances, and including external, shunt, primary and secondary capacitances, C_p and C_s, is shown in Figure 3(A). The secondary impedance, R_s, and external shunt capacitance, C_s, are secondary parameters referred to the primary by the square of the turns ratio. Examination of the expression for the total primary shunt capacitance, C_3, shows that the transformer introduces a term which is negative for turns ratios greater than one. Unless an abnormal amount of external shunt primary capacitance is present, C_3 is small and may be neglected in the design procedure.

For purposes of analysis, it is practicable to divide the transmitted pulse into three parts, consisting of: the rising front and subsequent damped oscillation, if present; the sloping top of the pulse; and the tail or subsidence transient. This division is based on the assumption of a rectangular pulse input (see Figure 4). The pulse front is represented very well by the equivalent circuit of Figure 3(B), which omits the excitation branch.

Symbols

a, a_1, and a_2 = wire diameter in centimeters
b = empirical correction factor in formula for leakage inductance
d = coil length in centimeters
e, e_0 = input and output voltages, respectively
k = dielectric constant
l = core length magnetic circuit, centimeters
n = turns ratio
p = derivative operator
t_0 = time in seconds for pulse to rise to 90 per cent flat pulse voltage, E
A = net core area, square centimeters
C = effective interwinding capacitance, farads
C_{pw} = effective primary winding-to-ground capacitance, farads
C_{sw} = effective secondary winding-to-ground capacitance, farads
C_p = external primary circuit capacitance, farads
C_s = external secondary-circuit capacitance referred to primary, farads
$C_{op}=C_{pw}+C_p$
$C_{os}=n^2C_{sw}+C_s$
$C_1=nC$
$C_2=n(n-1)C+n^2C_{sw}+C_s$
$C_3=(1-n)C+C_{pw}+C_p$
D = per unit pulse droop
E = basic flat-top pulse in volts
E_m = maximum value of output pulse in volts
E_δ = output pulse voltage at end of pulse duration
L = leakage inductance on primary basis, henrys
M = equivalent magnetizing inductance, henrys
$N_1=C_2/C_1$
$N_2=C_3/C_1$
N_p = primary turns
R_p = primary resistance, ohms
R_s = secondary resistance, referred to primary, ohms
T = base unit of time, $2\pi\sqrt{LC_2}$ for step-up transformer, $2\pi\sqrt{LC_3}$ for step-down transformer
U = mean coil perimeter, centimeters
$Z_0=\sqrt{L/C_2}$ for step-up transformer, $\sqrt{L/C_3}$ for step-down transformer
γ = impedance ratio, R_p/Z_0
Δ = interwinding insulation thickness, centimeters
δ = pulse duration, seconds
τ = per unit pulse rise time, t_0/T
μ_e = effective pulse permeability
λ = derivative operator

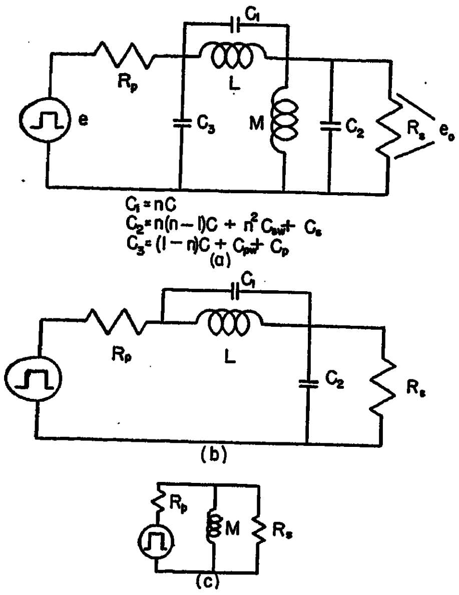


Figure 3. (A) Complete primary-basis equivalent circuit of step-up transformer and terminations

(B) Equivalent circuit for analyzing front of pulse

(C) Equivaent circuit for analyzing top of pulse

For the pulse top, only the excitation branch and terminating impedances are of importance and the transformer may be represented by the equivalent circuit of Figure 3(C). The equivalent circuit for the pulse tail in general involves all the circuit elements, including a more elaborate representation of the excitation circuit. For many practical applications the wave form of the pulse tail is not important or at least the wave forms resulting from the proposed design procedure are tolerable. A detailed discussion of this matter will be found in references 1 and 4.

Design of the Step-Up Transformer

The design procedure which is described in the following paragraphs provides a means for controlling the shape of he transmitted pulse on the front and top of the pulse. The design procedure is based on the assumption that the equivaent circuits of Figure 3 adequately repesent the pulse transformer and that he behavior of these circuits, for perfect ectangular pulse input, represents all possible wave forms for the transmitted pulse for a given set of circuit conditions. The design then is carried out in three main steps.

. For a given set of circuit conditions and urns ratio, to determine the transformer arameters L and C which give a satisfactory wave form, particularly on the pulse ont.

2. Construct a transformer with the required turns ratio and the L and C parameters as determined in step one.

3. Adjust the design until proper performance on the top and tail of the pulse is obtained.

Since only a limited range of pulse wave forms may be obtained, with perfect rectangular pulse input, it will be possible to define certain salient features of the transmitted pulse and incorporate these factors into the design procedure. With reference to Figure 4, three features of the pulse wave form are defined as follows:

1. The time t_0 for the pulse to rise to 90 per cent of the flat-top pulse voltage; that is the voltage which would be obtained if the transformer were ideal.

2. Amount of overshoot expressed as a percentage of the flat-top pulse voltage.

3. Amount of droop, D, which is the deficiency in pulse top voltage at the end of the pulse duration, δ, expressed in per unit of the flat-top pulse voltage.

The first two of these wave-form specifications are related to the pulse front and may be determined with the aid of the circuit of Figure 3(B). This circuit contains five independent parameters, and the determination of values of the two performance specifications for all possible combinations of these parameters would be a formidable task. However, consideration of the differential equations of the circuit shows that the performance of the circuit depends on only three dimensionless ratios of the five parameters and the definition of an arbitrary unit of time. These ratios which are derived in Appendix I are listed for reference:

Base unit of time, $T=2\pi\sqrt{LC_2}$

R_p/R_s

R_p/Z_0 (where $Z_0=\sqrt{L/C_2}$)

C_2/C_1

The determination of rise time, t_0, and per cent overshoot for a wide range of the above dimensionless parameters was made by means of a transient analyzer.[6,7] A typical set of these data for $C_2/C_1=7$ is shown in Figures 5 and 6. It is not possible to include all the data in this form because of space limitations. However, since an output wave form with zero per cent overshoot is desirable in many applications, Figures 7 and 8 have been prepared giving data in terms of per unit rise time, $\tau=t_0/T$ and $R_p/Z_0=\gamma$ versus the ratio, $C_2/C_1=N_1$ for several values of R_p/R_s and for zero per cent overshoot. Similar curve sheets for other values of per cent overshoot may be plotted from a complete set of basic data as illustrated by Figures 5 and 6.

It should be noted here that there are two values of the ratio γ for which zero per cent overshoot occurs with particular values of the ratios, R_p/R_s and N_1. These two values of γ will be referred to as zero per cent overshoot, low, or zero per cent overshoot, high, meaning the low or high value of γ for a particular set of values for the ratios, R_p/R_s and N_1. The occurrence of two critically damped points in the response of the circuit of Figure 3(B) is particularly interesting in that it makes possible two alternative designs for a particular set of specifications. This matter will be discussed later in the paper.

At this point, it is possible to find the two winding constants L and C in terms of fundamental specifications of the pulse wave form and external circuit parameters.

The two quantities τ and γ may be written as follows:

$$\tau=t_0/2\pi\sqrt{LC_2} \tag{4}$$

$$\gamma=R_p/\sqrt{L/C_2} \tag{5}$$

where $C_2=n(n-1)C+C_{os}$ and $C_{os}=C_s+n^2C_{ws}$. From equations 4 and 5

$$L=\frac{t_0R_p}{2\pi\tau\gamma} \tag{6}$$

$$C=\frac{1}{n(n-1)}\left(\frac{\gamma t_0}{2\pi\tau R_p}-C_{os}\right) \tag{7}$$

The leakage inductance, L, and effective interwinding capacitance, C, may be found, provided the quantities in equations 6 and 7 are known. Of the several quantities involved, t_0, R_p, n, and an estimated value of C_{os} will be immediately available. The values of τ and γ may be found from Figures 7 and 8 for zero per cent overshoot, low or high, provided R_p/R_s and N_1 are known. The ratio, R_p/R_s, will be given as part of the fundamental specifications, but the proper value of N_1 remains to be specified. In order to determine N_1 in terms of fundamental specifications of wave form

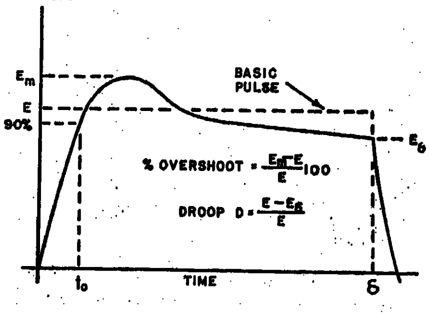


Figure 4. Specifications of pulse waveform

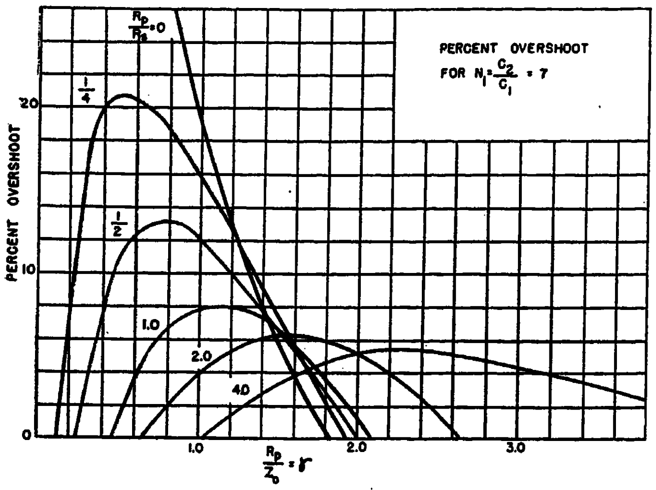


Figure 5. Percentage overshoot for step-up transformer; $C_2/C_1=N_1=7$

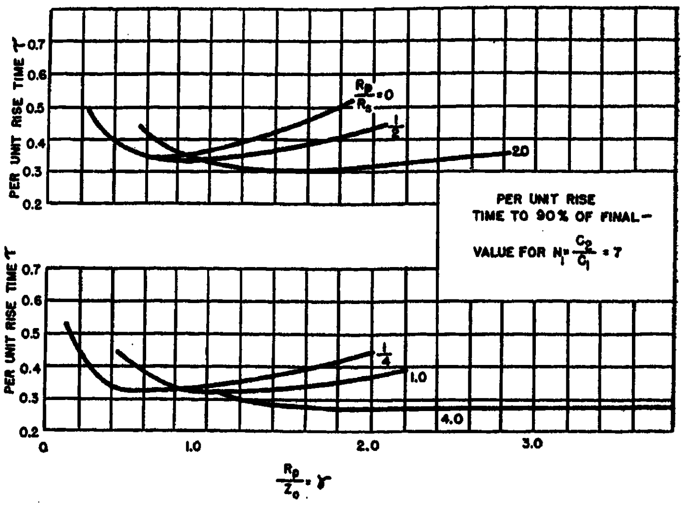


Figure 6. Per unit rise time for step-up transformer, $C_2/C_1=N_1=7$

and circuit parameters, the following calculation is made:
By definition

$$N_1=\frac{C_2}{C_1}=\frac{n(n-1)C+C_{os}}{nC}$$

Solving for C from the foregoing equation and using equation 7, the following equality is found

$$\frac{C_{os}}{nN_1-n(n-1)}=\frac{1}{n(n-1)}\left(\frac{\gamma t_0}{2\pi\tau R_p}-C_{os}\right)$$

When rearranged the foregoing equation becomes

$$\frac{t_0}{R_pC_{os}}=\left[\frac{2\pi\tau}{\gamma}\right]_{N_1}\frac{N_1}{N_1-(n-1)} \qquad (8)$$

Equation 8 is implicit in N_1, since τ and γ are functions of N_1. The equation may be solved by assuming values of N_1, evaluating the term in square brackets from Figures 7 and 8, and plotting the left-hand side of the equation as a function of N_1. Figure 9 shows the result of such a calculation for zero and five per cent overshoot, both low and high, with $R_p/R_s=1$ and a turns ratio $n=5$. Data for five per cent overshoot are included to illustrate the relative difficulty of designing for specified overshoot at high and low values of γ.

It should be noted that the curves of Figure 9 are functions of several parameters. Additional curve sheets must be prepared if one or more of these parameters are changed. This is a matter of only a few minutes calculation, even when interpolation on Figures 7 and 8 is required. It is also interesting to note that the curves of Figure 9 define certain ranges of compatibility among the several specifications.

The values of L and C for the transformer to give a specified wave form for specified terminating conditions now may be calculated. For example, if $R_p/R_s=1.0$, $R_p=50$ ohms, $t_0=10^{-7}$ second, $C_{os}=700\times10^{-12}$ farad, zero per cent overshoot, high, then $t_0/R_pC_{os}=2.85$ and from Figure 9, $N_1=6.8$. From Figures 7 and 8, for the derived value of N_1, $\tau=0.38$ and $\gamma=2.10$. From equations 6 and 7, $L=10^{-6}$ henry and $C=50\times10^{-12}$ farad.

The next step in the design procedure is to determine from equations 1 and 2 and the required values of L and C, the significant dimensions of the transformer core and coils. The manner in which this problem is handled will be guided by the following considerations.

1. A lower limit on wire size is set either by current loading or by mechanical strength and ease of winding.

2. A minimum is set on the interwinding insulation thickness Δ by the transformer voltage and the dielectric strength of suitable insulating materials.

3. The mean coil perimeter, U, and coil length, d, must be chosen to fit one of a standard set of cores.

Equations 1 and 2 may be put in the following form

$$N_p^2U^2=\frac{3\times10^{21}}{4\pi\times0.0885}\;\frac{LC}{bk}\;\frac{\Delta}{\left(\Delta+\frac{a_1+a_2}{3}\right)} \qquad (9)$$

$$d^2=\frac{12\pi10^3}{0.0885}\;\frac{N_p^2bC\Delta\left(\Delta+\frac{a_1+a_2}{3}\right)}{kL} \qquad (10)$$

In one form of the design procedure, it is convenient to start by assuming an appropriate wire size, and a value for Δ at least as large as required by the transformer voltage rating and the dielectric strength of the insulating material to be used.

1. With the chosen values of a_1, a_2, and Δ calculate $N_p^2U^2$ from equation 9.

2. Select a core and its corresponding value of U^2 from Table I and calculate N_p. As a rule of thumb, limiting the ratio E/N_p to about 100 to 150 volts per turn, maximum will give a reasonable design. In Table I, values of U^2 are tabulated for several of the cores in a standard line. The value of U is based on a 1/16-inch clearance from core to mean coil perimeter all around. This clearance usually will prove satisfactory for values of Δ less than 60 mils. Tables of this sort for the standard lines of cores and for several values of clearance are extremely valuable in this design procedure.

3. With the value of N_p calculated previously, determine d from equation 10. The value of d must be such that the coil may be wound on one or both legs of the core with adequate creepage margin at the coil ends and the product of required secondary turns by the wire diameter, a_2, does not exceed d.

4. In case the required value of d is not satisfactory, steps 1, 2 and 3 must be repeated. Perhaps a core with a larger value of U may be used or Δ, a_1 and a_2 may be changed. In some cases, an insulating material with larger dielectric constant k, and dielectric strength must be used. In any event, equations 9 and 10 indicate the

Table I. Significant Dimensions for a Standard Line of Cores

Style Number	Core Leg: Width, Inches	Core Leg: Depth, Inches	Core Window: Width, Inches	Core Window: Length, Inches	U Centimeters*	U^2, Square Centimeters	A/l, Centimeters
1356250	1/4	3/16	1/4	5/8	3.49	12.2	0.042
1356254	1/2	1/4	1/4	7/8	5.08	25.8	0.087
1356261	1/2	1/2	1/2	1 9/16	6.35	40.3	0.111
1356262	5/8	1/2	1/2	1 9/16	6.99	48.8	0.138

* 1/16-inch clearance from core to mean perimeter all around.

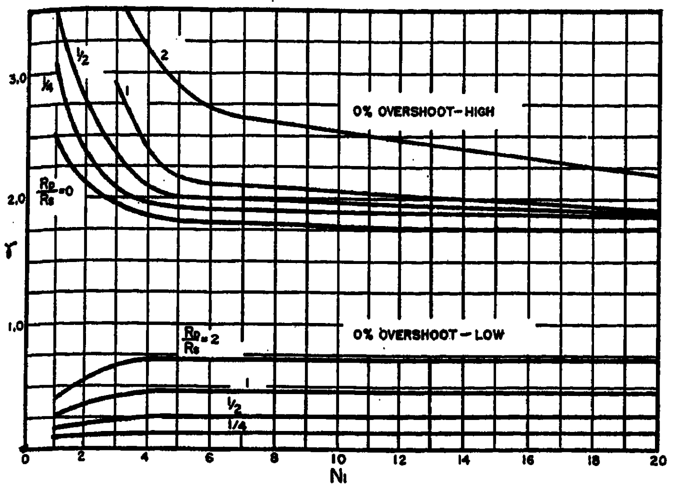


Figure 7. Impedance ratio, γ, versus N_1 for zero per cent overshoot, high and low, step-up transformer

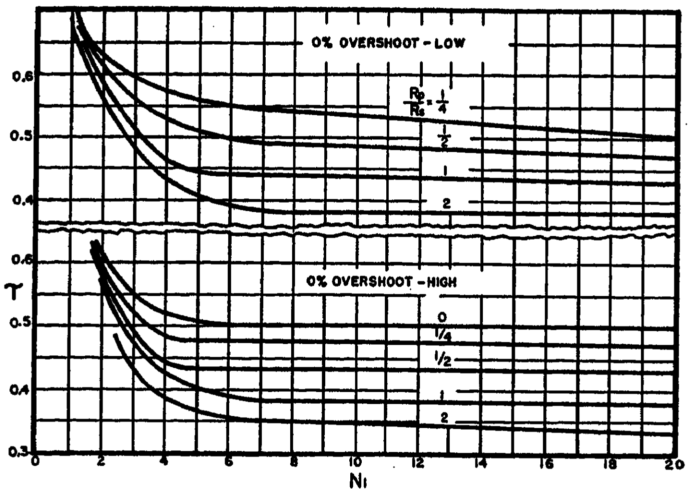


Figure 8. Per unit rise time, τ, versus N_1 for zero per cent overshoot, low and high, step-up transformer

several possibilities for effecting the proper change in design.

In the design procedure just described, a series of cuts and trys will lead to a choice of core, coil dimensions, and coil turns. However, two critical points arise in the design at step 3. The second of these points could be avoided if it were possible to find a relation such that the chosen wire diameter would just fill the required length, d, with the required number of secondary turns. Fortunately, such a relation can be developed. If the secondary and primary wire sizes are assumed to be equal, $a_1=a_2=a$, then equation 10 may be put in the following form

$$\frac{d^2}{a^2n^2N_p{}^2}=\frac{12\pi10^8}{0.0885}\,\frac{\frac{\Delta}{a}\left(\frac{\Delta}{a}+\frac{2}{3}\right)bC}{n^2kL} \tag{11}$$

The denominator of the term on the left of the equal sign is just equal to d^2 if the wire diameter a includes an allowance for the winding space factor. Therefore, equation 11 gives the following relation when solved for Δ/a

$$\frac{\Delta}{a}=-\frac{1}{3}+\sqrt{\frac{1}{9}+\frac{0.0885}{12\pi10^8}\,\frac{n^2k}{b}\,\frac{L}{C}} \tag{12}$$

Equation 12 determines uniquely the ratio of interwinding insulation thickness to wire diameter so that the secondary always may be wound with a single-strand closely spaced winding in the required coil length, d, with the required number of secondary turns. If the relation of equation 12 is satisfied, it will be necessary in step 3 to satisfy only the first requirement.

Equations 9 and 10 or 9 and 12 provide the starting point for designing a coil with circuit constants required to give a specified rate of rise and overshoot on the pulse wave form. There remains the problem of controlling the amount of droop on the pulse top. This characteristic sloping of the pulse top is due to the transformer exciting current flowing through the primary resistance, R_p. An approximate expression for the amount of droop, D, is derived in Appendix II and is given here for reference.

$$D=\frac{R_sR_p}{R+R_p}\,\frac{\delta}{M} \tag{13}$$

From equation 3 for the equivalent transformer magnetizing inductance and equation 13, it is found that the following inequality must hold if a specified amount of droop, D, is not to be exceeded.

$$N_p{}^2\mu_e>\frac{10^9}{4\pi}\,\frac{l}{A}\,\frac{R_pR_s}{(R_s+R_p)}\,\frac{\delta}{D} \tag{14}$$

It has been found convenient to design the transformer by means of equations 9 to 12 and then to check the droop requirements with equation 14. Equation 14 indicates the influence of the various parameters on droop and serves as a guide in altering a preliminary design.

The preceding discussion has described a design procedure for step-up transformers of the iso-type (that is, a transformer with isolated primary and secondary windings). Corresponding design equations for auto-type transformers are given in Appendix III.

Example of Step-Up Transformer Design

Assume the following transformer specifications:

1. Step-up iso-type transformer; $n=5$.
2. Primary resistance, $R_p=200$ ohms.
3. Primary-secondary impedance ratio, $R_p/R_s=1$.
4. Secondary external shunt capacitance, $C_{os}=75\times10^{-12}$ farads.
5. Pulse duration, $\delta=10^{-6}$ seconds.
6. Pulse front, $t_0=1/3\times10^{-7}$ seconds, zero per cent overshoot, high.
7. Pulse top, $D<0.05$.
8. Wire size number *33;* effective diameter, $a=0.0153$ centimeter.
9. Interwinding insulation, Kraft paper and oil; dielectric constant, $k=3.0$; dielec-

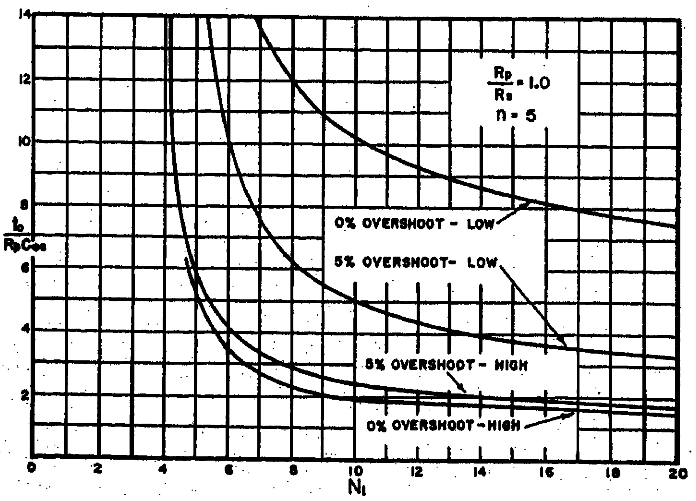


Figure 9. Graph of equation 8 for $R_p/R_s=1$, $n=5$

tric strength approximately 300 volts per mil.

10. Core, 2-mil Hypersil.

The first step in the design procedure is to find the proper values of L and C. From the specifications, $t_0/R_pC_{os}=2.22$, and from Figure 9, the required value of $N_1=8.5$. The proper values of τ and γ are found on Figures 7 and 8, and L and C are computed from equations 6 and 7, $L=1.36 \times 10^{-6}$ henry and $C=3.40 \times 10^{-12}$ farad.

This transformer will be constructed with a single-strand closely spaced secondary; therefore, the ratio, Δ/a, may be found from equation 12. The inductance correction factor, b, is assumed equal to one.

$$\frac{\Delta}{a}=-\frac{1}{3}+\sqrt{\frac{1}{9}+\frac{0.0885}{12\pi 10^{3}}\,\frac{25\times 3}{1}\,\frac{1.36\times 10^{-6}}{3.4\times 10^{-12}}}$$

$$\frac{\Delta}{a}=8.07$$

$a=0.0153$ centimeter or 6 mils
$\Delta=0.123$ centimeter or 48.4 mils

From equation 9, the core primary-turns factor, $N_p^2U^2$, is computed as follows

$$N_p^2U^2=\frac{3\times 10^{21}}{0.0885\times 4\pi}\,\frac{1.36\times 3.4\times 10^{-18}}{3.0}\,\frac{0.123}{0.133}$$

$$N_p^2U^2=3.85\times 10^{3}$$

At this point, a cut-and-try procedure to select primary turns and core is employed. Only the correct try will be shown here. Use of the values of U from Table I will facilitate this procedure. It is found that core number *1356254* is about right. Allowing 1/32-inch clearance over-all on the core dimensions, so that the coil will slip over the core, an exact value of U is calculated as follows:

Core dimensions:	0.500 inch	0.250 inch
Clearance:	0.031	0.031
Twice primary to core insulation thickness:	0.030	0.030
Twice primary wire diameter:	0.012	0.012
Interwinding insulation thickness:	0.048	0.048
	0.621 inch	0.371 inch

$U = 2(0.621 + 0.371) \times 2.54 = 5.05$ centimeters

$N_p=12.3$ or 12 primary turns

The transformer was constructed with a 12-turn primary, five wires in parallel, and a closely spaced winding. The secondary was wound with a 60-turn single-strand closely spaced winding. The coil length, $d=5 \times 12 \times 0.006 = 0.360$ inch is such that the coil will fit on one leg of the specified core with ample end margin on the coil.

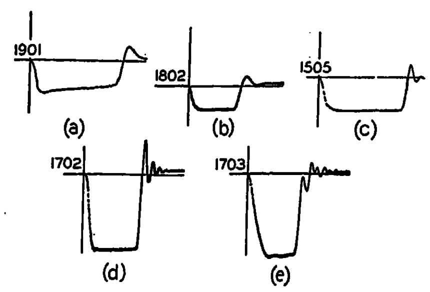


Figure 10. Oscillograms of output wave forms of step-up and step-down transformers constructed according to the proposed design procedure

(A) Zero per cent overshoot, high, $n=5$, $R_p=200$, $R_p/R_s=1$, $C_{os}=75\times 10^{-12}$, $t_0=1/3\times 10^{-7}$, $\delta=10^{-6}$

(B) Zero per cent overshoot, low, $n=1/5$, $R_p=312$, $R_p/R_s=1/4$, $C_{op}=5\times 10^{-12}$, $t_0=10^{-7}$, $\delta=10^{-6}$

(C) Zero per cent overshoot, low, $n=2$, $R_p=50$, $R_p/R_s=1$, $C_{os}=56\times 10^{-12}$, $t_0=10^{-7}$, $\delta=10^{-6}$

(D) Zero per cent overshoot, low, $n=5$, $R_p=50$, $R_p/R_s=1$, $C_{os}=175\times 10^{-12}$, $t_0=10^{-7}$, $\delta=10^{-6}$

(E) Zero per cent overshoot, low, $n=10$, $R_p=20$, $R_p/R_s=1$, $C_{os}=700\times 10^{-12}$, $t_0=2\times 10^{-7}$, $\delta=10^{-6}$

The transformer was constructed according to the foregoing design, and bridge measurements of L and C gave $L=1.35 \times 10^{-6}$ henry and $C=5.8 \times 10^{-12}$ farad. The output pulse of the transformer is shown in the oscillogram of Figure 10(A).

Further Aspects of the Design Procedure

There are several features of the design procedure which should be emphasized at this point.

1. The method is basically a means for controlling certain features of the output pulse wave form resulting from a perfect rectangular input pulse. The qualitative correlation of response to other types of input wave forms by frequency spectrum considerations is well known and is an essential feature in the design procedure.

2. Transformer rating is fixed by the choice of wire diameter and interwinding insulation thickness. They may be chosen independently, with some complication in design procedure, or they may be chosen on the basis of equation 12. If equation 12 is used the interwinding insulation thickness and wire diameter are not independent and certain restrictions will arise. If, for example, wire size is fixed only by mechanical considerations, Δ may be adjusted by choice of wire size. Where Δ and a must both be greater than certain minimum values, it may be advantageous to have some control over the ratio, Δ/a. This may be accomplished in two ways.

Firstly, there is usually freedom in the choice of the zero per cent overshoot point for either low or high values of γ. The choice of low values of γ will result in larger values of the ratio, Δ/a, than the choice of the corresponding high value for γ. Secondly, for a particular choice of γ, high or low, the ratio, Δ/a, may be varied by properly adjusting the value, N_1. Figure 9 shows that N_1 depends on the ratio, t_0/R_pC_{os}. It may be possible to change one or more of these basic specifications to achieve a required ratio, Δ/a. For example, on the zero per cent overshoot, high, curve of Figure 9, decreasing t_0 results in an increase in the required value of N_1. Calculation will show that this procedure increases the ratio Δ/a, other factors being held constant. It is also true that the Δ/a ratio will be larger for the autotype transformer.

3. The physical size of the transformer is controlled largely by the considerations discussed in part 2. For example, choice of the minimum mechanically or electrically feasible wire size will lead to the smallest possible transformer. However, it should be noted that even for a given wire size there is freedom of choice in the physical proportions of the transformer through the relations of equation 9. For example, a design may be realized for several different values of primary turns N_p, resulting in a choice among several of a given line of cores.

It should be noted that the physical size of the transformer is also related to the pulse duration, δ, through the rise time, t_0. If t_0 is to be a fixed percentage of the pulse duration, reduction of pulse duration requires a corresponding reduction in t_0. Reference to equations 6 and 7 shows that the transformer parameters, L and C, will change in proportion to the change in t_0. A study of equations 9 and 10 shows that as L and C are reduced in magnitude, the physical dimensions of the transformer also must decrease.

4. The primary winding for step-up transformers will be wound with two or more strands in parallel to give a closely spaced winding. This is necessary in order to satisfy the approximations under which the basic formulas for leakage inductance and interwinding capacitance were derived. Because of this requirement, two difficulties arise in practical design.

First, nonuniform distribution of current in the parallel strands, particularly for ratios of Δ/a less than about two,

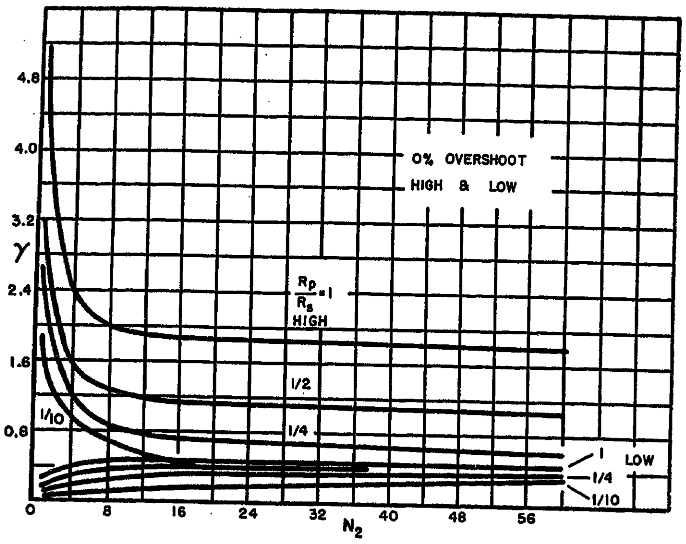


Figure 11. Impedance ratio, γ, versus N_2 for zero per cent overshoot, high and low, step-down transformer

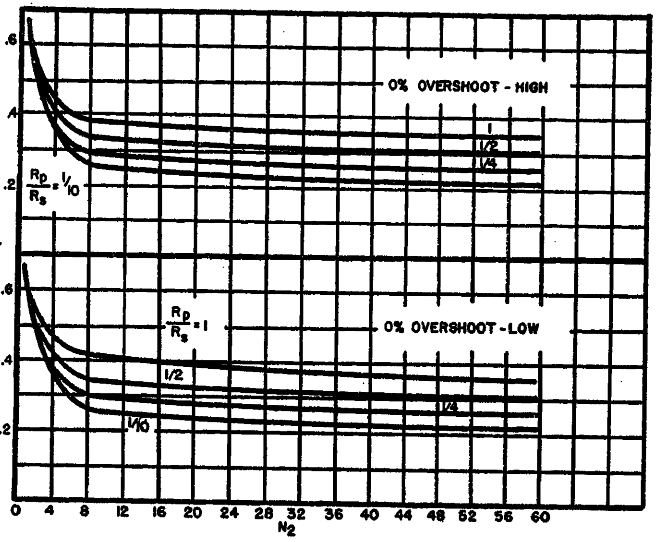


Figure 12. Per unit rise time, τ, versus N_2 for zero per cent overshoot, high and low, step-down transformers

cause the actual leakage inductance to depart from the required design value. This difficulty has been taken care of in the design procedure by introduction of the empirical factor, b, in the inductance formula. This empirical factor will range from one to two in magnitude and must be found by a series of approximating designs.

Secondly, for nonintegral ratios of transformation, it is difficult to obtain closely wound primary and secondary windings of practically equal length with equal primary and secondary wire sizes. With small wire size and low turns ratio, the difference in winding lengths may not be significant, and any small variation in parameters may be corrected by a proper choice of the factor, b. However, for other situations, it may be necessary to use unequal primary and secondary wire sizes. The extension of equations 11 and 12 to cover this situation is required.

Equivalent Circuit for the Step-Down Transformer

The complete primary-basis equivalent circuit of the step-down transformer is the same as that shown in Figure 3(A). All circuit elements shown will have the same interpretation as for the step-up transformer, except the turns ratio, n, will be numerically less than one. For the step-down transformer, the total secondary shunt capacitance, C_2, contains a term which is negative for turns ratios less than one. In many cases, the winding-to-core and external shunt capacitances, n^2C_{sw} and C_s respectively, are such that C_2 is negligibly small and may be assumed to be zero. Considerable data, too extensive to present in this paper, have been taken where C_2 is not small. It has been found that a design procedure based on the assumption, $C_2=0$, leads to satisfactory designs in all but extreme cases.

Therefore, under the assumption $C_2=0$, the dimensionless ratios and base unit time for the step-down transformer are found to be as follows:

Base unit of time, $T=2\pi\sqrt{LC_3}$

R_p/R_s

R_p/Z_0 (where $Z_0=\sqrt{L/C_3}$)

$C_3/C_1=N_2$

Design of the Step-Down Transformer

The design procedure for the step-down transformer is similar to that used for the step-up transformer. The basic pulse-front data giving per unit rise time, τ, and impedance ratio, γ, for zero per cent overshoot, both high and low, are shown in Figures 11 and 12. In these illustrations, the abscissa is the capacitance ratio, $C_3/C_1=N_2$, and curves are given for different values of R_p/R_s. For the step-down transformer, $\tau=t_0/2\pi\sqrt{LC_3}$ and $\gamma=R_p/\sqrt{L/C_3}$.

From the foregoing definitions, the required leakage inductance and effective interwinding capacitance of the transformer may be found.

$$L=\frac{t_0R_p}{2\pi\tau\gamma} \tag{15}$$

$$C=\frac{1}{1-n}\left(\frac{\gamma t_0}{2\pi\tau R_p}-C_{op}\right) \tag{16}$$

where $C_{op}=C_p+C_{wp}$. The relation between the capacitance ratio, N_2, and basic design specifications is found to be

$$\frac{t_0}{R_pC_{op}}=\left[\frac{2\pi\tau}{\gamma}\right]_{N_2}\frac{nN_2}{nN_2-(1-n)} \tag{17}$$

Equation 17 is analogous to equation 8 and is used in the determination of L and C from Equations 15 and 16, exactly as described in the case of the step-up transformer.

Equations 9 and 10 may be used without modification for the design of iso-type step-down transformers. Equations 11 and 12 will be changed because the primary winding is now the high-voltage winding and the coil length, d, for a single-strand close-wound primary is aN_p. Equation 18 gives the ratio, Δ/a, for the iso-type step-down transformer. The corresponding equation for the auto-type step-down transformer will be found in Appendix III.

$$\frac{\Delta}{a}=-\frac{1}{3}+\sqrt{\frac{1}{9}+\frac{0.0885}{12\pi 10^3}\frac{k}{b}\frac{L}{C}} \tag{18}$$

Equations 13 and 14 are to be used in checking the top-of-the-pulse characteristics of the design. An oscillogram of a typical step-down design is shown in Figure 10(B).

Conclusions

A technique for designing pulse transformers has been described. This design method takes into account the transformer terminating impedances and permits control of certain specified features of the output wave form. The procedure is straightforward leading to an optimum design for a suitable set of transformer specifications. The design relations indicate clearly the possibility of realizing a design for a given set of specifications and, in many cases, provide several alternative designs to fit a number of special requirements.

The design method described is of broader interest in that it is illustrative of a technique which has applications in other fields. The availability of high-speed computing machines such as the transient analyzer makes possible the analysis and synthesis of circuits from the transient point of view. This possibility of course has been recognized in the past, but without the computing machine, most problems require a prohibitive amount of labor.

Appendix I. Dimensionless Parameters

If a step voltage of unit magnitude is applied to the circuit of Figure 3(B), the operational form of the load voltage, e_0, will be

$$e_0(p)=\frac{R_s(1+p^2LC_1)}{(R_p+R_s)+(L+R_pR_sC_2)p+[R_s(C_1+C_2)+R_pC_1L]p^2+R_pR_sLC_1C_2p^3}$$

Now, instead of using seconds as a unit of time, a new dimensionless time unit will be introduced, $t_1=t/2\pi\sqrt{LC_2}$. In other words, one per unit time is equivalent to $2\pi\sqrt{LC_2}$ seconds. When this change is made in the independent variable, e_0 can be expressed as an operational function of λ where $\lambda=d/dt_1=2\pi\sqrt{LC_2}p$.
The above substitution gives

$$e_0(\lambda)=\frac{2\pi\left(4\pi^2+\frac{C_1}{C_2}\lambda^2\right)}{8\pi^3\left(1+\frac{R_p}{R_s}\right)+4\pi^2\frac{R_p}{Z_0}\times\left[1+\frac{R_p}{R_s}\left(\frac{Z_0}{R_p}\right)^2\right]\lambda+2\pi\left[1+\left(1+\frac{R_p}{R_s}\right)\frac{C_1}{C_2}\right]\lambda^2+\frac{R_pC_1}{Z_0C_2}\lambda^3}$$

where $Z_0=\sqrt{L/C_2}$

From this equation, it can be seen that the time form of the solution for e_0 will depend not upon the magnitudes of the parameters, R_p, R_s, L, C_2, C_1, but upon the dimensionless ratios R_p/R_s, R_p/Z_0, and C_1/C_2. This permits the scaling of a particular problem to fit the frequency and impedance range of the transient analyzer and also permits a more concise presentation of data than would be possible using the actual circuit constants.

Appendix II. Calculation of Pulse Droop

Figure 3(C) is the equivalent circuit of the pulse transformer after the pulse output has reached a quasi-steady-state value. The leakage inductance and winding capacitances will have negligible effect upon the average output voltage on the top of the pulse. If a pulse voltage equal to $(1+R_p/R_s)E$ is applied to the primary of the circuit of Figure 3(C), the secondary voltage initially will be equal to E and the current through the magnetizing inductance initially will be equal to zero. If the secondary voltage remains at the value, E, for the pulse duration, δ, the current through the inductance, M, at the end of the pulse will be $E\delta/M$. This current flowing through R_p will cause the secondary voltage to droop below E in magnitude. The per unit droop, D, where $D=(E-E_\delta)/E$ can be found analytically as follows. If a step voltage of magnitude $(1+R_p/R_s)E$ is applied to the circuit of Figure 3(C), the actual instantaneous secondary voltage as a function of time will be

$$e=E\epsilon^{\frac{-R_pR_s}{M(R_p+R_s)}t}$$

For the usual transformer application δ is much smaller than $M(R_p+R_s)/R_pR_s$ so that at the end of the pulse duration, the secondary voltage will be approximately,

$$E_\delta=E\left[1-\frac{\delta R_pR_s}{M(R_p+R_s)}\right]$$

and

$$D=\frac{\delta R_pR_s}{M(R_p+R_s)}=\frac{\delta R_p}{M(1+R_p/R_s)}$$

Appendix III. Design Equations for Auto-Type Transformers

In the auto-type of pulse transformer, a portion of the secondary turns are closely coupled to the primary turns, either by winding side by side or by having two layers of winding separated by only a few mils of insulation. The closely coupled primary and secondary turns are usually made equal. With this arrangement, the following design equations result:

$$L=\left(\frac{n-1}{n}\right)^2 b\frac{4\pi N_p{}^2U}{d}\left(\Delta+\frac{a_1+a_2}{3}\right)10^{-9} \quad (19)$$

$$C=\frac{(n-1)^2+(n-1)+1}{(n-1)^2}\;\frac{0.0885kUd}{3\Delta}10^{-12} \quad (20)$$

$$N_p{}^2U^2=\frac{n^2}{(n-1)^2+(n-1)+1}\;\frac{3\times10^{21}}{4\pi\times0.0885}\times\frac{LC}{bk}\;\frac{\Delta}{\left(\Delta+\frac{a_1+a_2}{3}\right)} \quad (21)$$

$$d^2=\frac{(n-1)^4}{n^2[(n-1)^2+(n-1)+1]}\times\frac{N_p{}^2bC\Delta\left(\Delta+\frac{a_1+a_2}{3}\right)}{kL} \quad (22)$$

For the step-up transformer

$$\frac{\Delta}{a}=-\frac{1}{3}+\sqrt{\frac{1}{9}+\frac{0.0885}{12\pi10^3}\;\frac{n^2[(n-1)^2+(n-1)+1]}{(n-1)^2}\;\frac{kL}{bC}} \quad (23)$$

For the step-down transformer

$$\frac{\Delta}{a}=-\frac{1}{3}+\sqrt{\frac{1}{9}+\frac{0.0885}{12\pi10^8}\;\frac{n^2[(n-1)^2+(n-1)+1]}{(n-1)^4}\;\frac{kL}{bC}} \quad (24)$$

References

1. Pulse Generators (book), G. N. Glasoe, J. V. Lebacqsz. Massachusetts Institute of Technology Radiation Laboratory Series, volume 5, McGraw-Hill Book Company, Inc., New York, N. Y., 1948, chapters 12–15.

2. Traveling Waves on Transmission Systems (book), L. V. Bewley. John Wiley and Sons, Inc., New York, N. Y., 1933, chapters 12–16.

3. Magnetic Circuits and Transformers (book), Massachusetts Institute of Technology Staff. John Wiley and Sons, Inc., New York, N. Y., 1943, pages 489–90.

4. Electronic Transformers and Circuits (book), Reuben Lee. John Wiley and Sons, Inc., New York, N. Y., 1947, chapter 9.

5. Equivalent Circuit of a Pulse Transformer Core, H. L. Rehkof. Radiation Laboratory Report Number 666, Massachusetts Institute of Technology (Cambridge, Mass.), March 20, 1945.

6. System Recovery Voltage Determination by Analytical and A-C Calculating Board Methods, R. D. Evans, A. C. Monteith. *Electrical Engineering* (*AIEE Transactions*), volume 56, June 1937, pages 695–705.

7. A New Device for the Solution of Transient-Vibration Problems by the Method of Electrical-Mechanical Analogy, H. E. Criner, G. D. McCann, C. E. Warren. *Journal of Applied Mechanics* (New York, N. Y.), September 1945, pages 135–39.

No Discussion

Protection of the Airplane Main Bus

D. W. EXNER
MEMBER AIEE

INCREASED thought has been directed in recent years toward protection of the airplane electric system so as to improve its reliability and reduce the hazards which result from its failure. Up to and including the early years of World War II, little attention was paid to electric system protection beyond the obvious necessities of reverse-current relaying on the generator circuits and the use of fuses and small circuit breakers on the distribution circuits. During these years the size of the system was growing rapidly and the primary system voltage was advanced from 14 to 28 volts direct current. From the 1,500-watt 14-volt system on a popular pre-war commercial transport, system capacities advanced to 11.2 kw at 28 volts on the early *B-17* bombers and eventually to 50 kw on the *B-29*'s. Because there is little sign of slowing up in the trend toward greater installed capacities, particularly on military airplanes, higher voltage a-c and d-c systems have been in the process of development for the past ten years. Until very recently the greater emphasis has been on the 160-kva 208-volt 3-phase 400-cycle system because of its selection by the Air Force for the *B-35* and *B-36* bombers, and still larger versions of this system are in the offing. After a slow start some years ago, the 115-volt d-c system is finally undergoing a new intensive development for certain military applications.

In view of this growth in size and because greater dependence is placed on electric power on modern airplanes, certain phases of system protection are receiving much attention at the present time.[1,2] This paper will discuss methods for protecting the main bus. To set the stage, a brief history of airplane circuit protection is reviewed first.

Review of Protection History

As the electric systems became larger and operating conditions more severe, the circuit protection deficiencies were demonstrated by a number of cases of fires resulting from ground faults on the generator feeders and main bus. In some cases the generator contactor failed because of insufficient arc rupturing capacity at high altitude or because of contacts welding. Even if the contactor operated correctly, there was no provision for deenergizing the field in the case of a faulted generator or generator feeder. Out of these difficulties better designs of reverse-current contactors appeared,[3] but the need for some form of back-up protection was apparent. Fusible fault limiters designed to blow above normal overload levels proved to be a solution of sorts. Another solution was the use of manually-reset reverse-current circuit breakers located between the bus and the generator contactors and having auxiliary contacts to open the generator field circuit when the circuit breaker tripped. Initial experience with these circuit breakers was unfortunate, to say the least, because their lack of time delay allowed them to trip on normal system reverse-current surges and the necessity of locating them at the nacelles, adjacent to the generator contactors, made them inaccessible for manual resetting. Subsequent provision of some inverse time delay to these circuit breakers minimized the danger of nuisance tripping, and the modern tendency toward the use of a centralized bus in the fuselage permits access for resetting.

In the early stages of development of the 208-volt 400-cycle system in 1943, it was agreed that comprehensive alternator circuit fault protection was desirable, and the necessary equipment and control schemes were developed. Figure 1 shows a representative single-line diagram of the alternator protection. This provides differential current protection against alternator line-to-line and line-to-ground faults and includes the feature of opening the field circuit in case of fault. Alternator feeder faults are cleared by the use of multiple conductors with fusible fault limiters of an improved type[4] in each end of each wire. A form of back-up protection is provided by a thermal delay relay which removes the alternator from the line and opens its field circuit when the alternator excitation is maintained at ceiling level for longer than about six seconds.[5] As this is a nonselective type of protection, it is not intended that it should act except for such severe conditions as a major main bus fault or a bogged-down system caused by heavy motor starting loads when the generating capacity has been abnormally reduced by heavy combat damage.

Within the last two years good progress has been made in the development of integrated generator control and protection equipment for the 28- and 115-volt d-c systems. Up to the present time, the details which constitute the electric system usually have been designed and procured to individual detail specifications which ostensibly cover every requirement and condition of service, including the interaction with other pieces of equipment. With the inevitable different sources of supply, and the practical impossibility of writing a "hole-proof" specification, the result has been that our systems were nonhomogeneous in performance and deficient in protection. Now for the first time the d-c generation system has been studied as a whole by individual equipment manufacturers from the viewpoint of established industrial power practices, with the result that a much higher order of protection is offered with a relatively small increase in over-all weight and with greatly improved facility of maintenance.[1,2] In addition to genera-

---

Paper 48-203, recommended by the AIEE Air Transportation Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Middle Eastern District Meeting, Washington, D. C., October 5–7, 1948 and at the Pacific General Meeting, San Francisco, Calif., August 23–26, 1949. Manuscript submitted July 28, 1948; made available for printing August 12, 1948.

D. W. EXNER is Research Engineer with the Boeing Airplane Company, Seattle, Wash.

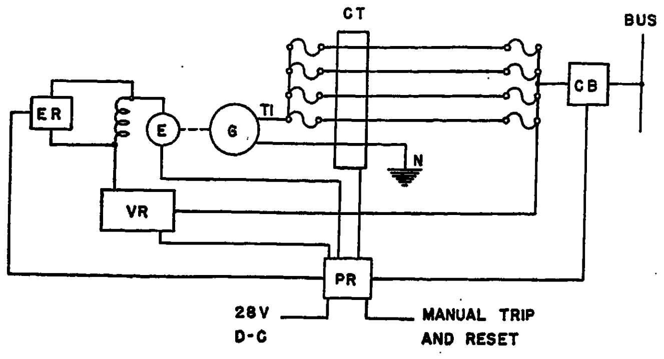


Figure 1. 400-cycle 3-phase alternator protection. Simplified single-line diagram, showing multiple-conductor feeder

G=alternator (phase 1 shown); E=exciter; CB=alternator circuit breaker; CT=differential-current transformer and relay; PR=pilot relay, exciter control; ER=exciter protection relay, thermal delay; VR=voltage regulator

tor and generator feeder fault protection, over-voltage protection is provided.

Since the earliest days of airplane electric systems, distribution circuits have had fuse or circuit breaker protection designed in general to protect the wire against dangerous overload. In many cases this has not been completely successful in preventing damage to the wire but generally has been able to interrupt in time to prevent fires. Particularly in commercial airplanes, but also in military airplanes, because of the adverse psychological effects, it is very desirable to avoid reaching wire temperatures at which the insulation gives off smoke or noisome odors. Distribution circuit breaker design has not fully kept pace with the increased fault current capacity of modern systems. Even the wartime *B-29* system, with armament amplidyne motor-generators in operation, was able to develop a peak fault current in excess of 12,000 amperes for a fault close to the main bus. The inadequate ultimate rupturing rating of small circuit breakers has in some cases required that back-up protection be provided by fusible limiters where it is necessary to tap loads from or near the main bus. Present practice in the 208-volt 400-cycle systems is to use three or more multiple circuits with fault limiter protection at each end of each wire so that a wire which develops a fault will be removed without losing the entire circuit. While this method is effective, the maintenance involved in periodic inspection of the many limiters required is excessive. This same protection may, perhaps be used on the 115-volt d-c system but will require careful study because of the limited sustained fault current capacity of the self-excited system operating without a battery and at a wide range of generator speed. Other methods of load feeder protection have been suggested, using current-balance or combined reverse-current and over-current principles, but to date none of these have been developed to the degree necessary for their use on airplanes. It should be mentioned that any method requiring the use of one or more pilot wires is not looked on with favor because of the added weight and increased vulnerability, particularly under combat conditions. While this feature may be tolerated within reason in generator protection schemes the much greater number of distribution feeder circuits makes it impractical.

The preceding review of the protection situation in airplane electric generation and distribution systems indicates that suitable methods are fairly well in hand with two exceptions:

1. Further work is needed on simpler and more effective methods of load feeder protection.
2. Main bus protection methods have not been developed to a satisfactory stage.

This paper concerns primarily a discussion of main bus protection.

The Need for Main Bus Protection

Practically all of the American multiengine airplanes flying today have an electric system in which two to six d-c generators or alternators are normally paralleled on a main bus from which the various distribution circuits extend, usually in a simple radial configuration.[6] The question of parallel versus isolated operation has been discussed many times in recent years, but it is generally conceded that motor starting and, in some cases, fault clearing requirements are met with minimum total generating capacity in a paralleled system.[7,8] Modern large airplanes have relatively large total motor loads, with a few individual motors approaching in some cases the rating of each generator. On the d-c systems some of the larger motors and a rather high percentage of the total motor load will be shunt or compound wound, so that the motor contribution to peak fault current will be high.[9]

The main bus in the paralleled system constitutes a principal vulnerable point, damage to which can disable the entire electric system. Recognition of this fact influences the designer to devote special attention to design and location of the bus structure so as to reduce the possibility of damage. For commercial airplanes at the present time, this is the best solution and is preferable to the added complication and weight of protective relaying. For combat airplanes such design treatment is, of course, necessary also, but some suitable form of quick-acting automatic protection is needed badly.[10] No amount of care in design, installation, or even armoring can eliminate the chance of missile damage completely. A direct hit on a bus compartment, or a nearby explosion, is almost certain to produce an electric fault either by impact damage to the conductors or by tearing and curling adjacent structure into contact with the bus. Methods for minimizing the results of such damage will be discussed.

Protection by Reducing Exposure

The first principle of bus protection is, naturally, to decrease the exposure area. Most multiengine airplane designs up to and including the war years used a wing-to-wing main d-c bus, shown in Figure 2. This showed weight economy by permitting engine starter and accessory loads to be tapped off in the nacelles instead of running separate feeders back from the center section. Although some postwar designs still use this method, it cannot be considered good practice from the safety standpoint. In such cases, the bus occupies the same general area with hydraulic lines and mechanical control cables and is adjacent to fuel tanks and lines. It is in a hot area when thermal wing de-icing is used. Extra precautions must be taken against abrasion failures of insulation and possible cable damage during maintenance work on other facilities in that area. Under combat conditions, it is extremely vulnerable to missile damage. If the bus itself is concentrated in the center section, the generator feeders which have to run through the wing section can be protected fairly well by present current-balance methods.

Location of the main bus in the fuselage offers maximum reduction in exposure area and incidentally improves the voltage regulation. The effective size of the bus is then determined mainly by the number and size of the generator contactors and load feeders which must be attached to it. It may be beneficial to move many of the secondary load circuits to separate load busses which are fed from the main bus through multiple conductors individually protected at each end by fusible fault limiters. The area around, and particularly above, the main and load busses should be kept clear of fuel, hydraulic, alcohol, and oxygen lines. In a recent case which emphasizes this point, a mechanic was tightening a connection to a circuit breaker at a load point. His screwdriver slipped and made contact with an oxygen line, starting a fire which caused heavy damage to the airplane before it was extinguished.

Much can be done to reduce the vulnerability of a bus by careful attention to arrangement and insulation so that foreign objects such as loose washers or small tools cannot create a fault. During the war, a new *B-29* was lost in Puget Sound because of conditions initiated by a fault which resulted from a loose washer lodging between two terminals of a reverse-current contactor. In this case, the design of the contactor was at fault, offering an example of the need for adequate enclosure and for insulating terminal guards on electric equipment.

Insulated Bus Compartments

At the present time, the use of bus compartments constructed entirely of insulating material is being considered seriously. Present techniques in the use of glass fiber laminates make this attractive and apparently practical. Needless to say, the resin should have nontracking and fire-resistant properties. Attention should be given to the location and shielding of any metallic attachment fittings so as to prevent accidental contact between the bus and those details which are at ground potential. A minor weak point in this arrangement is the need for control circuit ground connections to the coils of the generator contactors located inside the bus compartment. It would seem desirable from a safety standpoint to leave the contactor frames ungrounded and to complete the control ground circuits through number 20 gauge wire leads which can burn clear in the event of an internal breakdown in the contactor.

The construction of an equally safe 3-phase bus compartment presents more difficulty because of the dual danger of phase-to-phase and phase-to-ground faults and because of the unavoidable crossovers resulting from connections to the bus. The use of solid noncombustible insulating barriers between phases and at crossovers is good practice. The crossover problem becomes simpler if the three phases are stacked or staggered in depth.

Mechanical Protection

For protection against missile damage some use has been made of armor plating the bus compartments, although the constant struggle with weight problems on airplanes makes this extremely unattractive if it is made reasonably effective. In some cases it has been possible to obtain a certain degree of flack protection by suitable location with respect to heavy primary structure such as wing spar caps. There is then the probability that a direct explosive hit which would destroy the bus will do enough structural damage that the bus failure will be incidental.

The Sectionalized Bus

If we assume that a means is available for isolating a faulted bus section, it goes without saying that the bus must be arranged in two or more sections in order to obtain any benefit in continuity of service. In a commercial airplane the sections may be all in one compartment with suitable barriers. In a combat airplane, however, it is important to have the sections in separate compartments, well separated to minimize the chance of simultaneous damage. So as to avoid excessive exposure, the bus tie conductors should be run in spaced multiple with fusible limiter protection at each end of each conductor. Additional security is obtained if the bus sections are connected in a ring configuration so that the isolation of a central section will not cause the loss of parallel operation. The ring arrangement makes better use of the remaining generating capacity to blow the limiters to a faulted section and may also justify the use of lighter individual bus tie lines and tie limiters, the latter making easier the matter of clearing a faulted section. Vital loads on an airplane usually are duplicated, in which case they may be distributed over two or more bus sections.

Sectionalizing Through Fusible Limiters

For automatic isolation of a faulted bus section the fusible limiters in the tie lines function reasonably well under generating and load conditions which result in high fault current. This means having all or nearly all generators in operation, well loaded, and preferably with shunt motors on the line to contribute fault current. In the case of a d-c system a more favorable condition is to have the generators operating at speeds somewhat

Figure 2. Typical 28-volt wing-to-wing bus

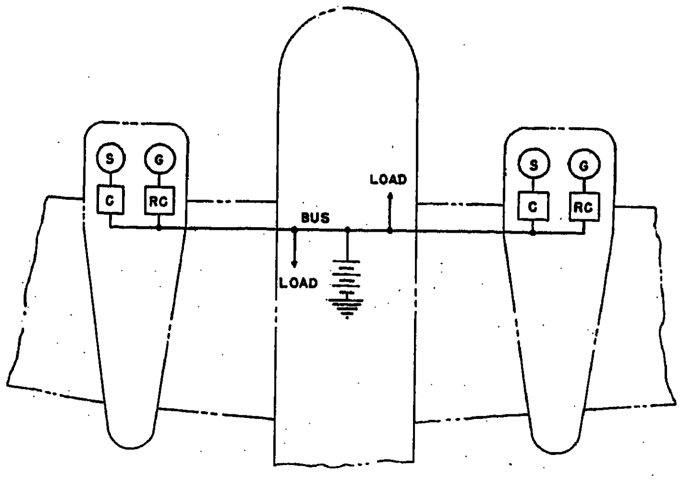
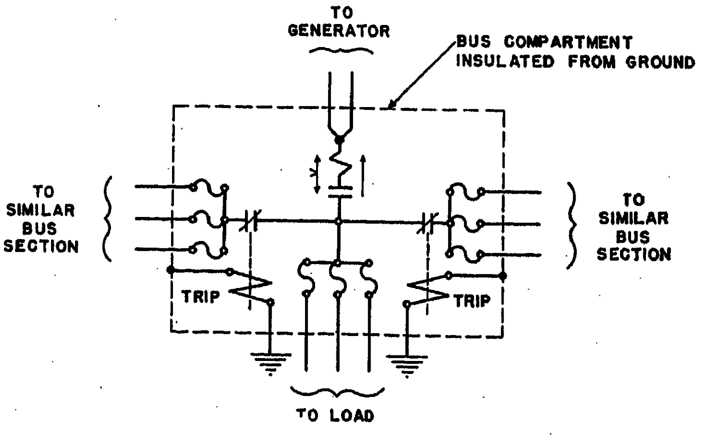


Figure 3. D-c bus section protected by ground-current tripping, with reverse-current and current-balance protection on generator feeder



above the minimum. As the system load is reduced or generators are lost from service the short circuit current capacity goes down and limiter blowing time lengthens until eventually it may become excessive. Because of the wide range of possible operating speed, which is a desirable feature of d-c systems, exciters have not been practical and therefore such systems are at present universally self-excited. This limits their sustained fault current capacity. In the 400-cycle system the alternators have individual exciters and, under a solid fault to the main bus, can deliver an ultimate sustained fault current which is appreciably higher than the transient current. On systems having a high percentage of motor loads, nevertheless, one cannot depend on the ultimate fault current for blowing limiters without running into the danger of essentially stalling all motors because of the long time involved. This may leave the system with a heavier starting load than it can pick up, and it may remain stalled until manual supervision removes the major motor loads. Calculations backed up by some tests on the 400-cycle system indicate that a clearing time longer than about 0.1 second may lead to this condition.

Ground-Current Relaying

Because of the imperfect performance of limiters for main bus protection, other faster and more selective means are needed for combat airplane systems. An inherently instantaneous method which has been proposed[11] is shown in Figure 3. Here the metallic bus compartment is insulated from the airplane structure except for a path through the low-impedance trip coils of the circuit breakers. The trip coils must be designed to operate even though they are partially short circuited by a low-impedance fault between box and ground. It is also to be assumed that battle damage to the box has not ruptured the trip coil circuit, damaged the circuit breakers, or faulted the tie lines where they leave the box. Since the latter condition is actually quite likely to happen, fusible limiters in each end of each tie line are still necessary. Another disadvantage is the fact that this method precludes the use of a compartment made of insulating material for the purpose of preventing accidental rather than combat faults, unless a heavier composite construction is used. Because of the difficulty of sensing phase-to-phase faults, and the problems in designing a 400-cycle trip magnet, it would seem that this method is best suited for d-c systems. As far as the author is aware, circuit breakers for this protection method are not yet available for airplane use.

Differential-Current Protection

Although differential-current bus fault protection is used widely in industrial practice, its use on airplane 400-cycle systems, especially under combat conditions, does not appear at all attractive. The difficulty arises principally from the large number of points at which current must be summed, particularly when line-to-line as well as line-to-ground faults are to be detected. Although 400-cycle current transformers are individually small and relatively light, the large number which is required enlarges the bus compartment so much that vulnerability is greatly increased. A broken control wire or a grazing hit on one transformer is sufficient to trip the bus off the line although the bus itself may be undamaged. While use of this method only for ground fault protection will reduce the number of transformers by two-thirds, when using through-type transformers, the lower order of protection obtained offsets the slight improvement in vulnerability. Also the reduction in number of transformers is obtained at the expense of bunching all conductors of all phases at the transformer, and this is a questionable practice. The need for bus-tie circuit breakers in either of the foregoing methods adds to the bus compartment size and vulnerability.

For the d-c systems it is conceivable that an arrangement of directional overcurrent relays might be devised to detect a bus fault and clear it by means of bus-tie circuit breakers. In some respects the problem appears simpler than that for the a-c case because only ground faults are possible. Further study along this line, with attention to nuisance-trip and vulnerability factors, is suggested.

Conclusions

Protection of the airplane electric system is receiving much attention in the industry at the present time because of the rapid growth of generating capacity in recent years and the greater dependence of airplane safety and performance upon the system reliability. While methods of generator circuit protection are fairly well in hand, the main bus, and to a lesser extent the distribution circuits, are inadequately protected because fully effective methods are not yet available. Although improved bus structure design and installation, such as the use of nonmetallic bus compartments, may afford sufficient protection for noncombat airplanes, more-positive and faster-acting protection is urgently needed in combat systems. At the present time the safest bus is the ring type with dispersed bus sections interconnected by multiple-conductor limiter-protected bus tie lines. The clearing time for a bus fault in this system is dangerously long after partial loss of generating capacity and may result in system collapse because of stalled motor loads. Further work on simple low-vulnerability bus protection methods is needed and there is a possibility that some form of directional over-current relaying would be suitable.

References

1. Control and Protection of Aircraft D-C Power Systems, **B. O. Austin.** *Electrical Engineering (AIEE Transactions)*, volume 65, November 1946, pages 741–45.

2. D-C Aircraft Generator Power Center, **E. L. Bottemiller.** *AIEE Transactions*, volume 66, 1947, pages 1267–70.

3. An Aircraft Differential-Voltage Cutout, **O. C. Walley.** *Electrical Engineering (AIEE Transactions)*, volume 63, September 1944, pages 632–34.

4. A Current-Limiting Fuse for Aircraft Applications, **A. H. Powell.** *Electrical Engineering (AIEE Transactions)*, volume 64, July 1945, pages 541–45.

5. Generator Fault Protection, United States Patent 2,426,005.

6. Paralleling and Regulation of 24–28-Volt D-C Generators in Multiengine Aircraft, **Arthur Siegal, Dennis G. DeCourcey.** *Electrical Engineering (AIEE Transactions)*, volume 63, November 1944, pages 854–57.

7. Parallel Operation of Airplane Alternators, **D. W. Exner.** *Electrical Engineering (AIEE Transactions)*, volume 62, December 1943, pages 755–60, Discussion, page 993.

8. Basic Considerations in Selection of Electric Systems for Large Aircraft, **W. K. Boice, L. G. Levoy, Jr.** *Electrical Engineering (AIEE Transactions)*, volume 63, June 1944, pages 279–87.

9. D-C Power Systems for Aircraft, Part V, **R. H. Kaufmann, H. J. Finison.** *General Electric Review* (Schenectady, N. Y.), May 1946, pages 46–54.

10. D-C Power Systems for Aircraft, Part VI, **R. H. Kaufmann, H. J. Finison.** *General Electric Review* (Schenectady, N. Y.), June 1946, pages 46–52.

11. D-C Power Systems for Aircraft, Part VII, **R. H. Kaufmann, H. J. Finison.** *General Electric Review* (Schenectady, N. Y.), August 1946, pages 41–48.

No Discussion

Aircraft Carbon-Pile Voltage Regulators, Fundamentals, and Design Improvements

B. O. AUSTIN
MEMBER AIEE

H. H. C. RICHARDS
NONMEMBER AIEE

IT HAS been known for many years that mixes of carbon can be processed into plates of various shapes, hardnesses, finishes, and thicknesses. When these plates are stacked and pressure applied at right angles to their surfaces, the resistance of the stack varies with the pressure change. Figure 1 is a typical curve of pressure plotted against resistance for such a carbon stack. This curve shows that the resistance decreases with an increase of pressure. The design of the carbon-pile voltage regulator is based on this fundamental characteristic of carbon surfaces in series under a controlled variable pressure.

D-c generator field strength is one of the determining factors for output voltage. Field strength may be controlled by varying the current with variable series resistance. Stacked carbon plates provide such a resistor when the applied pressure is properly controlled. Many designers in the past several decades have conceived a large variety of electromechanical mechanisms for controlling the pressure on carbon piles. These mechanisms have consisted usually of levers, cams, pivots, dash-pots, springs, rollers, magnets, and electrical dampers co-ordinated so as to produce the desired results.

Generally, the main design objective for carbon-pile voltage regulators is to provide an electromagnet with the necessary mechanisms arranged so that the pull curve of the magnet matches the force curve of the spring throughout the operating travel range of the magnet armature to properly control the pressure on the carbon stack. See Figure 2 for a typical matching curve. This accomplished, the device becomes a servomechanism for automatically regulating the resistance in series with the field winding of the generator to hold constant voltage output when the regulator operating coil senses generator voltage.

Carbon-pile voltage regulators designed for aircraft use appeared in quantity on military aircraft early in World War II. These regulators were similar to the English design by Newton which were of the direct-acting type with many of the mechanical arrangements common to the carbon-pile voltage regulators eliminated. Due to their mechanical ruggedness and ability to withstand momentary overload, they have been more successful than other types of regulators used previously. Even with the difficulties which have been experienced with them, they are still in wide use.

Many of the users of aircraft carbon-pile voltage regulators have suggested that the regulators be improved. Operators want longer life, easier maintenance, better temperature compensation, no creeping, no fluttering, no over-voltage due to stack wear, and minimum hysteresis and friction, both of which affect voltage regulation. Better provisions for withstanding environmental conditions such as high and low ambient temperature, vibration, acceleration, altitude, sand, salt spray, and humidity are further refinements desired.

In further consideration of the improvements in the design of the aircraft carbon-pile voltage regulator, it was found that it is much easier to build satisfactory regulators in model shops than to produce satisfactory regulators of the same design in the production line unless the design is adapted to easy accurate methods in obtaining close tolerances and perfect alignments of vital parts.

Performance Objectives

With the performance data from actual service at hand and with a wealth of information from production experience, it was clear that a complete study of all factors affecting the performance and production of aircraft carbon-pile voltage regulators was long overdue. The first requirement of any voltage regulator is accurate regulation. Regulator performance, as outlined in the *AN-R-la* Specification issued by the United States War and Navy Departments, is required from minimum generator speed to maximum generator speed with generator load varying suddenly or gradually over the load range. The load may be suddenly removed or applied at any time. This operating load condition may repeat at low or high ambient temperatures or under altitude conditions. Regulator

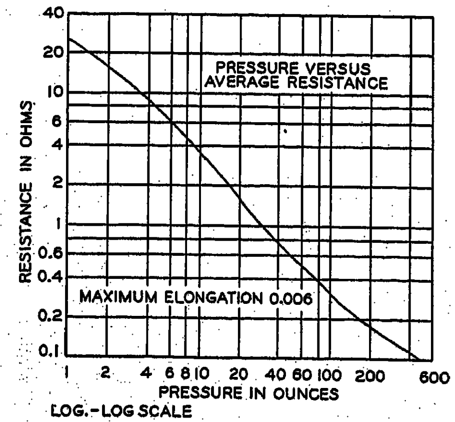


Figure 1. Pressure resistance curve of a typical carbon stack used in aircraft voltage regulators

---

Paper 48-214, recommended by the AIEE Air Transportation Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Middle Eastern District Meeting, Washington, D. C., October 5–7, 1948, and at the Pacific General Meeting, San Francisco, Calif., August 23–26, 1949. Manuscript submitted July 21, 1948; made available for printing August 25, 1948.

B. O. AUSTIN is Section Manager and H. H. C. RICHARDS is Electrical Engineer, both with the Aviation Engineering Department of the Westinghouse Electric Corporation, Lima, Ohio.

The authors wish to acknowledge the assistance of H. D. Else, J. D. Miner, and O. C. Walley of Westinghouse Electric Corporation.

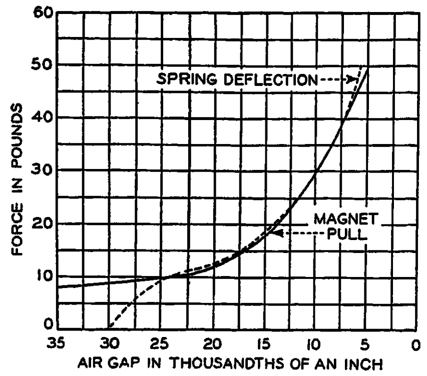


Figure 2. Curve showing matching between spring force and magnet force in the aircraft voltage regulator

performance must be such that minimum stack resistance will permit satisfactory generator output at low speeds. On the other hand, the maximum high resistance values at no-load and maximum speed must be accurate and stable under conditions of vibration. It is further required that the voltage controlled by the carbon-pile voltage regulator during load changes between rated load and no-load shall recover in the order of 0.1 second or less. Regulators are sometimes subjected to full field current through the stack and must carry this overload current for a long period without damage to any operating parts. Carbon-pile voltage regulators in operation on airplanes are subjected to periods of continuous acceleration. This is a severe operating condition which must be met without loss of regulation at high generator speed and high stack resistance, and the regulator must remain stable. Paralleling equalizer coils must be provided in the regulator so that satisfactory load division will be maintained by the generators. See Figure 3 for the diagram of paralleled regulators and generators.

Total temperature produced by watts loss in a carbon-pile voltage regulator determines the watts rating at which the regulator can be operated without unusual wear or damage affecting voltage regulation. Therefore, the design must be such that the regulator will dissipate the rated watts without overheating any of its parts. Endurance is closely allied with temperature, stability, time of recovery, pressure range on the stack, and the magnet air gap. Therefore, it is required that these factors (as well as the dielectric strength of insulating materials which are also affected by temperature) be properly considered in the design. The regulator also must have the ability to withstand sand, dust, salt air or spray, high or low humidity, and resistance to fungus growth.

Design Procedure and Results

There are many factors governing the carbon stack design which must be considered and accounted for if long life and good performance are to be expected. The operating temperature of carbon is limited by disintegration at the surface of the plates and therefore watts loss of these surfaces must be within the temperature limits of the carbon surfaces, which is in the order of 400 degrees centigrade. Resistance per inch of length of the carbon plates or disks must be controlled so as to limit the watts input. Therefore, the total length of the stack is determined largely by how fast the carbons can be cooled and the maximum temperatures that carbon can stand without disintegration. It is not advisable to blow air directly on the carbon stack as the oxygen present accelerates combustion at the hot spots.

Carbon plates in stacks do not make contact uniformly throughout the area of the plates. This is due to very minute high spots, even with the best grinding and polishing methods. Heat is concentrated at the points where high spots first make contact. This may result in glowing or overheating of the carbon at the localized points if the volts and amperes per plate are too high. This is controlled by providing the proper number of plates per inch of stack length. Therefore the permissible watts for each contact surface are a determining factor in the number of disks per inch. There seems to be no known means of cooling these hot spots fast enough to prevent disintegration of the carbon and to permit a reduction in the number of carbon plates and the lengths of stack in a given application.

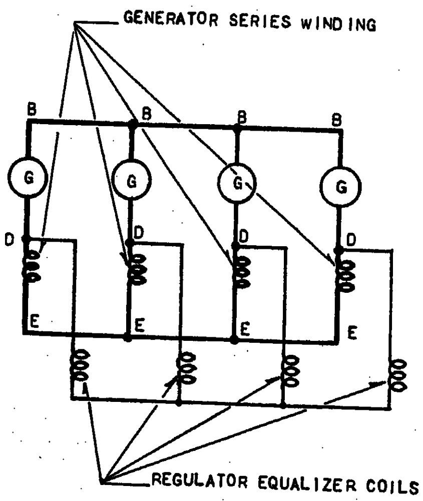


Figure 3. Diagram of equalizer circuits for equalizing generator loads

Figure 4. Carbon stack assembled in a pyrex tube and the electrodes for the aircraft carbon-pile voltage regulator described

Two other important factors entering into the stack design are the minimum and maximum resistance to be obtained under operating conditions and the available forces in the magnet and spring. A large number of disks per inch aid in obtaining maximum resistance, reduce arcing between plates and provide stability. On the other hand, a small number of disks aid in obtaining the minimum resistance. The design must be compromised between these two factors and the hardness of the carbon.

Stack sizes or diameters should be kept small provided they can be cooled satisfactorily. This is to minimize the weight of the stack which aids in the solution of vibration and manufacturing problems. The smaller diameter disks are easier to grind flat and they remain flat after heating in the regulator. Large rigid disks are hard to grind and warp easily with temperature. This can seriously affect the performance of the regulator. Very thin disks may be acceptable when they can be produced with sufficient strength. In this case, the surfaces of the carbon disk would adjust themselves to one another by bending. Figure 4 shows the stack used in the improved regulator.

The method of cooling and the necessity for enclosing the carbon stack make it necessary to support the stack with a tube made of such material as Pyrex glass or ceramic. Both of these materials are satisfactory; the ceramic material being a little more difficult to grind and polish to eliminate friction.

Probably the most important structure in the carbon-pile voltage regulator is the armature and its associated parts. The vital parts of this armature consist of an iron disk, an abutment on which the spring rests, a damper, a spring, a spring clamp, insulation, electrode, and hardware.

For the purpose of obtaining quick response and eliminating difficulties due to vibration, the component parts of the armature assembly which move are kept as light as possible. A flat star-shaped spring is used whose ends rest on an outer abutment ring. The ring surface is shaped so that as the spring travels, the forces required to make it travel are nonlinear and match the pull curve of the magnet. For maintaining accuracy of the assembly, this special design uses a star-shaped spring which is flat and is not formed before being included in the armature assembly. During the assembly of the armature an inner and an outer cone-shaped coined washer clamps the spring and forms it to the desired angle. This eliminates the manufacturing difficulties of accurately forming springs. In this same assembly there is a thin, soft, copper diaphragm whose purpose is to air and spring damp the action of the star-shaped spring and control the wear curve of the regulator, minimizing armature flutter and over-voltage due to carbon stack wear.

The problem of excessive heat transfer from the electrode and carbon stack to the star-shaped spring was solved in the regulator shown by using a ceramic bushing support for the electrode which is clamped into the armature assembly. The ceramic was selected for its high heat insulating characteristics. The electrode has a shunt which is attached to the electrode by tamping. The shunt passes through a slot in the side of the ceramic bushing. The electrode can be removed from the ceramic bushing in about the same manner a carbon brush is removed from a generator. This provides an armature which is very stable and is not subject to change or shrinkage such as that experienced with other types of armatures. See Figure 5 illustrating the component parts of the armature assembly.

Magnetic forces to operate the armature are rather simple and are subject to standard calculations. The pull curve is matched to the armature spring as nearly as it is practical to do with such a design. Structural studies of the magnetic circuit were made and the circuit arranged so as to provide the maximum ease in the accuracy of assembly during production and maintenance operations. Hysteresis in the magnetic field is minimized by thoroughly hydrogen annealing all iron in the magnetic circuit. In addition a high grade iron is used to eliminate as many impurities as possible.

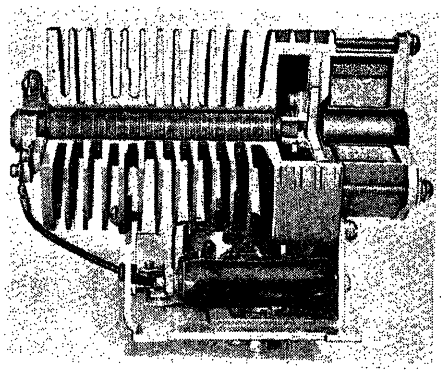


Figure 6. Cut-away view of the aircraft carbon-pile voltage regulator

The stationary electrode is the same as the moving electrode except that it does not have a shunt and is pressed into a metal threaded plug. It is removable and replaceable. Removing the stationary electrode plug permits the removal of the carbon stack without further disassembly. This plug is clamped in place by the support casting being split and closed with a clamp screw.

Heat dissipation and thermal expansion are controlled by the amount of radiating surface and the characteristics of the materials used. The carbon stack which is to be cooled is enclosed in a Pyrex tube which is not a good heat conductor, but is acceptable from this standpoint and is used. This material and a ceramic are about the only ones that will stand the heat shocks and provide the necessary insulation. The stack diameter, the inside and the outside diameter of the Pyrex glass tube are controlled as closely as it is practical to avoid blankets of air which also are very poor conductors of heat. Tolerances between the glass tube and the metal tube of the housing are closely controlled for the same reason.

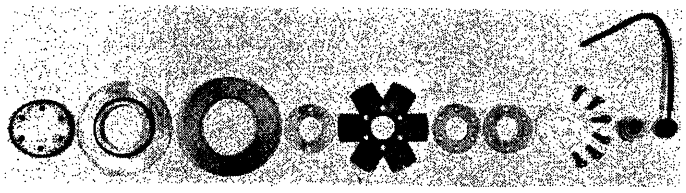
Heat is finally dissipated by means of cast aluminum fins on an invar steel tube. The number and area of these fins depend on the watts to be dissipated and the maximum allowable total temperature rise of the carbons. An aluminum cast housing for cooling and supporting the structure has been a problem due to the high temperature coefficient of the expansion of aluminum. The problem was solved by casting the aluminum fins separately on an invar tube which has a very low temperature coefficient of expansion. In this manner, the individual aluminum fins can expand, but do not affect the over-all expansion of the housing. Temperature rise of the invar tube is about 135 degrees centigrade for 90 watts dissipation. See Figure 6 for a cut-away view showing this construction.

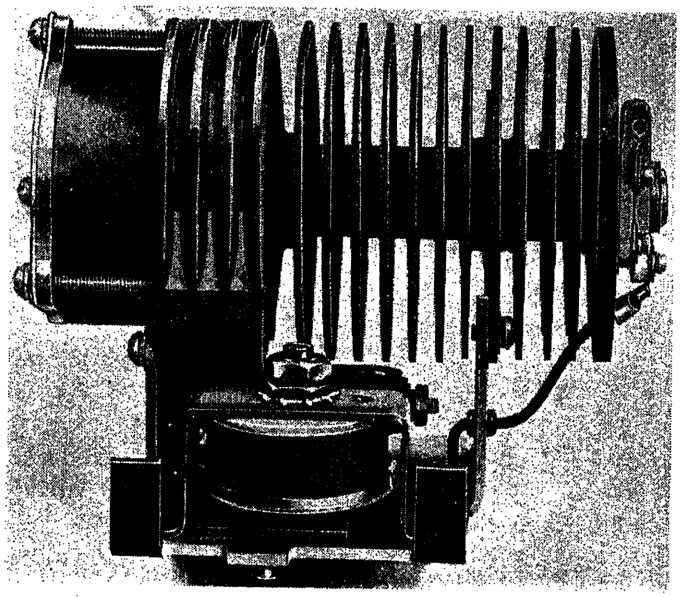
Carbon stacks and electrodes have not as yet been designed so that they will have an indefinite life. They are an item of wear and must be replaced. The regulator structural design was studied thoroughly and arranged so that any of these parts could be removed and replaced within ten minutes. At the same time, the design also was arranged so that when it was disassembled or reassembled there would be no errors of misalignment which could cause poor voltage regulation due to increased friction of parts being pushed sidewise. See Figure 6.

External to the body of the regulator is a 1-piece mounting bracket or base on which are mounted three resistors and the necessary terminals. The variable re-

Figure 5 (left). Exploded armature in line of assembly, left to right (1) armature, (2) copper damper, (3) abutment ring, (4) coned washer, (5) star-shaped spring, (6) concave washer, (7) spacer, (8) clamping washer, (9) hardware, (10) ceramic bushing electrode support, (11) graphite electrode and shunt

Figure 7 (right). A 90-watt aircraft carbon-pile voltage regulator





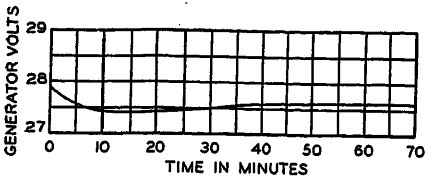


Figure 8. Voltage regulation curve during a 70-minute warmup period from a 25 degree centigrade ambient

sistor is in series with the operating coil of the regulator. The purpose of this is to set the regulator voltage within the established limits. It was felt by the designers that an improvement could be made by providing a locking nut for this adjustment, rather than by using a detent or ratchet mechanism to hold the adjustment in place. The locking nut can not be changed so easily by unauthorized persons and it has, therefore, been provided. The fixed resistor is in series with the operating coil and is for temperature compensation, which will be discussed later. The fixed stabilizing resistor is connected so as to form a feedback to the operating coil, thereby stabilizing the action of the coil in the magnet circuit of the regulator. See Figure 11.

Aircraft carbon-pile voltage regulators are subjected to a very high range of temperatures from ambient sources as well as warm-up due to heat generated within them. For this reason, temperature compensation of the regulator was thoroughly studied and means were provided for correction. Several variations of materials such as steel whose magnetic characteristics change with temperature were tested and evaluated. The design was finally co-ordinated with materials used in the construction of the regulator. With such an arrangement, the adding of details for temperature compensation was unnecessary. This arrangement involved the proper location of invar alloy and the aluminum support for the magnet assembly. (See Figure 6.) Also note that tie rods formerly used have been eliminated. A fixed resistor mounted on the base is connected in series with the winding of the operating coil. It has a very small resistance change with temperature and is used to swamp out a large portion of the total resistance change of the operating coil. The ratio of the resistance of the resistor to that of the coil is about 4.4 to 1. Therefore, the effect of coil temperature change on voltage regulation is reduced by the amount of this ratio.

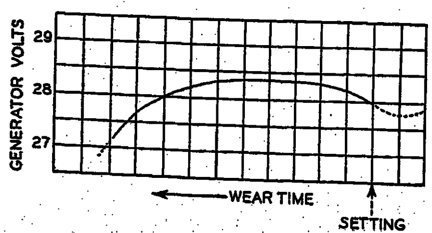


Figure 9. Curve showing voltage regulation due to stack wear. Wear time depends on regulator application

It has been pointed out previously in this paper that it was necessary to minimize the weight and anchor the moving parts of the armature. This was done and proved to be very helpful in the solution of the vibration and centrifugal force problems. The carbon stack also is a free member and is subject to this same service condition. Likewise, its weight, as well as the number of disks in the stack, is important. A small number of disks require less pressure and separate more readily under vibration. Therefore, the number of disks to be used is a compromise between arcing conditions at the disk surfaces and arcing due to vibration.

Endurance or life of an aircraft carbon-pile voltage regulator depends on many factors, some of which have been discussed previously in this paper. Carbon stacks must be operated at or below the recommended temperatures to prevent disintegration. Flutter or unstable action of the armature must be damped out or otherwise excessive arcing and burning will take place at the electrodes and between the disks. Effects of vibration, acceleration, and centrifugal forces on these moving parts must be minimized. Grade or hardness of carbon disks used must be carefully selected and co-ordinated with many other factors in the stack design. Operating stack pressures must be sufficient to prevent arcing under maximum resistance conditions.

There are several environmental conditions, with which many are familiar, under which aircraft carbon-pile voltage regulators must be operated satisfactorily. These are sand, dust, salt air, humidity, and fungus growth. After a thorough study, arrangements were made for the effective sealing of the vital parts against these hazards. Wherever possible, pressure is used at the open joints and high temperature soft synthetic seals are used at cable entrances. Considerable improvement has been made at the armature assembly enclosure where exclusion of sand, dust, and other foreign materials is highly important. Materials subject to fungus are treated with the best known preventatives. See Figure 6 for the assembly seals.

Test Results on 90-Watt 28-Volt Regulator

1. Voltage regulation at 3,500 rpm of a 200-ampere United States Air Force type *P-1* generator varied from 0 to 0.3 volt for three observations, zero load to full load.

2. Voltage regulation at 4,500 rpm of a 200-ampere type *P-1* generator varied from 0.2 to 0.3 volt for three observations, zero load to full load.

3. Voltage regulation at 3,500 rpm of a 200-ampere type *P-1* generator varied from 0.05 to 0.4 volt for three observations, zero load to full load at −55 degrees centigrade ambient.

4. Voltage regulation at 4,500 rpm of a 200-ampere type *P-1* generator varied from 0.1 to 0.4 volt for three observations of zero load to full load at −55 degrees centigrade ambient.

5. Voltage regulation at 3,500 rpm of a 200-ampere type *P-1* generator varied from 0.1 to 0.3 volt for three observations of zero load to full load at +72 degrees centigrade ambient.

6. Voltage regulation at 4,500 rpm of a 200-ampere type *P-1* generator varied from 0.2 to 0.3 volt for three observations of zero load to full load at 72 degrees centigrade ambient. (See Figure 8).

7. Dielectric strength was satisfactory at 500 volts for one minute from live parts to base.

8. With 0.25 volt applied to the equalizer coil, the regulator voltage was lowered 2.35 to 2.50 volts on ten regulators.

9. Minimum resistance varied from 1.17 to 1.22 ohms on the ten regulators tested as per Specification *AN-R-1a*. Resistance of 1.25 ohms or less is acceptable.

10. Maximum resistance of 35 ohms under vibration conditions without producing arcing in the stack is shown by curve in Figure 11.

11. Maximum recovery time tested 0.06

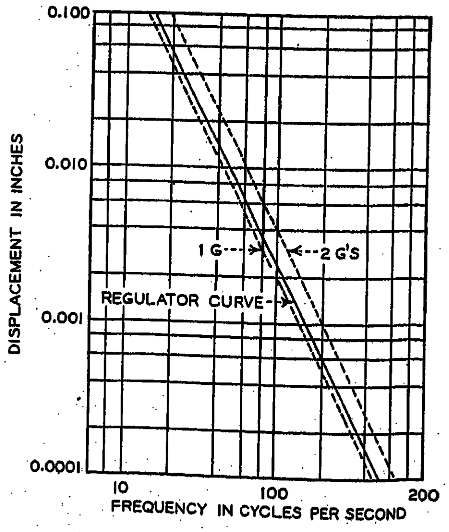


Figure 10. Regulator stack arcing elimination test between one and two gravitational units under vibration conditions. Taken on regulator described

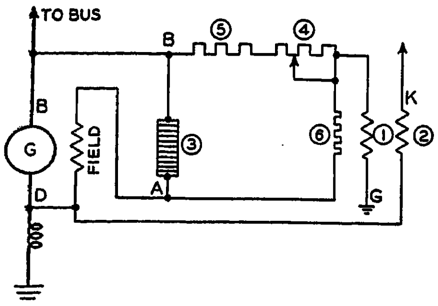


Figure 11. Schematic diagram of connections for the 90-watt aircraft voltage regulator

second. Recovery time of 0.10 second is acceptable.

12. Voltage regulation at 4,500 rpm of a 300-ampere *P-1* generator, 72 degrees centigrade ambient, after 25 hours varied from 0.1 to 0.3 for three observations zero to full load.

13. Carbon stack closed with no voltage on the operating coil. Regulator withstood 15 amperes through the carbon stack for one hour without damage.

14. Maximum increase in ripple, voltage tested to be 0.35 volts, while 0.6 volt is acceptable.

15. Regulator was placed in a centrifuge under the most unfavorable conditions (coil end toward center, stack radial); satisfactory operation was observed up to 6 G's (acceleration due to gravity). 5 G's acceptable to Specification *AN-R-1a*.

16. Regulator was vibrated for two hours in each of three directions making a total of six hours without shock mounts; 10 to 55 cycles at 0.06 total excursion. No damage resulted.

17. At the end of a 200-hour accelerated endurance test the voltage dropped 0.7. The voltage did not change more than 0.3 during any 25-hour period.

18. Regulator was subjected to salt spray for 25 hours, passing regulation requirements afterwards.

19. Regulator was subjected to sand resistance tests for eight hours. At the end of the test, voltage had changed 0.1.

Conclusions

Objectives accomplished in this program of designing and improved aircraft carbon-pile regulator resulted in the following:

1. Better voltage regulation.
2. Lower operating temperatures.
3. Improved acceleration and vibration characteristics which affect life and regulation.
4. Longer life.
5. Sealed protection against salt spray, sand, and other foreign objects.
6. Good temperature compensation without added details.
7. Friction and creep reduced very substantially.
8. Better performance under high and low ambient temperature conditions.
9. Wearing parts made readily accessible and replaceable.
10. Reduction in the number of parts.
11. Disassembly and reassembly without change in alignment.
12. Improved armature construction.
13. Dissipates a maximum of 90 watts.
14. Weight increased about two ounces over regulators of 75-watt rating.
15. Design well adapted to production and maintenance of alignment of parts.

References

1. CARBON-PILE VOLTAGE REGULATORS FOR AIRCRAFT, W. G. Neild. *Electrical Engineering (AIEE Transactions)*, volume 63, November 1944, pages 839–43.

2. THE APPLICATION OF VOLTAGE REGULATORS TO AIRCRAFT GENERATORS, L. W. Thompson, F. E. Crever. *AIEE Transactions*, volume 61, 1942, pages 363–65.

3. PARALLELING AND REGULATION OF 24–28-VOLT D-C GENERATORS IN MULTIENGINE AIRCRAFT, Arthur Siegal, Dennis G. DeCourcey. *Electrical Engineering (AIEE Transactions)*, volume 63, November 1944, pages 854–56.

No Discussion

Current Ratings of Aircraft Cable

WADE H. ASKEW
ASSOCIATE AIEE

A. T. McCLINTON
MEMBEE AIEE

Synopsis: This paper presents the results of an investigation conducted to determine the continuous and short-time current ratings of copper aircraft cable, using visual indication of smoke as the limiting factor. Tests were made on several wire sizes at various ambient temperatures, using six types of aircraft cable. Curves were obtained showing the highest current which can be carried continuously by each size of cable at different ambient temperatures without evolvement of smoke. Thermal curves, with smoking and delayed smoking values, were obtained for various current overload applications. Procedure is given for interpolating smoking and delayed smoking curves for wire sizes not experimentally obtained. Results of this investigation indicate that present continuous current ratings are too high for most of the cable used during this investigation.

MUCH of the electric equipment in aircraft is used, or draws its greatest current, for short periods of time. When voltage drop is not a restricting item, considerable saving in weight and material can be realized by the use of small size cables to carry, for these short periods, currents much higher than the continuous ratings. If the wiring system is designed on the basis of cables being subjected to overload currents, it is necessary that protective devices be installed to make certain that the interval of application shall not exceed the safe time limit. Proper co-ordination between protective equipment and short-time ratings is necessary to assure that the cable will not be subjected to an excessive overload-time combination.

Tests conducted previously by other laboratories indicate that, for short-time periods, a conductor temperature sufficient to produce light smoke will cause no measurable change in the insulating or physical properties of the cable. However, any trace of such smoke in a plane is psychologically dangerous; therefore, the wiring must be protected from possible smoke evolvement.

Definitions

Certain terms used throughout this report are defined as follows:

Maximum Continuous Current. The highest current which can be carried for an unlimited time by a specified cable at given ambient conditions without producing visible evidence of smoke.

Overload. Any value greater than the maximum continuous current.

Smoking Time. The time required to produce visible evidence of smoke when an overload current is carried by a cable.

Delayed Smoking Time. The longest interval of time an overload current can be carried by a cable without visible evidence of smoke occurring after the current is interrupted.

Methods of Conducting the Investigation

Tests were conducted on single cables in still air, at approximately sea level altitude, in ambient temperatures of 0, 25, and 45 degrees centigrade. Cable sizes *AN* numbers 0, 8, 16, and 20 from various manufacturers were used for the investigation. Data for other ambient temperatures and cable sizes were interpolated from the results experimentally obtained.

The conductor temperature was ascertained at any time during test runs by calculating the change in resistance as indicated by the change in voltage drop across a section of the cable. A submarine battery was used as the source of power; the current was held constant by variable resistors, paralleled as necessary for the various currents used. Tests proved that when only light traces of smoke evolved, the same cable could be used several times without noticeable change in any of the measured results. When the sample was used more than once, subsequent tests were made only after the cable had cooled to room ambient temperature.

Since the appearance of smoke determined the length of time the cable could carry a specified current, it was necessary to have an accurate method of detecting the smoke. Preliminary test runs showed that the best results were obtained in a partially darkened room, using a small desk-type fluorescent lamp secured about three inches above the cable. A piece of photographer's cloth was hung about three feet behind the light, and a small strip of this cloth was suspended just below the light, about two inches in front of the cable. It was found that accumulation of smoke in the lamp shade was particularly helpful in observations where the time at which smoke appeared was not an essential factor, that is, observations for determining continuous current

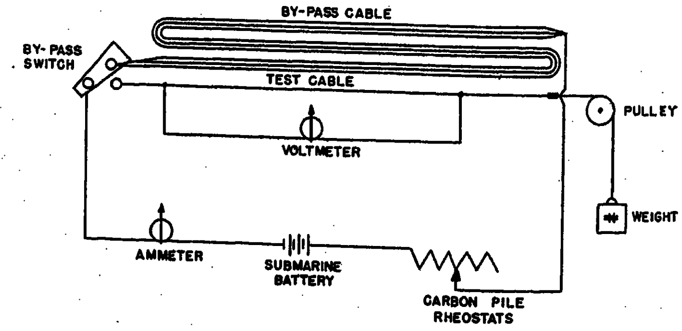


Figure 1. Schematic diagram of test circuit

Paper 49-202, recommended by the AIEE Air Transportation Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Pacific General Meeting, San Francisco, Calif., August 23-26, 1949. Manuscript submitted September 3, 1948; made available for printing June 28, 1949.

WADE H. ASKEW and A. T. McCLINTON are both with the Naval Research Laboratory, Washington, D. C.

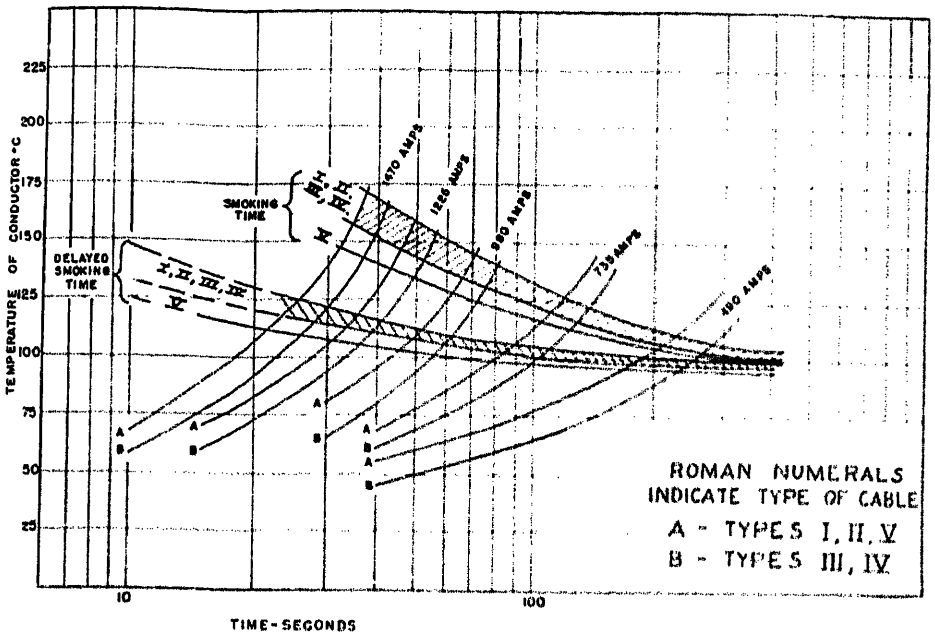


Figure 2. Temperature–time curves for cable size *AN* number 1/0. Ambient temperature 25 degrees centigrade

and delayed smoking values. The best position for observation was found to be 12 to 24 inches directly in front of the light.

Consideration was given to the possibility that different individuals might not detect smoke at the same time under similar conditions. However, data taken from observations made by six persons during a minimum of 20 test runs showed that except for the first five or six observations, the difference is observed time for the various individuals was well within the experimental error.

Figure 1 shows a schematic diagram of the test circuit. The by-pass circuit consisted of three parallel cables, each of the same size as the cable under investigation, but three times its length. This circuit was used for the initial setting of the variable resistors. Carbon piles were used for the variable resistors since their change in resistance partially compensated for the change in resistance of the cable, thus facilitating the maintenance of a constant current. Weights of 20 pounds for size number 0; 5 pounds for size number 8; and one pound for size number 16 and number 20 cable were hung over a pulley with a cord connecting the weight to the cable. Any slack caused by elongation due to temperature rise was taken up by the weight, and the tension was kept constant. These weights were not sufficient to cause stretching of the cable.

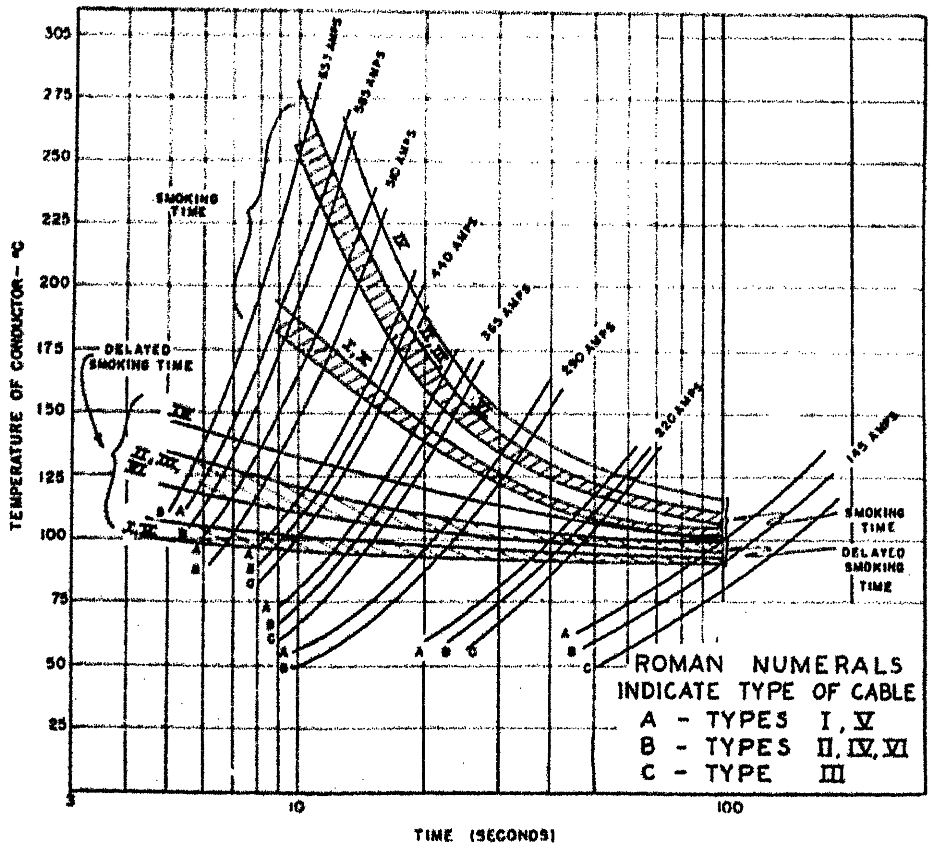
Discussion of Results

Figures 2, 3, and 4 show conductor temperature versus time curves for overloads applied to the various types and sizes of cable. The delayed smoking time values shown on these and other curves establish the overload-time values for the cables; but for comparative information, the smoking time values also are shown. It will be noticed that the grouping of thermal, smoking time, and delayed smoking time curves by types is not consistent for the different sizes of cable used during the investigation. This is probably due to different graduated scales used by the manufacturers to determine the relative amounts of primary insulation and braid components for the various sizes of cable. For example, it was found that the weight ratio of primary insulation and/or braid to the total weight of the cable might be nearly equal on one size for cable from four of the manufacturers, whereas when the same type of comparison was made using another size, this weight ratio would be about the same for possibly only two companies; and for a third size, these ratios would be different for all of the manufacturers.

Several tests were made to determine the effect of increase in insulation thickness (outside diameter), the results of which are shown in Figure 5. No attempt was made to determine the thickness of any material added or removed, but attention is directed to the fact that every material added decreased the operating temperature of the conductor, and that the type of outer surface apparently had little effect. Also, this indicates that the heat dissipated from the cable by conduction and convection is large compared to that dissipated by radiation.

In addition to the data mentioned previously on short-time temperature characteristics, data were obtained to show the

Figure 3. Temperature–time curves for cable size *AN* number 8. Ambient temperature 25 degrees centigrade



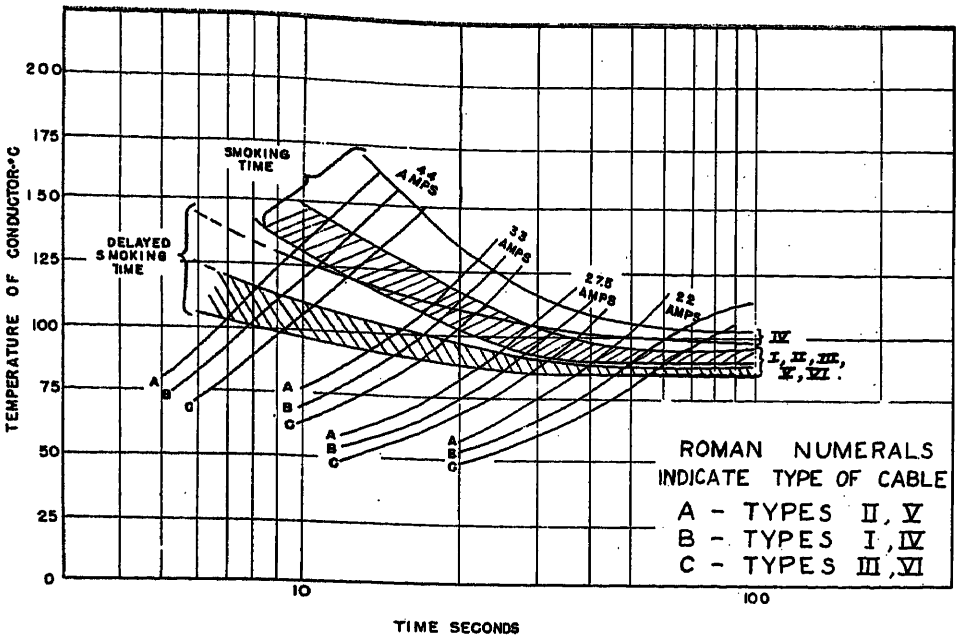


Figure 4 (top). Temperature–time curves for cable size *AN* number 20. Ambient temperature 25 degrees centigrade

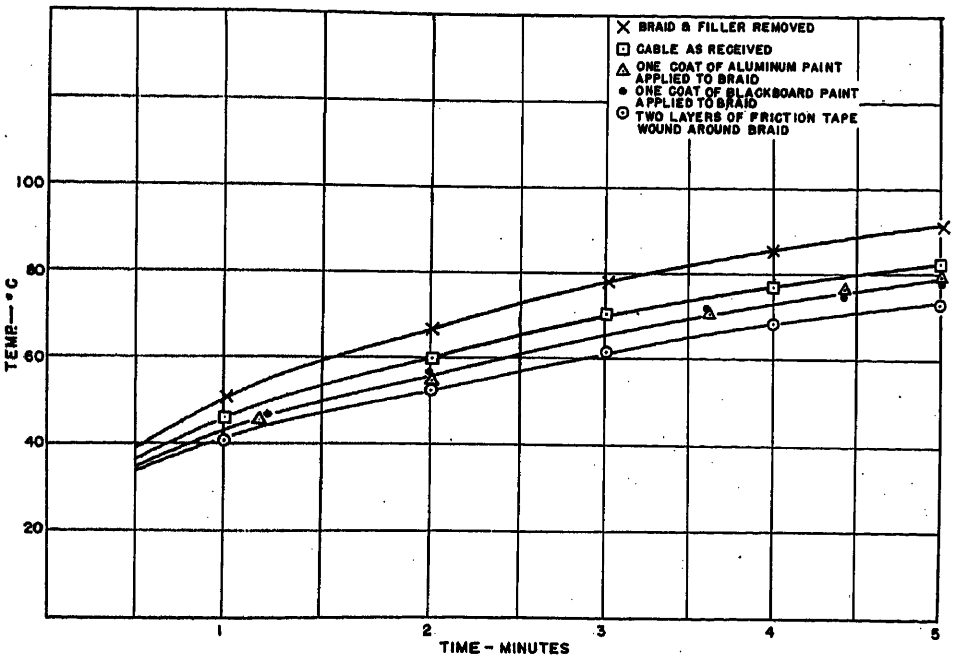


Figure 5 (middle). Effect of insulation thickness on temperature–time curves

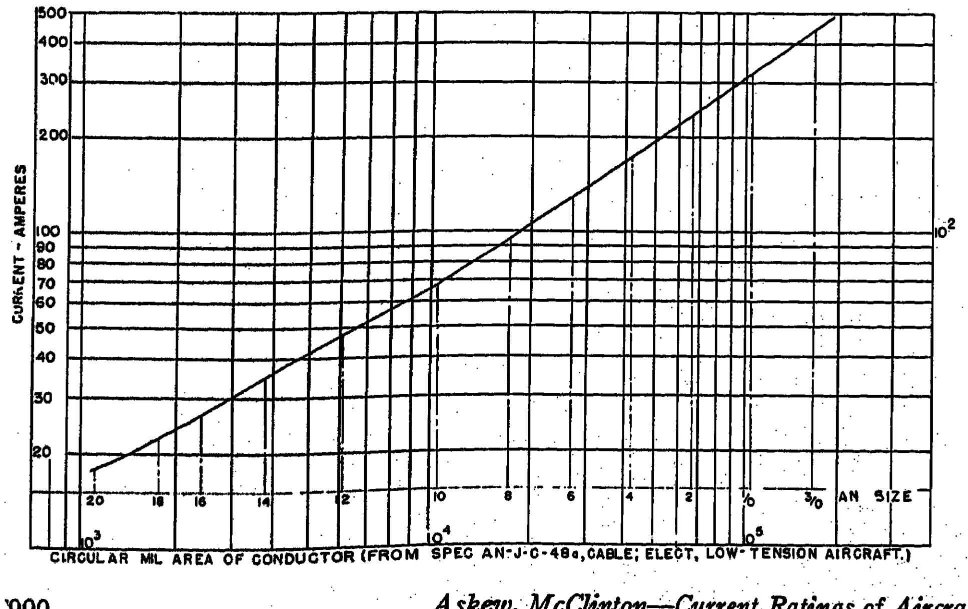


Figure 6 (bottom). Recommended continuous current ratings for aircraft cable in ambient temperature of 25 degrees centigrade

maximum continuous current that the cables could carry without evolvement of smoke. The results of this investigation are presented in Table I and in Figure 6.

Table I

Cable Type	AN Size Number	Current, Amperes	Temperature* of Conductor When Stabilized, Degrees Centigrade
1	8	92	91.5
3	0	305	89.8
3	8	102.5–107.5	95.4–104.2
3	20	19	93.0
4	0	330	101.4
4	8	102.5–107.5	99.8–107.1
4	20	20	105.2
5	8	92.5–95.0	90.4–92.7
5	20	17	84.5

* Ambient temperature—25 degrees centigrade.

The values shown in Table I are the highest currents that the respective cables could sustain without causing the insulation to smoke while operating in an ambient temperature of 25 degrees centigrade. Those of Figure 6 are the average continuous current values for the six types of cable investigated. It should be noted that most of the conductor temperatures in Table I are lower than the 100 degrees centigrade generally accepted as being satisfactory.

The effect of ambient temperature on short-time and continuous ratings of the various cables also was investigated. Figure 7 shows the thermal curves with smoking time and delayed smoking time values obtained on one type of cable at various ambient temperatures. The average maximum continuous current ratings are shown in Figure 8. It should be noted that the current rating curves will all approach zero current at 95 degrees centigrade.

These tests were extended to show the effects of ambient temperature on the short-time ratings of the various types of

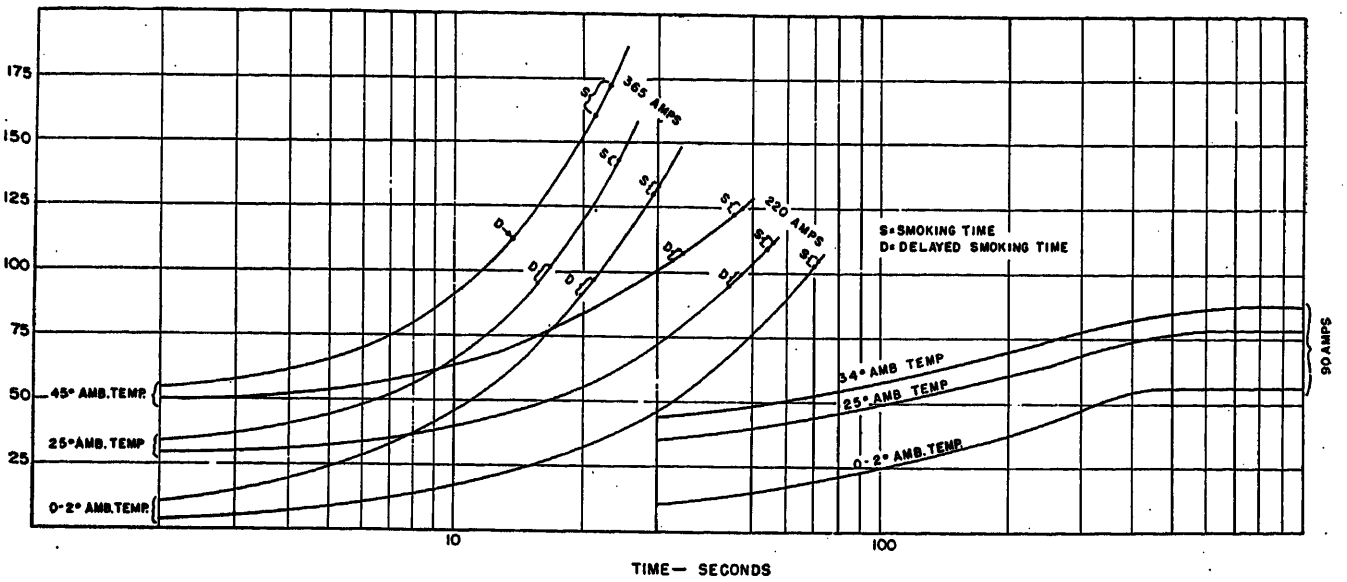


Figure 7 (top). Effect of ambient temperature on temperature–time curves of one type of cable. *AN* size number 8

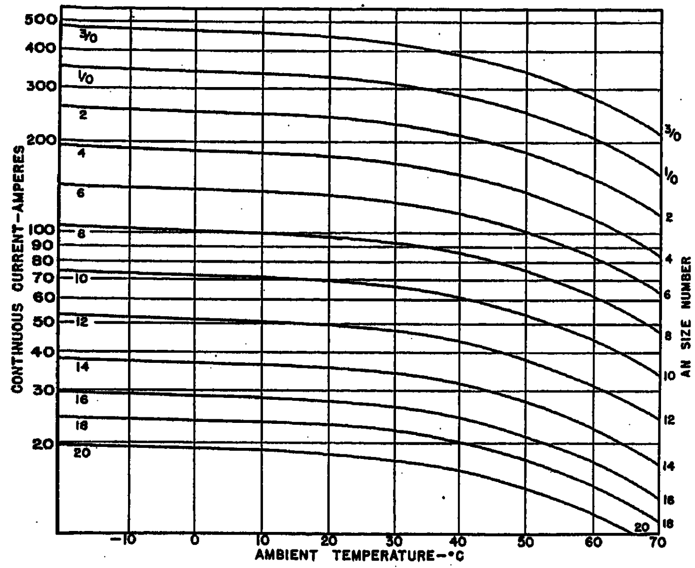


Figure 8 (left). Effect of ambient temperature on continuous current ratings of aircraft cable

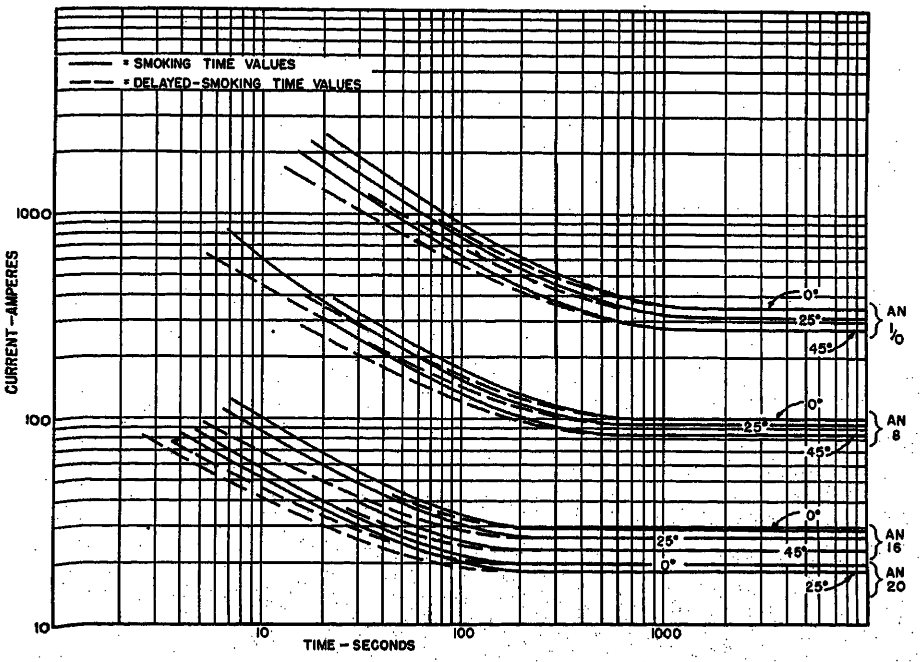


Figure 9 (below). Effect of ambient temperature on the short-time current ratings of aircraft cable

cables. Figure 9 presents the average results of smoking-time and delayed smoking-time as a function of current for various ambient temperatures. Data were not taken for periods of time less than five seconds because of limitations of accuracy in control and measurement.

Using the data of Figures 8 and 9 a set of curves was prepared showing the maximum safe current that a conductor could carry for a given period of time when in an ambient temperature of 25 degrees centigrade. This is shown in Figure 10. The shape of the curves for *AN* sizes numbers 0, 8, 16, and 20 for times between 2,000 seconds and five seconds is based on experimental data. Below one second the curves are based on calculation of the heat energy required on a no-loss basis to raise the temperature of the wire size in question to a value of 150 degrees centigrade. A conductor temperature of 150 degrees centigrade was used as the maximum safe temperature for the shorter time periods. Each curve, from 2,000 seconds to five seconds, is extrapolated from five seconds to one second to join the calculated curve at this point. The curves for other wire sizes were drawn with the same shape as that for *AN* sizes numbers 0, 8, 16, and 20; and asymptotic to the lines representing maximum continuous currents for the respective cable sizes and ambient temperature as found on Figure 8.

It is important that attention be directed to the fact that the curves of Figures 6, 8, 9, and 10 are values averaged from six types of *AN* aircraft cable used for this investigation. These values do not exceed by more than six per cent those obtained for the cables most susceptible to smoking. Therefore, the general application of these curves to all existing

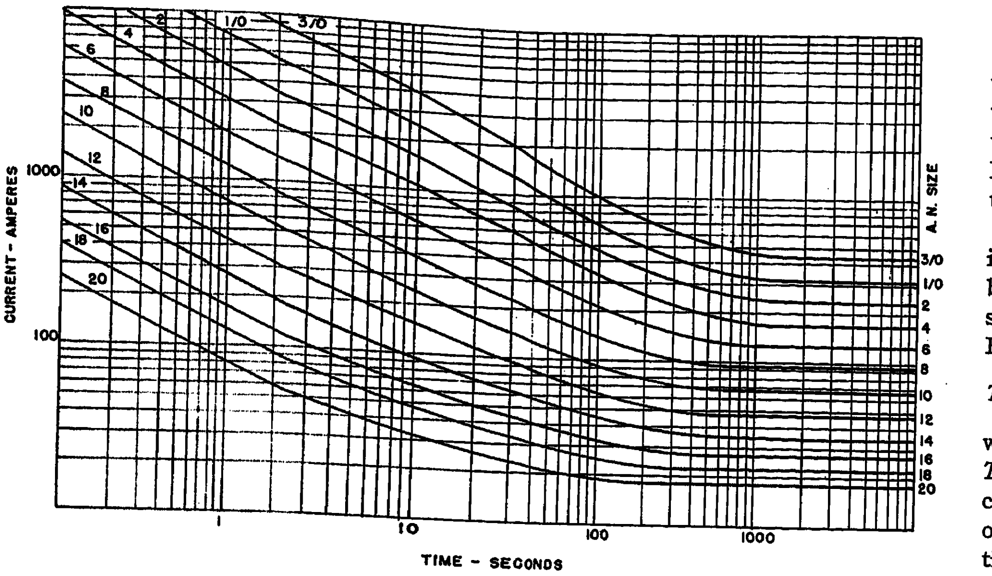


Figure 10 (top). Recommended short-time current ratings in 25 degrees centigrade ambient temperature for aircraft cable

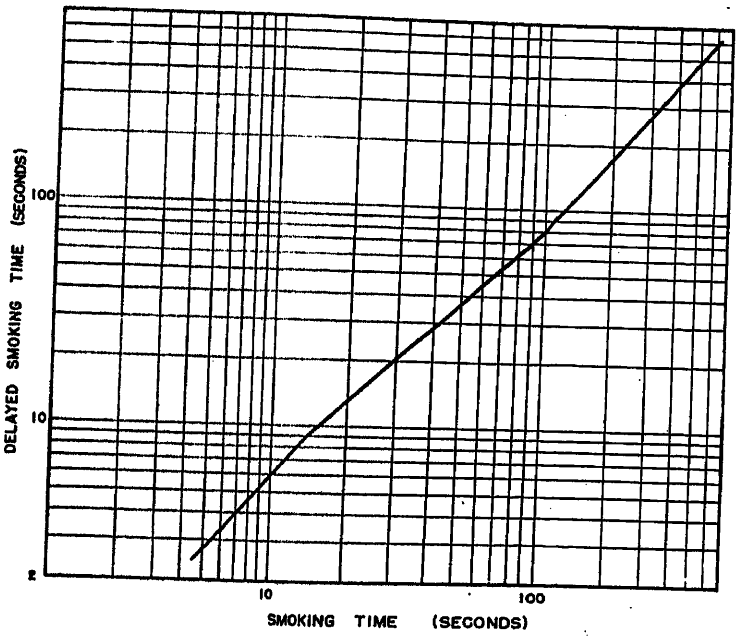


Figure 11 (left). Relationship between smoking and delayed-smoking time values

types of *AN* cable should not be made without an allowance for this six per cent variation. However, it is recommended that consideration be given to the use of Figure 10 as a standard for the manufacture of aircraft cable.

During the course of this investigation, it was noted that a relationship existed between the delayed smoking time and smoking time data. This is shown in Figure 11, or by the equation

$$T_d = 0.46 T_s^{1.12}$$

where T_d is the delayed smoking time and T_s the smoking time. Data for this curve were obtained from the numerous observations made during the investigation. However, this curve is satisfactory for intervals up to five minutes; for longer periods of application the continuous current rating should be used. Although the relationship was not used during this investigation, it may prove useful in any future work of a similar nature.

Conclusions

The proper application of the curves of Figure 10 to the selection of protective devices and cables for both intermittent and continuous duty loads should result in safer operation of aircraft electric systems. The application of these data will in some instances permit the use of smaller size conductors, whereas in other instances the application will indicate the need for larger conductors than would result from the use of less adequate information. However, in either case, the engineer can be certain that the cable will be adequate to carry the current.

No Discussion

Some Oscillographic Methods of Investigating Mechanical and Hydraulic Conditions During Operation of Hydroelectric Units

LENARD R. ENGVALL
ASSOCIATE AIEE

THIS paper outlines some of the methods by which a magnetic oscillograph, with auxiliary equipment, has been used at the Grand Coulee Power Plant, Columbia Basin Project, to obtain and co-ordinate information about hydroelectric units which can not be obtained from ordinary indicating and recording instruments. Quantities measured and recorded include mechanical movement, hydraulic pressures, bearing loads, and turbine vibration and noise.

A special form of impedance bridge was built to measure mechanical movement and hydraulic pressure. Well-known commercial apparatus, plus electronic amplifiers, were adapted to register the other values. A number of oscillograms illustrates how a simultaneous record of several related quantites can facilitate analysis of a particular problem. It is not intended to imply that a new process has been developed, but rather to show how valuable information can be obtained with the help of comparatively simple equipment.

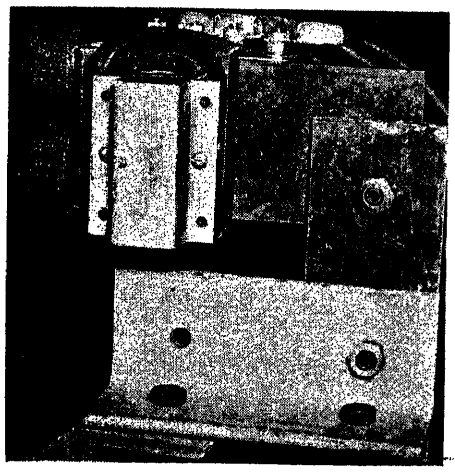


Figure 1. Impedance bridge pick-up

Measurement of Mechanical Movement

While attempting to adjust a Kingsbury thrust bearing, following some trouble which had subjected the long connecting shaft between turbine and generator to severe torque stresses, it became evident that it would be advantageous to measure the shaft run-out at several points, and to record these measurements against a common time base. The obvious instrument for the job was the magnetic oscillograph. From the several possibilities for a pick-up, that is, the device which would transform mechanical movement into electrical quantities, the impedance bridge was chosen as the most suitable because it was simple and rugged, and could be made sufficiently sensitive without electronic amplification. Also important was the fact that no mechanical connection to the moving member was needed. Figure 1 shows the appearance and physical arrangements of the pick-up, which is shown in position for use in measuring movement of 44-inch diameter steel shaft. Coils are wound on laminated core of one square inch cross section. Two coils on each core are connected electrically in diagonally opposite sides of bridge circuit.

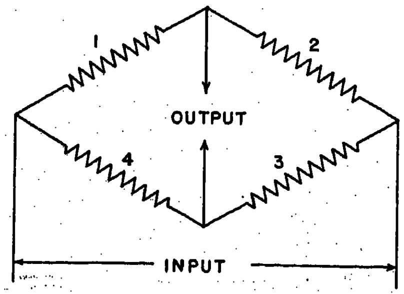


Figure 2. Diagram of impedance bridge connections

The particular form used consists of four identical coils connected in a bridge circuit and placed in pairs on two iron cores. One core is held firmly on a minutely adjustable support a little less than one-eighth inch from the moving member of which the record is to be taken, so that that member completes the magnetic circuit and varies its reluctance. The other, for convenience, is mounted on the same frame, but its magnetic circuit is entirely independent, and includes a fixed and approximately equal

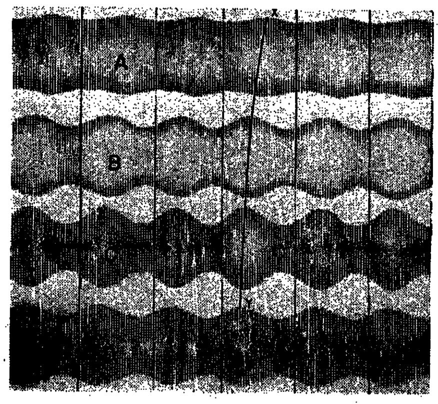


Figure 3. Typical record of large vertical shaft

Paper 49-204, recommended by the AIEE Instruments and Measurements and Power Generation Committees and approved by the AIEE Technical Program Committee for presentation at the AIEE Pacific General Meeting, San Francisco, Calif., August 23–26, 1949. Manuscript submitted May 9, 1949; made available for printing July 14, 1949.

LENARD R. ENGVALL is with the United States Department of the Interior, Coulee Dam, Wash.

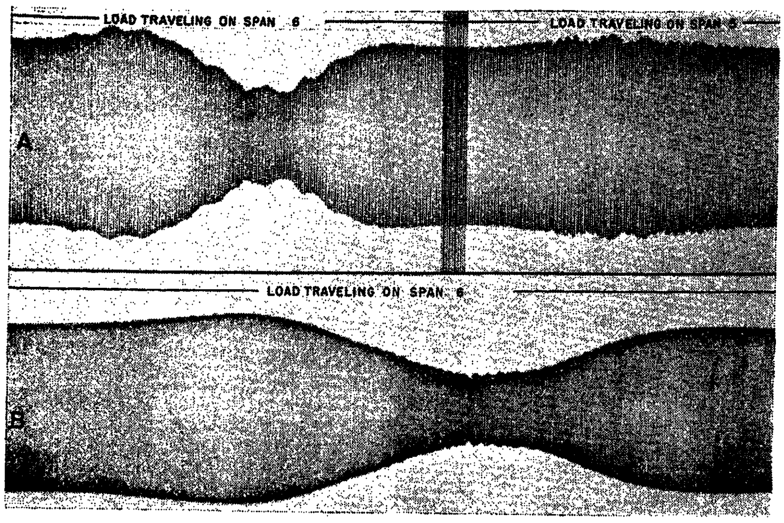


Figure 4. Records of deflection of center of 150-foot span on multiple-arch reinforced-concrete bridge while 35-ton load is passing over

Curve A was taken with load traveling 25 miles per hour. Maximum deflection downward is 0.060 inch; upward 0.017 inch. Curve B was taken with load traveling 5 miles per hour. Maximum deflection downward is 0.069 inch, upward 0.012 inch. Maximum design loading is 40 tons, concentrated. Pick-up located at center of span 6

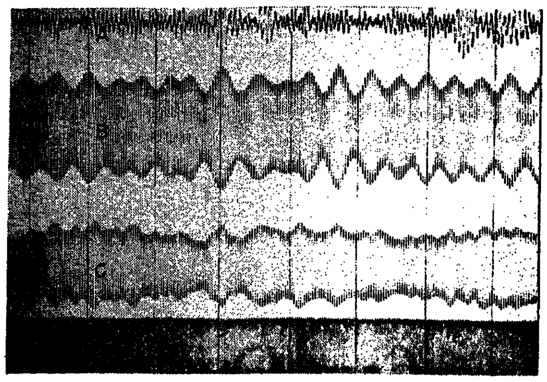


Figure 6. Record of hydraulic conditions in relation to shaft run-out on 150,000-horsepower turbine, 0.40 gate, 50,000-kw load

Maximum pressure change in scroll case, trace B, is 16 pounds per square inch; in draft tube, trace D, 7 pounds. Shaft run-out, trace C, at approximate middle is 0.007 inch

air gap. Electrical connections are shown in Figure 2, showing coils 1 and 3 mounted on one laminated iron core, coils 2 and 4 on the other.

A low, constant, 60-cycle a-c potential is applied to one pair of terminals of the bridge and an oscillograph galvanometer connected to the "neutral," or output, terminals. Actually, the device functions because it is always unbalanced, the adjustment of the variable air gap being made either greater or lesser than the fixed air gap so that the neutral point is not crossed. The galvanometer then indicates the amount of unbalance, and the peak-to-peak deflection of the trace on the record is a measure of the movement being studied.

It should be kept in mind that in the oscillograms shown in this paper which were obtained by the use of an impedance bridge, only a change in envelope width indicates a change in the quantity measured. This relation is calibrated. The actual width of the envelope has little significance. While the relation between movement and deflection is not exactly linear, it is sufficiently close to it so that the error can be disregarded for most purposes. A change of one inch in the width of the envelope on the original record indicates a movement of approximately 0.015 inch.

Figure 3 shows a typical record of a shaft in normal operation on 108,000-kw machine turning 120 rpm. The distance between top and bottom pickups is about 62 feet. Trace A shows a horizontal movement of 0.003 inch at a point just below the thrust bearing, that is, a run-out of 0.0015 inch either way from the average position. Trace B is taken at a point just below the rotor and shows a run-out of 0.004 inch; trace C is taken at the intermediate coupling, and trace D just above the turbine bearing, indicating run-outs of 0.0055 and 0.0035 inch respectively.

Figure 5. Record of hydraulic conditions in relation to generator output on 150,000-horsepower turbine, .046 gate, 60,000-kw load

Maximum pressure change in scroll case, trace B, is 15.5 pounds per square inch; in draft tube, trace C, about 10 pounds per square inch. Variation in output, trace D, is about 2 per cent

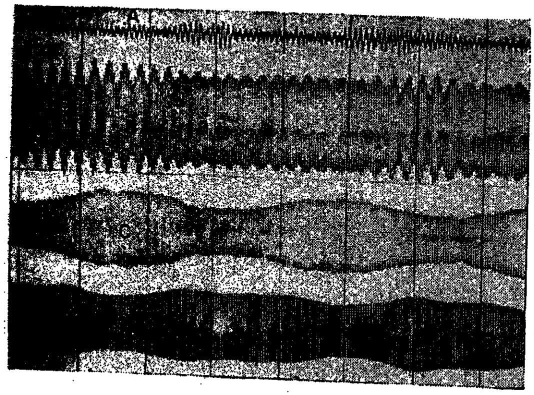
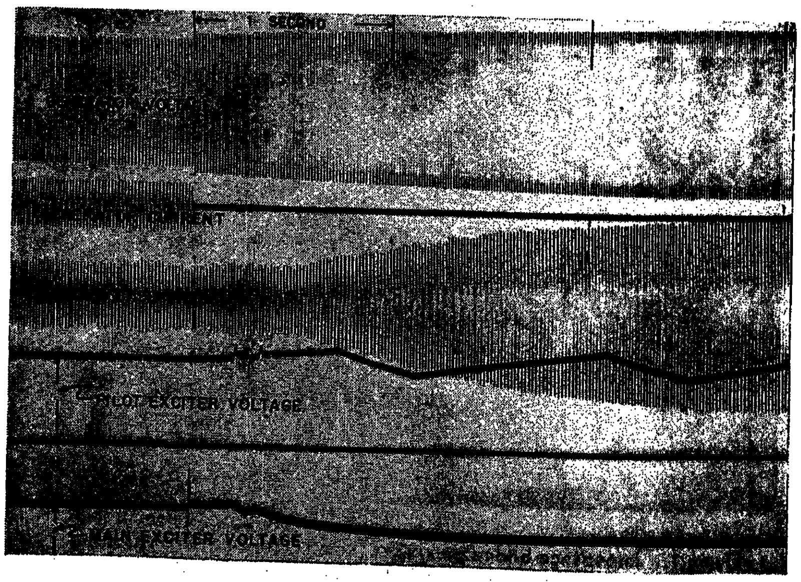


Figure 7. Record of pressure build-up in scroll case of 150,000-horsepower turbine in relation to generator electrical quantities during load rejection test

Pressure increased 38 pounds per square inch. Penstock is 18 feet in diameter and about 340 feet long



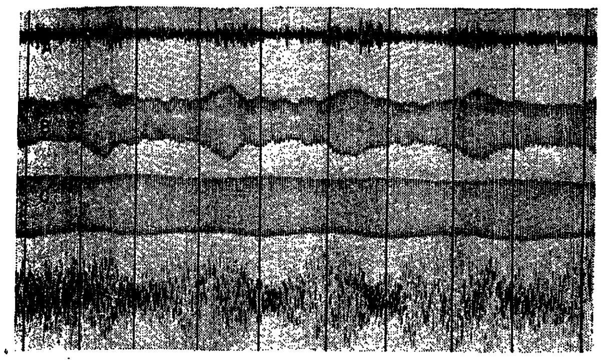


Figure 8. Record of noise and vibration associated with pressure changes in hydraulic turbine

One might logically expect the whole shaft to move in the same direction at the same time. This it does not do, however, as may be seen from the curve X-Y, drawn through the maximum point in each trace, which indicates a phase difference of about 120 degrees between the top and bottom. Similar records have been of assistance in balancing the rotors on these slow speed machines.

Another interesting application is the use of this apparatus to check the deflection and vibration of one span of a multiple-arch reinforced-concrete bridge under a rapidly moving load. Figure 4 shows the deflection at the center of the span as a 35-ton load passed over at speeds of 25 and 5 miles per hour, respectively. Maximum deflection from no load was 0.068 inch. Attempts had been made to obtain the same information by reading indicating instruments, but the changes were too rapid to obtain satisfactory results.

Measurement of Hydraulic Pressure

Since there has been reason for considerable curiosity about the performance of the turbines at the Grand Coulee Power Plant, the next logical step was the investigation of hydraulic pressures under various conditions of operation. The impedance bridge also lent itself admirably to this application. To the end of a Bourdon tube, taken from an ordinary pressure gauge, is attached a small piece of sheet steel which becomes the moving member of the bridge. This type of pick-up is somewhat limited in frequency response, the particular ones constructed having a natural frequency of a little over 20 cycles per second. Where much higher frequency measurements are necessary, a diaphragm type of gauge probably would give better results. Such pick-ups connected to show the scroll case and draft tube pressures gave a good picture of how the two varied at different loads, and to what extent disturbances on one side of the turbine runner appeared on the other side. In Figure 5, trace A shows the crown plate vibration, trace B scroll case pressure, trace C draft tube pressure, and trace D generator output. A swing of about two per cent can be detected in the output, but it does not appear to be synchronized with the hydraulic variations.

At certain loads, interesting relations have been found between shaft run-out and draft tube pressures. An example of this appears in Figure 6, where traces C and D represent shaft run-out and draft tube pressure, respectively. It is evident that these pressure changes, which seem to have definite cycles of about three revolutions each, and may be the result of vortex movements, are powerful enough to pull the shaft an appreciable amount out of line.

Figure 7 shows a record of pressure build-up in the scroll case resulting from governor action during a load rejection test when 132 megawatts were dropped.

Measurement of Noise and Vibration

A device which has been adapted to yield additional information is the ordinary radio loud-speaker of the permanent-magnet field type. By weighting and cushion-mounting the frame so that its movement is negligible, and mechanically coupling the moving coil or cone to the member being studied, an alternating current is produced which is generally indicative of its movement. The output is usually sufficient to be easily recorded on a fairly sensitive galvanometer. Unfortunately, it is not readily calibrated, since the output voltage is dependent on frequency as well as amplitude. Nevertheless, it is valuable for comparison. Noise, transmitted by air, can be similarly recorded. An example of a record on which are shown these quantities is given in Figure 8. Trace A indicates vibration of the turbine cover plate, as picked up by the loud-speaker, traces B and C show pressures in the scroll case and draft tube respectively, and trace D represents noise coming from the draft tube.

Measurement of Bearing Loads

Another type of pick-up device has provided considerable information about bearings. On each jack pin supporting a segment of the Kingsbury thrust bearing on the main units at the Grand Coulee Power Plant, are mounted two Baldwin-Southwark type *SR-4* strain gauges. These are used in the initial installation to secure equal loading on all the bearing shoes. However their usefulness does not end there. By connecting each pair in the well-known Wheatstone bridge circuit, and applying an a-c potential, a small output is obtained across the midpoints whenever the bridge is adjusted a little off balance. This output must be amplified before the ordinary oscillograph can record it, and this was done by using a multiple channel electronic amplifier of conventional design. By means of a vacuum tube voltmeter, the over-all response was calibrated, and the change in bearing loading measured.

Examples of the type of records obtained are shown in Figures 9 and 10. It is evident from Figure 9, that in spite of the extreme care with which these bearing runners are finished, this one had two high spots, but it is otherwise typical of normal operation. Figure 10 shows the variation in total loading assumed to be caused by hydraulic disturbances. Measurements indicate a total load change of 192,000 pounds or 96,000 pounds more or

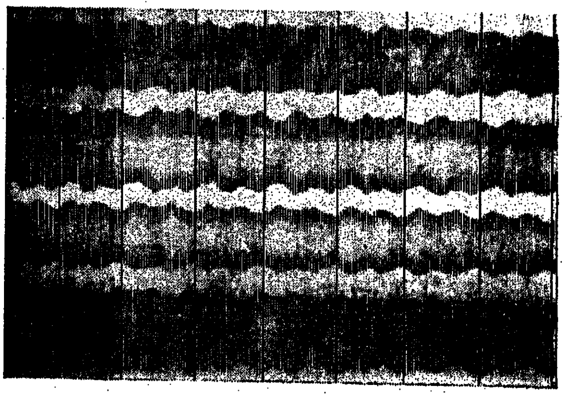


Figure 9. Record of load changes on alternate shoes of a Kingsbury thrust bearing, on a 108,000-kw generator turning 120 rpm, full load. Average load change per shoe is 7,500 pounds

less than the average. This amounts to six per cent of the 1,430,000 pounds rotating weight.

The apparatus also has been used for a determination of the hydraulic thrust, which for the particular unit on which these records were taken amounts to as much as 280,000 pounds upward, reducing the bearing load to 80 per cent of the maximum.

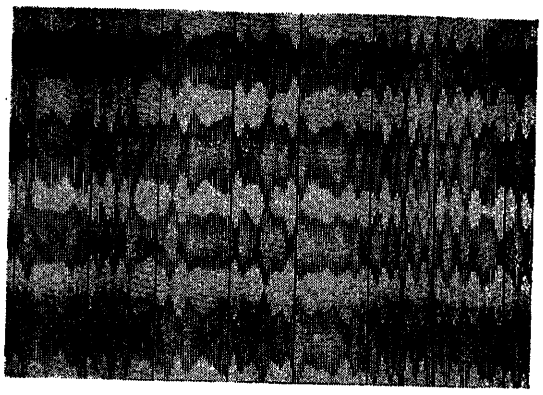


Figure 10 (right). Record of load changes on 4 of 8 shoes of Kingsbury thrust bearing, on a 108-megawatt generator, 25-megawatt load. Trace A indicates maximum load change of 19,000 pounds, trace B 27,000 pounds, traces C and D 25,000 pounds

Conclusion

The foregoing examples have been selected from the files of the United States Bureau of Reclamation, to show the variety of information which can be readily recorded and is likely to be of value at any hydroelectric station. Probably many instances will occur to operating and test engineers where similar data would be helpful. Since the oscillograph first came into general use, its value for testing electrical circuits on all kinds of power equipment has been recognized. The ease with which it can record mechanical conditions as well, such as shaft movements, bearing pressures, hydraulic or steam pressures, vibration, and many others, and the low cost and simplicity of the auxiliary apparatus required should make it of equal value to operating and maintenance personnel.

No Discussion

Deionization Time of High-Voltage Fault-Arc Paths

EVERETT J. HARRINGTON
ASSOCIATE AIEE

E. C. STARR
FELLOW AIEE

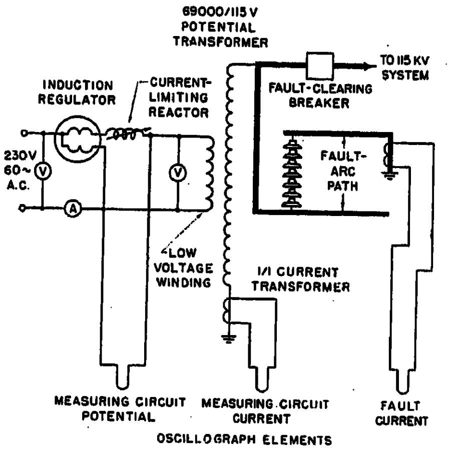
AUTOMATIC reclosing of circuit breakers in high-voltage transmission lines can materially increase system transient stability limits provided reclosure can be accomplished very rapidly. In case of temporary faults resulting from lightning, the minimum time in which this reclosure can be accomplished successfully is determined mainly by three factors:

1. Circuit breaker limitations.
2. Total duration of lightning stroke or its multiple components.
3. Deionization time of the fault-arc path and its consequent recovery of dielectric strength.

With regard to the first factor, modern circuit breakers have been developed to the point where minimum reclosing time is not expected to be limited by them. The second factor is one which may very well be the determining item. Inasmuch as this is a natural phenomenon, limitations which it may impose can be determined only by extensive experience with high-speed reclosing combined with field data on lightning-stroke duration. The rather meager data available at present on multiple lightning strokes to lines indicate that approximately 10 per cent persist for more than 0.25 second or 15 cycles (from initiation of fault) on a 60-cycle system.[1]

The third factor is one on which little data are available, particularly in regard to deionization time of fault-arc paths on 115- and 230-kv transmission lines. In order that reclosure be successful, dielectric recovery of the ionized fault-arc path must have proceeded to the point where re-application of system voltage will not produce sufficient current through the arc path to maintain ionization. If for a given recovery voltage and length of arc path the minimum current to maintain ionization is known, the minimum time in which successful reclosure can be accomplished can be determined provided the high-voltage resistance of the arc path in respect to time can be obtained.

Figure 1. Schematic diagram of 115-kv test circuit



A method has been developed for determining the resistance and dielectric recovery rate of the arc path after fault-current interruption. Field tests on a 115-kv system (at the J. D. Ross Substation of the Bonneville Power Administration) were performed, and data obtained on deionization of fault-arc paths produced by single-phase faults having equivalent 3-phase magnitudes from 750 to 3,000 megavolt-amperes. The effects of fault magnitude and duration are shown, and additional tests are proposed.

Test Circuit

In order to determine the dielectric strength of the fault-arc path during the period subsequent to fault-current interruption, it is essential that the high-voltage measuring circuit have negligible effect upon the resistance of the ionized path and its rate of dielectric recovery.

Figure 1 shows the circuit used in the 115-kv tests. A measuring potential was applied to the arc path from a high-impedance source consisting of a 69,000/-115-volt potential transformer whose low-voltage winding was energized from a 60-cycle a-c source through a current-limit-

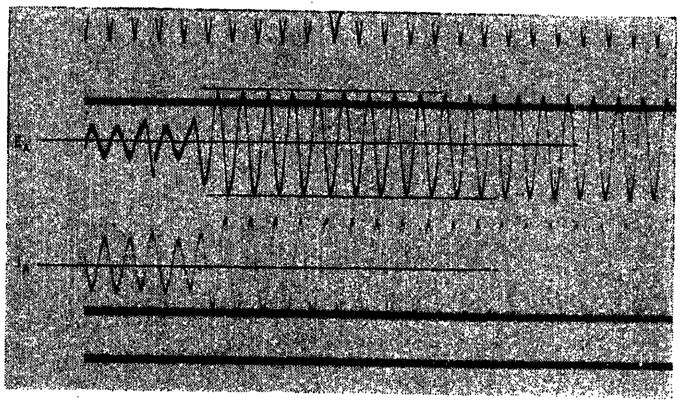


Figure 2. Recovery characteristic of test circuit only. Short circuit on secondary of measuring circuit cleared by opening circuit breaker B26
E_A = measuring circuit, primary voltage
I_A = measuring circuit, secondary current

Paper 49-205, recommended by the AIEE Instruments and Measurements, and Transmission and Distribution Committees and approved by the AIEE Technical Program Committee for presentation at the AIEE Pacific General Meeting, San Francisco, Calif., August 23–26, 1949. Manuscript submitted May 25, 1949; made available for printing June 29, 1949.

EVERETT J. HARRINGTON is with the United States Department of the Interior, Bonneville Power Administration, Portland, Oreg.; and E. C. STARR is Professor of Electrical Engineering at Oregon State College, Corvallis, Oreg.

The authors wish to acknowledge the assistance of George Anderson, Richard Dietrich, and the Pacific Electric Manufacturing Corporation in the preparation of this paper.

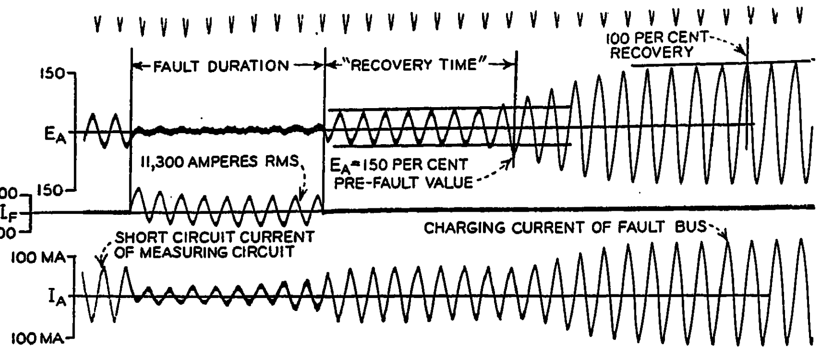


Figure 3. Typical recovery oscillogram 8.5-cycle fault; 11,300 amperes rms. Time, 10:20 p.m.; fault megavolt-amperes, 2,200; bus voltage, 110 kv; arc location, air

E_A=measuring circuit, primary voltage; I_F=fault current; I_A=measuring circuit, secondary current

ing reactor. This reactor was adjusted as follows: with the transformer high-voltage winding open circuited (test bus capacitive excitation load only), the induction regulator was adjusted to give 69 kv across the transformer high-voltage winding. This winding was then short circuited, and the reactor adjusted so as to limit the current in the winding to less than 100 milliamperes. This adjusting procedure was repeated until the desired open-circuit voltage and short-circuited current were obtained.

Establishment of the fault arc short circuits the transformer high-voltage winding, and the oscillograph element across the low-voltage winding indicates a potential essentially equal to the transformer and its supply lead impedance drop. This drop is small compared to that across the current-limiting reactor. After fault-current interruption, deionization of the arc path and consequent increase in its resistance progressively limits the transformer current and the voltage across its terminals rises, reaching normal value when conduction through the arc path ceases. This voltage is then a measure of arc-path conductivity. Under ideal test conditions the current in the transformer high-voltage winding would drop to zero when arc-path conduction ceases, and normal voltage is re-established.

During the tests described herein, cessation of current flow in the arc path did not result in zero current through the transformer high-voltage winding due to the presence of appreciable capacitance in the form of bus work, insulators, and circuit breaker bushings. The charging current at normal voltage to this equipment was actually greater than the short-circuit current; consequently dielectric recovery of the arc path resulted in a rise of measuring-circuit current. This effect plus the fact that the measuring-circuit potential recorded was actually the vector sum of the arc-path resistance and transformer and lead impedance drops prevent simple direct calculation of arc-path resistance from these data. Figure 2 is an oscillogram showing the time of response of the measuring circuit only. This was the tained by opening a short circuit across ob- high-voltage winding of the potential transformer by means of a 115-kv oil circuit breaker. From this it may be seen that the natural recovery period of the measuring circuit was slightly more than one cycle which is less than the recovery time of any of the phenomena investigated.

Another test circuit has been designed which will permit recording of voltage

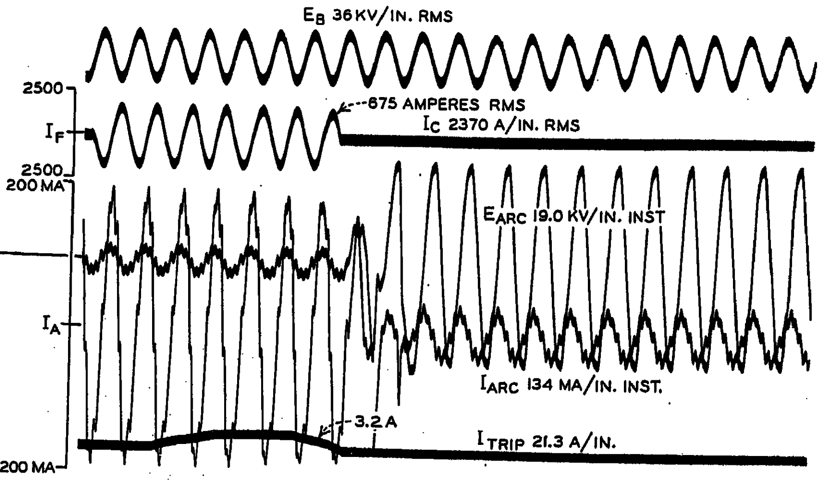


Figure 5 (A). Preliminary 13.8-kv test oscillogram of 7-cycle fault arc; 675 amperes rms. Time, 2:44 p.m.; bus voltage 13.8 kv; arc location, insulator

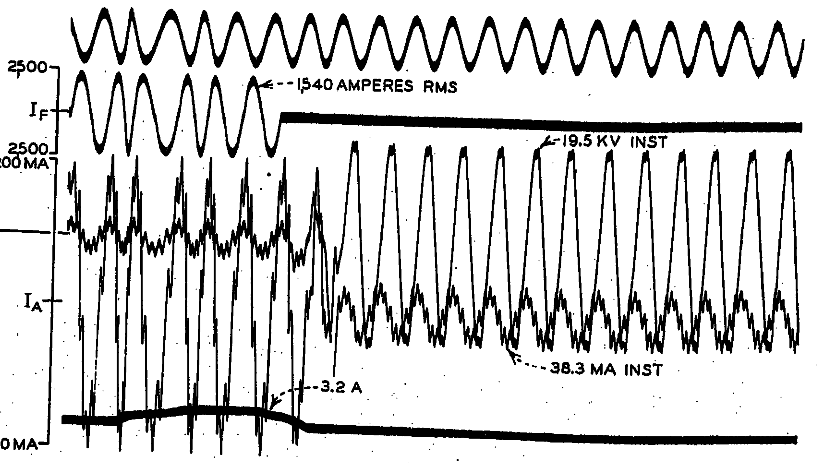


Figure 5 (B). Preliminary 13.8-kv test oscillogram of 6-cycle fault arc; 1,540 amperes rms. Time, 1:39 p.m.; bus voltage 13.8 kv; arc location, insulator

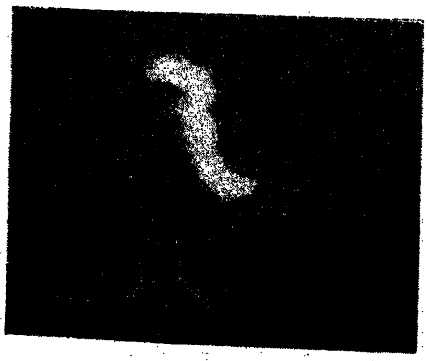


Figure 4. Typical arcing fault on test bus

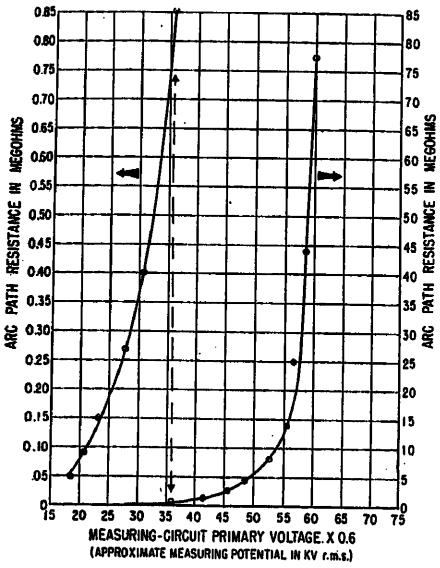


Figure 6. Fault-path resistance during recovery period as a function of measuring circuit primary voltage

drop across the arc path only and conduction current through this path only. This circuit will be described in subsequent paragraphs. It was not used during these tests, as they were performed in conjunction with a group of scheduled circuit breaker tests, and the time available for preparation did not permit construction of the necessary equipment.

The records obtained, while not ideal do permit dielectric recovery of the arc path to be observed in terms of measuring circuit potential. Figure 3 is typical of these oscillographic records. E_A is the measuring-circuit potential. Prior to application of fault current, the fault bus was grounded by a fuse wire; consequently the amplitude of the trace E_A is proportional to the transformer and lead impedance drops until the fault circuit breaker is closed. At this time the potential E_A drops to a low value due to reduction in station-service voltage combined with a phase difference in fault-producing potential. Immediately after fault interruption this voltage returns to the prefault value and remains so for several cycles, thereby indicating low arc-path resistance. After approximately $7^1/_4$ cycles in this case, the voltage begins to rise and continues to do so until full voltage is reached. At this point dielectric recovery may be considered nearly complete, as approximately 69 kv is being applied across the arc path with practically no conduction resulting.

Before using the circuit just described, it was first necessary to establish proof that currents in the order of 100 milliamperes or less would not prolong dielectric recovery of the arc path. There also was some question as to the validity of initiating fault arcs by small diameter copper wire. To determine these effects, preliminary tests at 13.8 kv were performed using a very similar circuit except that the current-limiting impedance was in the form of capacitors rather than a reactor. Use of capacitors necessitates precautions to prevent series resonance and ferro-resonant conditions in circuit.

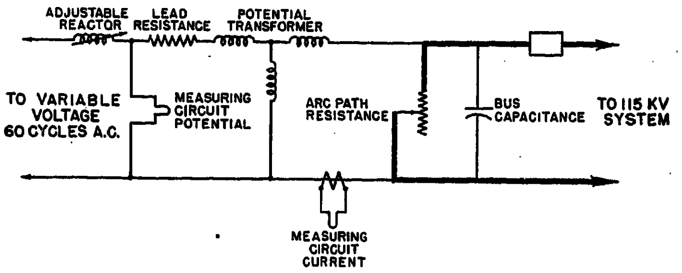


Figure 7. Equivalent measuring circuit

Test Procedure

The 13.8-kv preliminary tests were performed by closing into a fault established by a fuse wire across a 15-kv insulator or a comparable gap with subsequent automatic tripping of the fault circuit breaker and interruption of the arc. Faults were initiated by copper fuse wires varying in size from number 19 to number 33 American wire gauge. Fault currents of 600 and 1,600 amperes were used. Magnitude of the measuring-circuit current was varied from approximately 2 milliamperes to 140 milliamperes. All pertinent currents and voltages were oscillographically recorded.

The 115-kv tests were all single-phase-to-ground faults. All faults were initi-

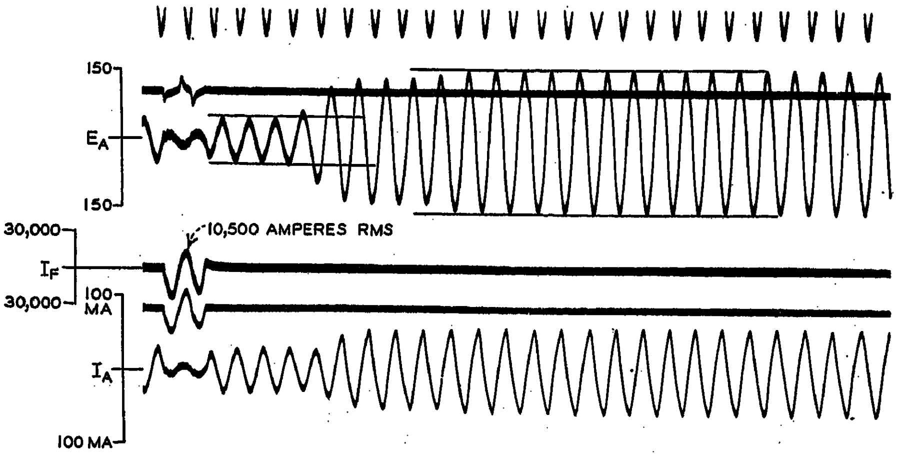


Figure 8 (A). Oscillogram showing effect of fault duration upon recovery time. Short fault duration, 10,500 amperes rms. Time 10:38 a.m., fault megavolt-amperes 2,000, bus voltage 110 kv, arc location, air

E_A = measuring circuit, primary voltage, I_F = fault current, I_A = measuring circuit, secondary current

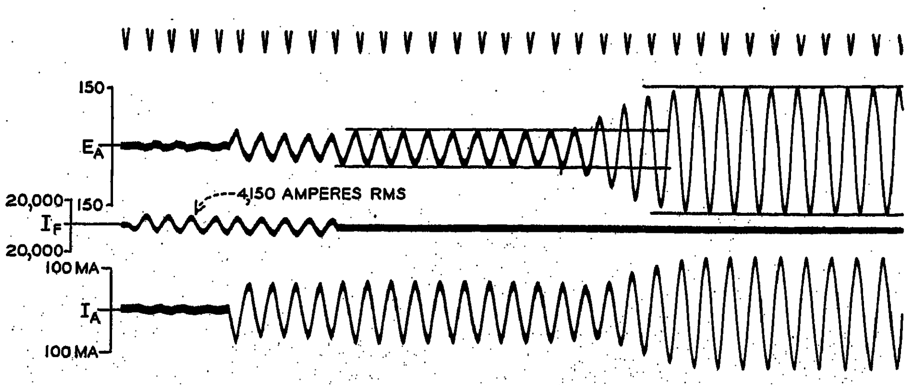


Figure 8 (B). Oscillogram showing effect of fault duration upon recovery time. Long fault duration, 4,150 amperes rms. Time, 2:14 a.m., fault megavolt-amperes 805, bus voltage 110 kv, arc location, air

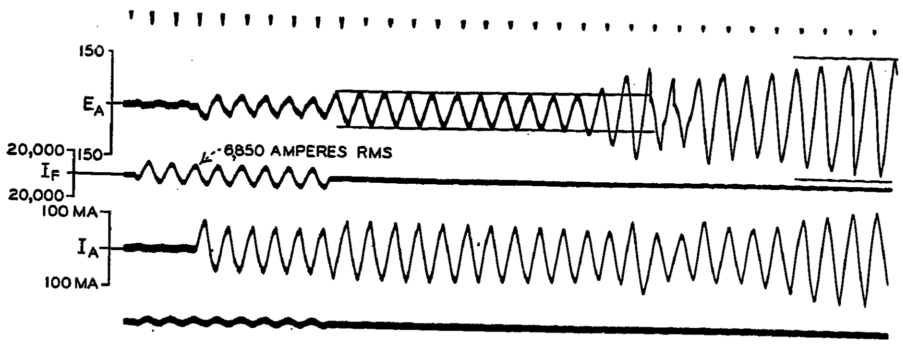


Figure 9 (A). Oscillogram showing recovery interruptions. Long duration fault and relatively low current, 6,850 amperes rms. Time 4:44 a.m.; fault megavolt-amperes 1,330; bus voltage 106 kv; arc location, air

E_A = measuring circuit, primary voltage; I_F = fault current; I_A = measuring circuit, secondary current

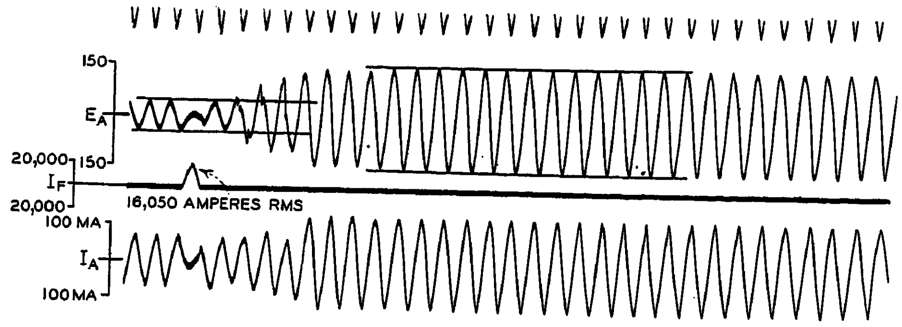


Figure 9 (B). Oscillogram showing recovery interruptions. Short duration fault and high current, 16,050 amperes rms. Time 9:40 p.m., fault megavolt-amperes 3,060; bus voltage 110 kv; arc location, insulators

ated by number 33 American wire gauge, copper wire and were arranged to cascade a 7-unit string of suspension insulators or to bridge a vertical gap formed by two strain busses spaced 46.5 inches apart (see Figure 4), except that two faults were produced between busses 14 feet apart. Fault megavolt-amperes varied from approximately 750 to 3,000 (4,000 to 16,000 amperes rms), and fault duration varied from a fraction of a cycle to a maximum of 10 cycles.

Data Obtained

A total of 28 tests was performed at 13.8 kv from which 21 useful oscillograms were obtained. These data enable conclusions to be drawn regarding effect of initiating faults by fuse wire and the effect of measuring-circuit current upon deionization of the arc path.

A total of 48 tests was performed on the 115-kv system resulting in 41 useful oscillograms. Ultra-rapid motion pictures (3,000 frames per second) of 13 of the fault arcs were taken. Single frames from two of these films are reproduced in Figure 12.

Discussion of Results

The 13.8-kv preliminary tests indicated variation of wire size from number 19 to 33 produced no appreciable change in deionization time. In all of the 13.8-kv tests recovery was very rapid, never exceeding three cycles from current interruption to full recovery of the measuring potential. From this it was concluded that the presence of metallic vapor from the initiating fuse had very little effect upon dielectric recovery.

Variation of measuring-circuit current from 2 to 140 milliamperes during the preliminary tests produced no noticeable change in deionization time. As a result, it was decided to make the 115-kv tests using number 33 copper wire to initiate the fault arc and a measuring-circuit current of approximately 70 milliamperes.

Variation of fault current from approximately 600 amperes to 1,600 amperes produced only a small change in deionization time at 13.8 kv. Figure 5 shows typical oscillograms obtained during these tests. It will be noted in Figure 5(A) that full recovery occurred in approximately 1.25 cycles with a fault current of 675 amperes rms. Figure 5(B) indicates full recovery in approximately two cycles with a fault current of 1,540 amperes. Apparently low-energy faults produce such small quantities of ionized gas that deionization is accomplished in a very brief interval of time.

In interpreting the data obtained during the 115-kv tests, it was necessary to select a time after fault-current interruption as the point where dielectric recovery had progressed sufficiently that re-ignition would not occur upon reapplication of full voltage. This point was arbitrarily chosen as being the point where the measuring-circuit potential had recovered to 150 per cent of the value attained immediately after fault interruption. For the circuit used, this point corresponds to an arc-path resistance of approximately 0.25 megohm. It is believed, however, that system voltage (69 kv to ground) could not maintain ionization through a normal fault path whose resistance is in excess of 50,000 ohms; therefore the foregoing criterion is considered conservative. The time from fault-current interruption to this point is hereafter referred to as

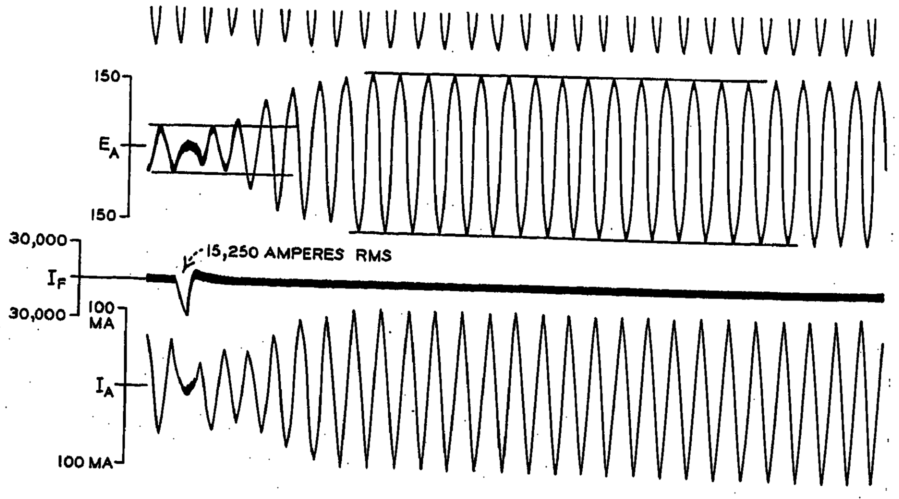
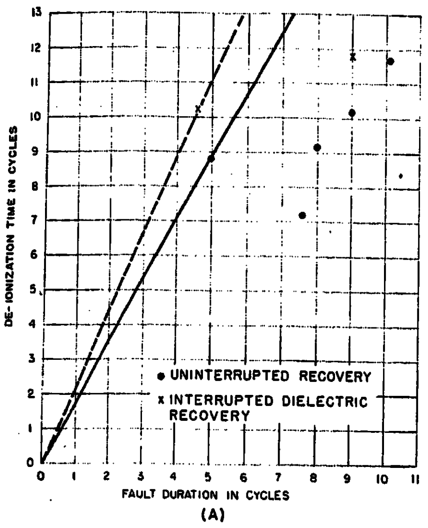
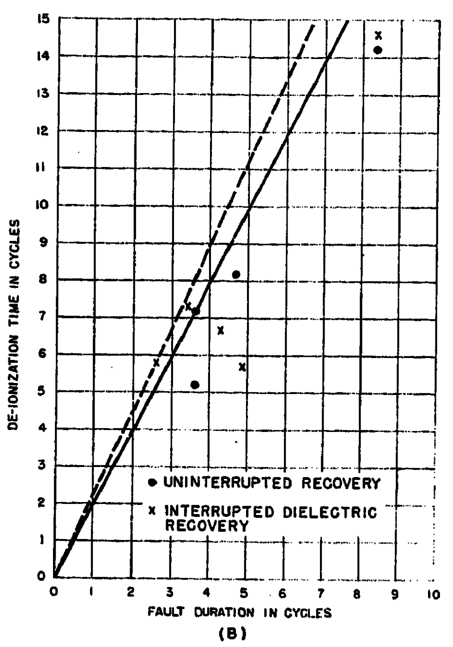


Figure 10. Oscillogram showing very rapid recovery after fault of 0.75-cycle duration; 15,250 amperes rms. Time 3:20 p.m., fault megavolt-amperes 2,920; bus voltage 110.5 kv; arc location, air





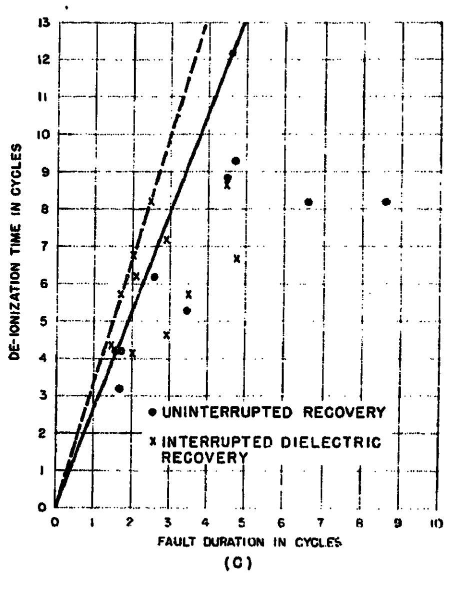


Figure 11. Deionization time as a function of fault duration. Curves drawn through maximum time points

(A) (above) 750-megavolt-ampere faults
(B) (center) 1,200-megavolt-ampere faults
(C) (right) 2,000-megavolt-ampere faults

"recovery time." This recovery time has been selected to give a consistent basis for recovery comparison and does not necessarily represent minimum time for successful reclosure.

Figure 6 is a curve of arc-path resistance shown as a function of measuring-circuit potential. This curve was obtained by setting the equivalent measuring circuit, Figure 7, up on an a-c calculating board. Arc-path resistances necessary to give currents and voltages corresponding to instantaneous values recorded on the oscillogram for test number *PE-4C* throughout the recovery period were determined directly thus eliminating much calculation. This curve shows that the path resistance is approximately 50,000 ohms at the point where the measuring potential first begins to increase. An increase in measuring potential of 150 per cent corresponds to a path resistance of approximately 0.25 megohms.

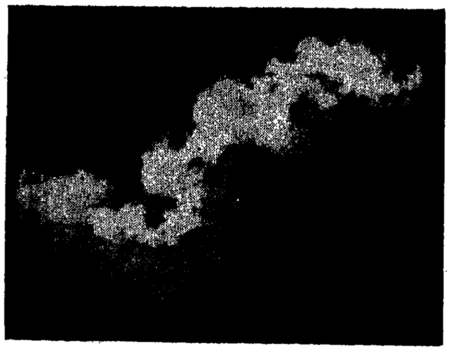
Figures 8, 9, and 10 are typical of the records obtained during the 115-kv tests. Note that in Figure 8 the measuring-circuit potential indicates a relatively low resistance of the arc path for a period of several cycles after fault interruption, after which a fairly uniform increase in path resistance occurs until conduction ceases and the measuring potential becomes a maximum. Figure 9 indicates somewhat the same conditions except that recovery is interrupted as evidenced by sudden collapse of the measuring potential. These interruptions and subsequent recoveries are probably the result of eddying of gases in the ionized column due to convection currents. Whether such recovery interruptions represent a decrease in resistance sufficient to permit re-ignition is a matter that still has to be determined. It is believed that re-ignition will not occur as a result of such interruptions as may occur after quite a few cycles of dead time; however, in order to be conservative, recovery times here-in were, in the cases where such interruptions occurred, measured from fault-current interruption to the measuring-voltage crest succeeding the last dielectric interruption.

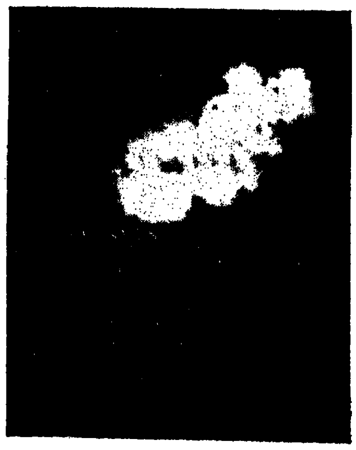
Deionization Time of Arc Path

Figure 11 shows the recovery times obtained for three values of fault megavolt-amperes. Perhaps the most significant fact disclosed by this is that recovery time is to a great extent determined by fault-current duration. Faults greater than 2,000 megavolt-amperes resulted in somewhat greater recovery time, but in all cases a decrease in fault duration shortens the recovery time. Figures 3 and 8(A) serve to illustrate this effect. In Figure 3 a fault current of 11,300 amperes rms lasting for 8.6 cycles gave a recovery time of 8.2 cycles, while in Figure 8 a fault current of approximately the same magnitude but of only 1.6-cycle duration required a recovery time of only 4.2 cycles. In Figures 8(B) and 9(A) it may be seen that relatively low currents of long duration required long recovery times, while in Figures 9(B) and 10, high currents of very short duration resulted in very rapid path deionization. The reason recovery is rapid after very brief fault durations is quite apparent from study of a series of ultra-rapid (3,000 frames per second) motion pictures taken of some of the arcs. In the cases of long fault duration, even at relatively low currents, very large volumes of luminous gases are created which cool and break up relatively slowly; furthermore, magnetic forces caused the arc to progress along the lower fault bus in one direction and along the upper fault bus in the opposite direction, see Figure 12 (left). The hot gases produced along the lower bus rise toward the upper bus as a large billowing mass prolonging con-

Figure 12. High-speed photographs of 115-kv fault arcs

(below) Long duration arc (7,500 amperes rms)
(right) Short duration arc (16,000 amperes rms)





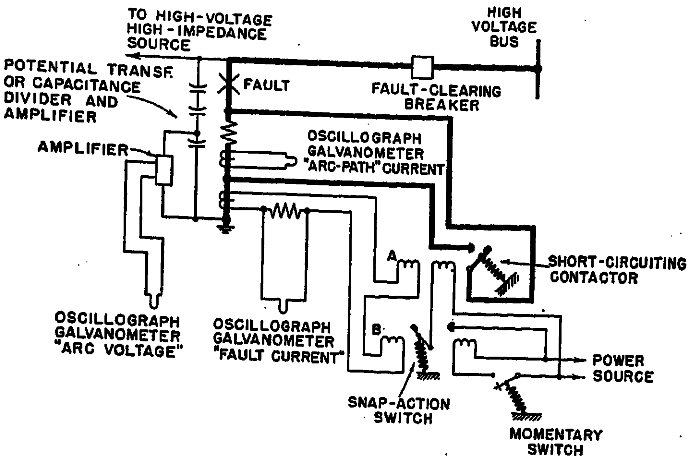

TO HIGH-VOLTAGE HIGH-IMPEDANCE SOURCE
HIGH VOLTAGE BUS
POTENTIAL TRANSF. OR CAPACITANCE DIVIDER AND AMPLIFIER
FAULT
FAULT-CLEARING BREAKER
AMPLIFIER
OSCILLOGRAPH GALVANOMETER "ARC-PATH" CURRENT
A
SHORT-CIRCUITING CONTACTOR
OSCILLOGRAPH GALVANOMETER "ARC VOLTAGE"
OSCILLOGRAPH GALVANOMETER "FAULT CURRENT"
B
POWER SOURCE
SNAP-ACTION SWITCH
MOMENTARY SWITCH

Figure 13. Improved test circuit for future deionization time investigations

duction between buses. Short periods of fault current produced a vertical column of luminous gas, see Figure 12 (right) of relatively small cross section which, upon current interruption, broke into segments as it rose and rapidly disappeared.

Circuit Breaker Reclosing Time

The results of these tests should not be taken as conclusive in respect to absolute minimum time of reclosure in practice. They are considered, however, as being indicative and possibly somewhat conservative as to 3-phase reclosing times applicable to 115-kv grounded-neutral systems. The curves of Figure 11 indicate that large magnitude faults cleared in 4.5 cycles might require as much as 12.5 to 14.5 cycles of dead time for path deionization, thereby permitting reclosure 17 to 19 cycles after inception of the fault. This is, of course, based upon the assumption that no additional lightning strokes occur after or immediately prior to reclosing.

Additional Tests

In order that definite reclosing times may be established, much additional data are necessary. Further tests on the Bonneville Power Administration 115- and 230-kv systems are planned; moreover, it is proposed that manufacturers having suitable facilities incorporate such tests as a part of routine and special high-voltage circuit breaker tests. It is believed that these investigations should be organized to provide more data similar to that presented in this paper. In addition, a series of reclosing tests should be performed in an attempt to establish what arc-path resistance is necessary to permit successful reclosure, and to determine whether the dielectric recovery interruptions discussed previously are of such a nature as to permit re-ignition if reclosure is made during that period.

Figure 13 is a schematic diagram of an improved test circuit which probably will be used for future tests on the Bonneville Power Administration system. Voltage across the arc path will probably be taken from a bushing-capacitance tap and applied to the oscillograph through an amplifier.[2] Fault current will be recorded from the secondary of a suitable step-down current transformer. After the fault current ceases, the measuring-circuit current through the arc path will be in the order of milliamperes, so in order to obtain sufficient deflection on the oscillograph element a one-to-one ratio current transformer will be used. During flow of fault current this transformer will be by-passed by the short-circuiting contactor. Action of this protective circuit is as follows:

Prior to fault-current application the momentary switch is closed, energizing the closing coil of the snap-action switch which, upon reclosing, causes the short-circuiting contactor to pick up and by-pass the one-to-one ratio current transformer. The momentary switch is then released and the fault applied. Flow of fault current through coil "B" causes the snap-action switch to open, thereby de-energizing the short-circuiting contactor pick-up coil. This contactor is prevented from opening by flow of fault current through holding coil "A." Interruption of fault current permits the short-circuiting contactor to open and thus

Table I. Summary of Results of Deionization Tests at 115-Kv J. D. Ross Substation of the Bonneville Power Administration

Test Number	Fault Mva	Fault Duration in Cycles	Time to Start of Recovery in Cycles	Deionization Time in Cycles*	Alternate Deionization Time in Cycles**	Arc Location
PE-1F	741	4.6	8.2	9.2	10.2	Air
AC-2B	717	5.0	8.2	8.8		Air
PE-1A	745	7.6	6.8	7.2		Air
PE-1E	750	8.0	8.1	9.2		Insulators
PE-1B	792	9.0	8.2	10.8	11.8	Air
PE-1C	792	9.0	9.8	10.2		Air
PE-1D	741	10.1	10.1	11.7		Air
PE-2D	1,272	2.6	2.7	3.8	5.8	Air
PE-2E	1,349	3.4	4.2	4.8	7.3	Insulators
AC-3B	1,262	3.6	3.2	5.2		Insulators
PE-2F	1,336	3.9	6.2	7.2		Air
AC-4B	1,297	4.3	3.2	3.7	6.7	Air
AC-1A	1,241	4.7	7.2	8.2		Insulators
WE-1A	1,380	4.9	3.8	4.2	5.7	Insulators
PE-2C	1,324	8.4	12.8	14.2		Air
PE-2B	1,305	8.4	10.7	11.6	14.6	Air
PE-3F	1,758	1.5	2.2	3.3	5.8	Insulators
PE-3E	1,793	2.9	6.7	7.3		Air
AC-8B	1,980	1.5	2.3	2.8	4.3	Air
PE-4F	1,995	1.6	3.2	4.2		Air
PE-5F	2,290	1.7	2.1	3.1		14-feet long
PE-8E	2,045	1.7	2.7	3.2	5.7	Insulators
AC-7B	1,992	1.7	4.1	4.2		Insulators
PE-4D	2,053	2.0	2.1	2.1	4.1	Air
PE-3D	1,970	2.0	2.3	2.8	6.8	Insulators
PE-3A	1,907	2.1	3.2	3.7	6.2	Insulators
PE-4E	2,016	2.5	3.7	4.7	8.2	Insulators
PE-5B	2,097	2.6	5.7	6.2		Air
AC-2A	1,967	2.9	5.2	5.7	7.2	Air
AC-3A	1,955	2.9	2.1	2.6	4.6	Insulators
AC-9B	1,990	3.5	1.7	2.2	5.7	Air
PE-5D	1,980	3.5	3.8	5.3		Insulators
PE-7E	2,056	4.5	7.7	8.8		14-feet long
AC-6B	1,989	4.5	6.2	6.2	8.7	Insulators
PE-4A	2,157	4.6	10.8	12.2		Air
PE-3C	1,960	4.7	8.2	9.3		Insulators
WE-3A	2,000	4.75	4.7	6.2	6.7	Insulators
WE-4A	2,060	6.6	7.2	8.2		Air
PE-4C	2,166	8.6	7.1	8.2		Air
PE-6F	3,000	0.75	1.6	2.0		Air
PE-5A	3,150	0.8	1.6	2.1	4.1	Insulators

* Taken as the time from interruption of fault current until measuring-circuit potential has recovered to 150 per cent of the value attained immediately after fault interruption.

** In cases where dielectric recovery was characterized by one or more interruptions, this alternate deionization time was taken as the time from interruption of fault current until the first measuring-circuit voltage crest after the last recovery interruption.

energize the oscillograph element recording measuring-circuit current through the arc path during the recovery period.

This circuit should permit recording of the arc-path voltage drop and current from which instantaneous values of arc-path resistance may be computed throughout most of the recovery period.

Conclusions

1. A method of obtaining useful data on fault-arc deionization time has been devised. It is hoped this will lead to the obtaining of additional data which, when summarized, will enable minimum reclosing times for 115- and 230-kv circuits to be established.
2. Deionization time of arc path is principally a function of fault duration.
3. On the 115-kv system variation of fault magnitude from 750 to 1,700 megavolt-amperes had very little influence on maximum deionization times of the arc path. For fault magnitudes of 2,000 to 3,000 megavolt-amperes, maximum deionization times were increased somewhat.
4. Although data contained herein are not conclusive, the indication is that for fault clearing times of four cycles 3-phase reclosure may be feasible after 10 cycles of dead time. Reduction of fault duration to three cycles indicates only seven cycles of dead time; while two cycles of fault duration indicate approximately five cycles of dead time prior to reclosure on a 115-kv system.

   This serves to illustrate the importance of high-speed relaying and fast circuit breakers on systems contemplating ultra-fast reclosing.
5. While decrease in fault-clearing time is always desirable and future developments may make this possible, it may not be feasible to decrease reclosing time below a certain limit as determined by multiple-lightning-stroke phenomena.
6. The presence of insulator strings in the arc path does not appreciably decrease deionization time. The use of small diameter copper wire for fault initiation does not appreciably delay deionization of the arc path.

References

1. Multiple Lightning Strokes—II, K. B. McEachron. *Electrical Engineering* (*AIEE Transactions*), volume 57, September 1938, pages 510–12.

2. A Simple and Inexpensive Method for Accurately Measuring Steady-State and Transient High Voltages, Harold L. Levinton. *AIEE Transactions*, volume 66, 1947, pages 1304–11.

3. Lightning and 60-Cycle Power Tests on Wood-Pole Line Insulation, P. L. Bellaschi. *AIEE Transactions*, volume 66, 1947, pages 838–50.

4. L'extinction des Arcs dans le Reenclenchement Ultrapide Monophase sur les Lignes a 220 Kilovolts, M. E. Maury. *Revue Generale De L'Electricite* (Paris, France), May 1944.

5. Keeping the Line in Service by Rapid Reclosing, S. B. Griscom, J. J. Torok. *Electric Journal* (East Pittsburgh, Pa.), May 1933.

6. Systematic Tests with Automatic High-Speed Reclosing in the Goesgen and Laufenburg Power Systems, W. Wagner. *The Brown Boveri Review* (Baden, Switzerland), December 1945.

Discussion

T. E. Browne, Jr. (Westinghouse Research Laboratory, East Pittsburgh, Pa.): This paper describes an interesting experimental study of an important but heretofore little understood phenomenon, the post-arc dielectric recovery of long open-air arc trails, and is to be commended as a pioneering effort. As such, it is understandable that these early results are not entirely satisfactory or conclusive, but they do give some new insight into the behavior of fault-arc paths.

In particular the authors speak of deionization, rise of high-voltage resistance, and dielectric recovery of the fault-arc paths as if these expressions were virtually synonomous. Also, in interpreting their results, the authors appear to overlook significant differences in meaning between the measured arc path resistances and the inferred dielectric strengths of the arc paths. They speak of and plot an arbitrarily defined "recovery time" or "deionization time" based on use of arc resistance, without showing how this is related to the desired practical quantity, time for recovery of sufficient dielectric strength to permit successful circuit breaker reclosure.

The basic concepts involved in the dielectric recovery of a long arc space were discussed rather completely by Dr. Slepian[1] nearly 20 years ago. The long-time scale involved here, many cycles for dielectric recovery of long open-air power arcs compared with a fraction of a half-cycle for circuit breaker arcs, does not really alter the phenomena so long as the arc can be considered to be homogeneous throughout its length. Dr. Slepian pointed out that the stable existence of an arc column depended upon a balance between power loss, W, from the hot ionized arc space due to cooling and ion diffusion, and the electric power input e_a times I. The dielectric strength of an arc space was defined as the voltage required to maintain this balance and so to hold the conductance of the arc space constant. In terms of the arc resistance, R, this can be very simply expressed by the equation

$$\frac{E_d^2}{R}=W_0, \text{ or } E_d=\sqrt{RW_0}$$

when E_d is the dielectric strength corresponding to the arc space resistance, R, and also to the existing rate of energy loss from the arc space, W_0. It is clear that E_d is a breakdown voltage, since any sustained increase in applied voltage above E_d will cause the arc space energy to increase, resulting in lowered resistance and cumulatively increasing current flow.

In assuming correspondence between the dielectric strength and the resistance of a long arc space, the authors have treated W_0, the power dissipation from the arc space, as a constant. If, as the writer believes, W_0 also is a variable, it is equally as important as R in determining the dielectric strength of the arc space. It is true that effective dielectric recovery of an arc space will accompany a rise of arc space resistance to and above some high value, but the actual magnitude of this critical resistance is entirely dependent upon the existing level of W_0. Therefore, in the absence of knowledge about W_0, the arbitrary selection of some level of arc space resistance as indicating effective "deionization," or dielectric recovery of the arc space would appear to be relatively meaningless. In particular, one would expect W_0 to be quite largely determined by both the magnitude and duration of the fault-arc current preceding the recovery period, since these quantities largely affect the actual arc path length and diameter, the degree of turbulence in the immediately surrounding air, and the related temperature and ion density gradients during the recovery period. Thus, it is not at all surprising that plots versus fault current and fault duration of "deionization time" based on rise of arc resistance alone, Figure 11 of this paper, do not agree with the plots of directly observed dielectric recovery times in the paper by Boisseau, Wyman, and Skeats.[2]

Another consideration which may lessen the practical value of resistance measurements is the observed inhomogeneity of the arc space, especially toward the end of the recovery period, as reported by the authors, and shown in Figures 4(A) and 12(A) in the paper by Boisseau, Wyman, and Skeats. If this apparent "breaking up" of the arc residue should result in virtual loss of conductance by some fraction of the arc path only, this fraction might be too short to sustain the full restored voltage even though the total resistance of the arc path may have already approached infinity. This appears to be the explanation of the so-called "recovery interruptions" reported in the paper and illustrated by the oscillograms of Figure 9. In Figure 9(A) there is clearly a dielectric failure with sudden discharge of the bus capacitance at about 60-kv rms restored voltage somewhat later than the point defined as the end of the deionization time, followed by three other successive breakdowns at still lower voltages. These show that the dielectric recovery was influenced by the measuring circuit and was not accurately indicated by the resistance measurement. In Figure 9(B) the discharges apparently begin much earlier, occurring both before and after the specified deionization time.

In view of these observations, it is the opinion of the writer that resistance rise measurements are interesting and of value in aiding our knowledge of the nature and behavior of post-fault arc trails but that they cannot safely be substituted for actual dielectric tests in determining limits for circuit breaker reclosing times.

A further comment with regard to Figure 11 is that the lines, if drawn through the means of the points instead of through extreme points in the badly scattered groups, would have slopes roughly proportional to the arc current magnitudes, indicating that the resistance rise time may be more accurately said to depend upon the product of fault current and fault duration rather than upon duration alone.

With regard to future studies, the writer wonders whether the authors have considered the use of a direct rather than an alternating source of voltage in the resistance measuring circuit.

References

1. Extinction of a Long A-C Arc, J. Slepian. *AIEE Transactions*, volume 49, April 1930, pages 521–30.

2. Insulation Flashover Deionization Times as a Factor in Applying High-Speed Reclosing Circuit Breakers, A. C. Boisseau, B. W. Wyman, W. F. Skeats. *AIEE Transactions*, volume 68, part II, 1949, pages 1058–67.

D. M. Umphrey (Pacific Electric Manufacturing Corporation, San Francisco, Calif): The authors have made a valuable contribution to fundamental knowledge concerning the high-speed reclosing of transmission circuits. It has been difficult in the past to isolate the various factors which control minimum reclosing times. For example, this paper presents the first quantitative information, to my knowledge, regarding the effect of fault arc duration on minimum reclosing time. The minimum permissible de-energized time for successful staged reclosing tests has varied from as low as six to as high as 16 cycles. There was only speculation as to whether the discrepancy was caused by air velocity, influence of metal vapor from fuse wire, humidity, time of arc duration, the proximity of porcelain insulating surfaces, or perhaps capacitively induced currents in the fault arc path during the supposedly de-energized period. In all such cases with which I am familiar, shorter fault arcing time permitted faster reclosing. This would be expected from the information supplied by the authors.

It is interesting also to note that metal vapor from the fuse wire has little effect on deionizing time. This has been a controversial point for some time.

The principle reason in the past for clearing faults in the shortest possible time on high-voltage circuits was to minimize the direct effect on transient stability. However, there now appears to be another reason of nearly equal importance, in that the time consumed by fault clearing equipment such as relays and circuit breakers, can seriously influence the shortest permissible reclosing time. The effect of multiple lightning discharges on high-speed reclosing may present a rather serious problem in addition to that of limiting reclosing speed as indicated by the authors. During the de-energized period in which deionization must take place, circuit breakers at the line terminals will receive an impulse with every repetitive stroke. Thus the circuit breaker arc rupturing equipment will be subjected to a series of impulses throughout its opening and closing strokes. I understand that such repetitive impulses have damaged interrupting equipment during normal opening operations. Furthermore, it is apparent that the circuit breaker dead time should be considered only when the circuit breaker is sufficiently open to withstand such impulses. Otherwise added interrupter arcing time may seriously reduce the interval intended for deionization.

G. Jancke (Swedish State Power Board, Kungl Vattenfallsstyrelsen, Stockholm, Sweden): The length of the dead time at automatic reclosing with regard to system stability is of great interest. The authors state that the minimum dead time is determined mainly by three factors:

1. Circuit breaker limitations. I think that we cannot neglect the spread in the relay protection time and in the electromagnetic or pneumatic-mechanical devices which govern the dead time.

2. Total duration of lightning stroke or its multiple components. The authors state that the data available at present indicate that about 10 per cent of the lightning strokes persist for more than 0.25 second. The experiences of the Swedish State Power Board refer mainly to 22-kv systems. We have statistics from 1,100 high-speed reclosures up to 1947. The system networks are grounded through Petersen coils and thus the experiences refer to 2-phase and 3-phase faults, the single-phase faults being mainly extinguished without line circuit breaker operation. Eighty-two per cent of the high-speed reclosures have been successful, while in 14 per cent of the cases a second delayed reclosure in a couple of minutes has succeeded. The fault duration has been about 0.10 second and the dead time 0.20 to 0.25 second. At first a dead time of 0.10 to 0.15 second was used but then the percentage of unsuccessful reclosures was much higher. The 14 per cent may be caused by multiple strokes, birds, trees, and so forth. On 132- and 220-kv networks we have used 0.25 to 0.30 second dead time interval with good results. Our experiences however, are not sufficient to allow any definite conclusions. In many cases it is very important to use short dead time interval with regard to stability and then we might take the risk of being unsuccessful at the relatively spare multiple strokes of long duration.

3. Deionization time of the fault-arc path and its consequent recovery of dielectric strength. The experiments made by the authors are most valuable and form a good base for the determining of the dead time interval. I am especially interested in the conclusion that high-speed relaying and fast circuit breakers are of an extreme importance. The authors promise that further tests will be performed. I think it would then be valuable to investigate single-phase reclosure and especially the influence from line section length on the minimum dead time. Furthermore many circuit breakers are fitted with resistors. On a common Swedish 220-kv circuit breaker, nonlinear resistors will be inserted to diminish switching surges. The current through the resistors will not be interrupted during the dead time. It is very small but may affect the deionization unfavorably to some extent.

E. J. Harrington and **E. C. Starr:** The comments by Mr. Umphrey relative to the influence of multiple lightning strokes on circuit breaker reclosing performance, and the experience data on high-speed reclosing in Sweden given by Mr. Jancke are valuable additions to the paper. In general, the gain in system stable capacity obtained by high-speed reclosing is very sensitive to fault and reclosure times. Fast clearing and the minimum possible dead time are highly desirable, but unsuccessful reclosures must be kept to an acceptable minimum. The present paper covers an investigation of some of the interrelated variables involved in the problem.

In reply to the excellent discussion by Mr. Browne, it should be emphasized again that the authors were attempting primarily to obtain relative information on the dielectric recovery of fault-arc paths for a limited number of fault conditions. Information was desired and obtained on the actual time required for the arc trail to deionize to such a degree as to permit full voltage to be restored across the space, using a high-impedance source to limit the current to a negligible value.

Since fault-path resistance values were available from the data obtained, and since finite currents are required to re establish such an arc, a resistance value (250,000 ohms), obtained at relatively high voltage, was chosen as a bench mark to indicate a relative state of deionization. As was stated in the paper, it is not known just what per cent of successful reclosures could be obtained at this indicated resistance; that information is yet to be obtained. The very character of the arc, that is, turbulence, looping, and path multiplicity, as vividly shown by high-speed motion pictures taken during the tests, is such as to make the minimum successful-reclosure time a matter of probability; there can be no abrupt threshold between successful and unsuccessful reclosure times.

It was pointed out in the paper that eddying of the hot gases tended to produce voids that probably resulted at times in measured resistances that would infer dielectric strengths greater than actually existed. In some of these cases the rapid rise in measuring potential caused at least partial breakdown of the voids with accompanying decrease in resistance. There is no assurance however, that reapplication of full voltage at the instant of interruption in recovery would have resulted in currents of sufficient magnitude in each case to re-establish the arc. These delays or interruptions in dielectric recovery were taken into account in the curves presented.

The equation given by Mr. Browne for arc voltage, $E_d=\sqrt{RW_0}$, is recognized as applying to stable arcs. Since the voltage drop across a given length of arc is essentially constant and independent of current, it is indicated that the critical resistance is inversely proportional to the energy loss from the path of the arc core. If that core is surrounded by a cloud of incandescent gas resulting from long arc duration at high current, the energy loss will be lower than if the gas cloud is absent or less extensive. Hence the critical resistance for arc reignition should be somewhat lower for short-duration arcs, but there is probably not an order of magnitude difference. Furthermore the short-duration arcs exhibit a much more rapid rise of resistance than the arcs of long duration. See Figures 8(A), 8(B), and 10 of the paper.

The authors cannot agree with Mr. Browne's statement that the dielectric recovery was influenced by the measuring circuit. If this were the case the extremely rapid recovery indicated in Figure 10 following a 1-cycle 15,000-ampere arc would not have occurred.

Future studies are contemplated again in connection with a limited number of circuit breaker capability and acceptance tests. Improved measuring techniques will be employed, but a source of high-voltage direct current will not be available for the measuring circuit at least not for the next tests. Some of the future work may include the influence of charging current in the arc path following fault interruption to simulate the conditions of single-pole switching.

A Transient Fault Locator for High-Voltage Transmission Lines

L. R. SPAULDING
ASSOCIATE AIEE

C. C. DIEMOND
MEMBER AIEE

THE location of high-voltage power line faults by echo-ranging means has received considerable attention during the post-war years. Recent work by the Bonneville Power Administration (BPA) in the development of high-voltage transmission line fault locators, together with a broad review of the problem in general and a rather complete bibliography, has been reported in another paper,[1] therefore, that material will not be repeated here except in brief summary. That paper describes two types of transient fault locators operating on the fault generated surge principle.

In the type *A* system, the surge from the fault travels to a station at one end of the line where it starts a timing device and reflects back toward the fault. From this instant, the device acts in normal echo-ranging sequence as though the surge were generated at the station. The round-trip time to the fault and back again is a measure of the distance to the fault.

In the type *B* system, the surge from the fault travels to a station at one end of the line where it starts a timing device. The reflection of the surge from the station bus is not used. A surge simultaneously travels from the fault to a station at the other end of the line where it is transmitted via a broad-band radio channel to the first end. The elapsed time interval between the arrival of the two surges at the first station provides a measure of the distance to the fault.

Another system also has been proposed[2] for transient fault location in which an echo-ranging type of fault locator continuously pulses the line and makes a photographic record automatically from an oscillograph during fault. This device is permanently coupled to just one phase of a 3-phase line.

Other echo-ranging fault locators of the nontransient type also have been described, or are commercially available. Most of these require that the line be removed from normal service for fault location purposes.

One type,[3] builds up direct voltage on the line to a potential high enough to break down any point of weak insulation. The surge generated by this breakdown is used to start the timing device and to reflect from the line discontinuities in typical echo-ranging sequence.

Another type,[4] requiring metallic connections to the line, applies a repetitive pulse to a permanently faulted phase of the line and gives a visual presentation of the fault location on an oscilloscope screen.

Still another type applies a continuous wave of constant frequency (nonpulsing) to a faulted phase of a line and observes the phase shift of the reflected wave.

It is the purpose of this paper to report further work by the BPA in the study of the problem and in particular to describe the type *C* fault locator which fulfills several additional requirements of an ideal fault locator.

The transient fault locator discussed in this paper permits the location of nonpermanent faults such as are often caused by lightning. These may not remove the line permanently from service, but may cause damage to insulation or conductors. It is highly important that these points of fault be located for inspection and repair.

The type *C* fault locator is designed for use where suitable wide-band communication circuits are not available to carry the intelligence requirements of the type *B* fault locator. It also provides a freedom from spurious pulses; a trace which does not require specially-trained engineering personnel for interpretation when a photographic record is used; a means of operating an electronic counter for a direct reading record; and a freedom from causing interference to closely associated carrier or radio communication facilities.

The difficulty encountered from spurious pulses associated with the surge at the start of a fault which was used as the source of pulse energy for echo-ranging in the type *A* fault locator, together with difficulty in handling the extremely wide ranges of pulse magnitudes encountered in the fault generated surge, lead to investigations of this so-called pulse-radar method of echo-ranging for fault location. .

Improved methods of coupling the equipment to a live line through carrier coupling capacitors which are connected during fault by information from a conventional phase selecting relay system, were developed to obtain more pronounced reflections from the fault and a single applied pulse instead of a continuously repeating pulse was selected for simplicity and freedom from causing carrier and radio interference.

Operation of the Type *C* Fault Locator

During the waiting period between faults, the fault locator is not connected to the line. When a single-line-to-ground fault occurs, a simple phase selecting relay system connects the fault locator through a carrier coupling capacitor between the faulted phase and ground. After approximately 33 milliseconds from the

---

Paper 49-206, recommended by the AIEE Instruments and Measurements and Transmission and Distribution Committees and approved by the AIEE Technical Program Committee for presentation at the AIEE Pacific General Meeting, San Francisco, Calif., August 23–26, 1949. Manuscript submitted May 2, 1949; made available for printing June 29, 1949.

L. R. SPAULDING and C. C. DIEMOND are both with the Bonneville Power Administration, Portland, Oregon.

The authors wish to express their appreciation to A. W. Adams, H. L. King, D. E. Smith, T. T. Smith, and T. W. Stringfield for their criticisms and suggestions.

start of the fault, a single unidirectional (d-c*) 1-microsecond pulse from a 10-kv source is applied to the line after which the pulsing unit is disconnected and a receiving unit is actuated. A timing device is started by the initial pulse and the elapsed time before the return of a reflection is recorded. This provides a measure of distance to the fault. In the event of a multiple phase fault such as a phase-to-phase or 2-phase-to-ground fault suitable information from the relay system will connect the fault locator between one of the two or three faulted phases and ground.

Investigation

In the experimental work, the timing device used was a cathode-ray oscilloscope with a linear sweep. The initial pulse started the sweep and the reflected pulse appeared as another "pip" on the sweep at some time later proportional to the fault distance. The permanent record was obtained by photographing each trace on an individual frame with a standard 16-millimeter motion picture camera. This method was particularly suited to the work because of its ability to show the presence of any spurious or undesired pulses which might interfere with the operation of an electronic counter or other direct indicating devices which might be employed.

In the development of the type *C* locator, these questions had to be answered:

1. What pulse magnitude should be used?
2. What interval during the fault should be used for operating the fault locator?
3. Should the pulse be repeated continually at all times; applied on a repetitive basis during fault; or applied just once during fault?
4. What pulse width should be used?
5. Should a d-c or a radio-frequency pulse be used?
6. How should the fault locator be coupled to the line?
7. What type of recording device should be used?
8. What relaying system should be used?

Obviously these questions are all related so the best answer to one might not allow the best answer to the others.

Pulse Magnitude

In seeking an answer to the question of pulse magnitude, a large number of oscillograms were reviewed which were obtained from automatic magnetic oscillograph operations throughout the Bonneville system during a period of several years. Also, cathode-ray oscillograms obtained by photographic means during staged faults were reviewed, paying particular attention to voltage wave distortions existing during the first few cycles after the start of a fault. The information gained from the magnetic oscillograms was limited due to the comparatively low-frequency response of the oscillographs and their related inability to record steep-wave fronts of the order which would provide interference to a fault locator of the type proposed. However, it was the best accumulated source of information available and did give an idea of the frequency of occurrence of low-frequency voltage distortions in the time immediately after the start of a fault. A comparison of the magnetic and cathode-ray oscillograms indicated that it was logical to assume that indicated low-frequency distortions would be related to higher frequency distortions and that an interval free from low-frequency distortions would probably be free from higher frequency distortions. The results of this review, together with a

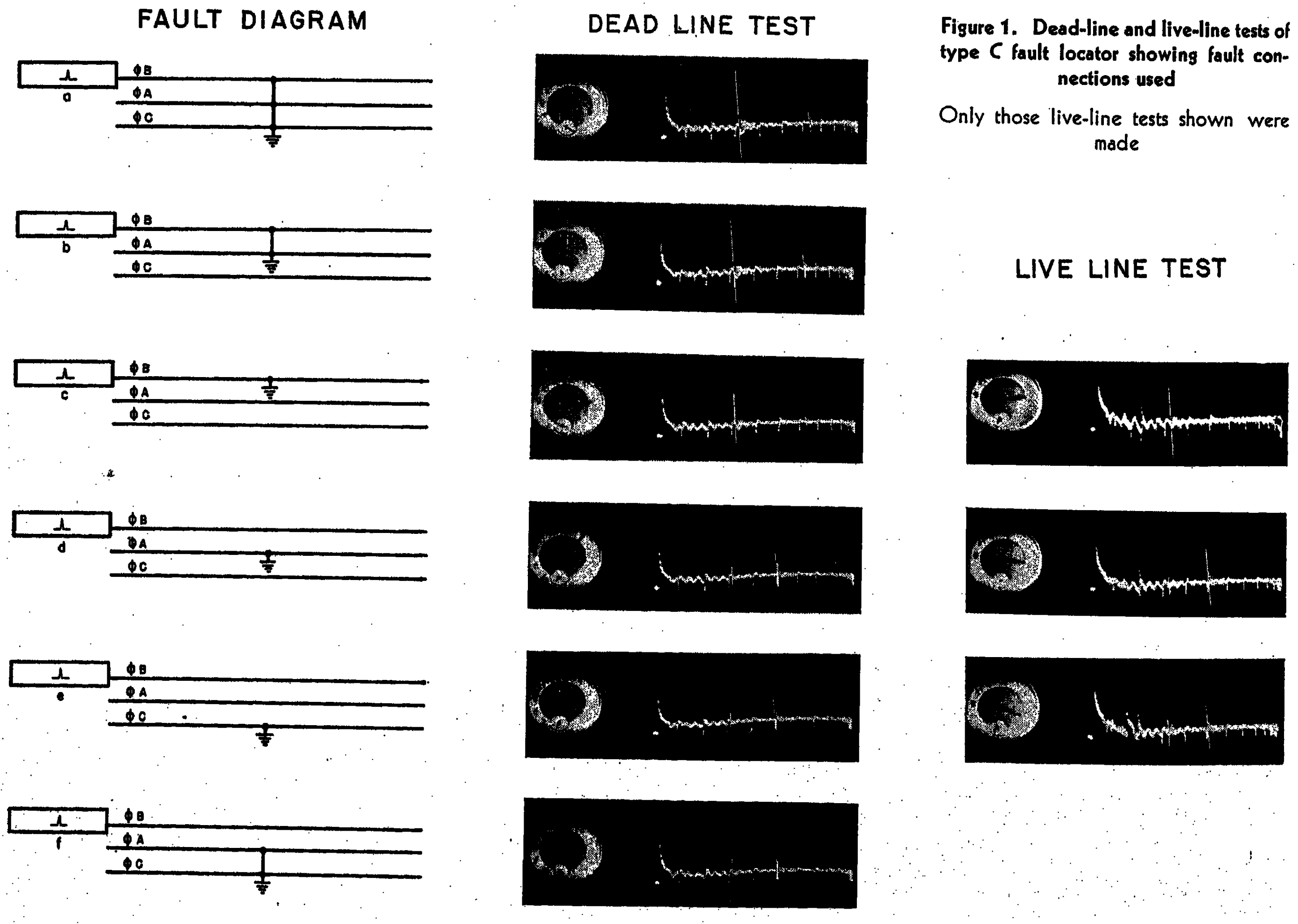


Figure 1. Dead-line and live-line tests of type C fault locator showing fault connections used

Only those live-line tests shown were made limited number of live-line fault tests using a d-c pulse for echo-ranging, disclosed that a pulse from a 10-kv source could be expected to override most interference after the second cycle of fault.

---

* The term "d-c pulse" is used to denote this unidirectional pulse.

Interval During Fault

Since the device must obtain its information before the fault is cleared, yet must wait at least two cycles of 60 cycles, the third cycle of fault was defined as the logical time for its operation.

Pulse Repetition

If a continuously repeating pulse were to be used, the time of most value in obtaining a record, for example, photographing a cathode-ray trace, would be during the third or later cycle of fault. A photograph of multiple traces on one frame might be distorted by spurious pulses if an exposure were started too soon after the start of the fault or continued through the circuit breaker opening time. The number of useful repetitions of the pulsing interval which might be expected during the third cycle of fault depends upon the length of the line being monitored and the recovery time required by the pulse originating equipment. For a 100-mile line, the theoretical maximum number of pulses could not exceed 16 and the actual practical number might not exceed five. In the event an electronic counter were employed, it could only make use of the first transmitted pulse and its reflection, so subsequent pulses would be of no value. Considering the added complications involved in delivering a repetitive pulse of the required magnitude for the benefit of the photographic record, and the questionable advantage to be gained, it was decided to use only one pulse properly placed with respect to time after fault initiation and before fault interruption.

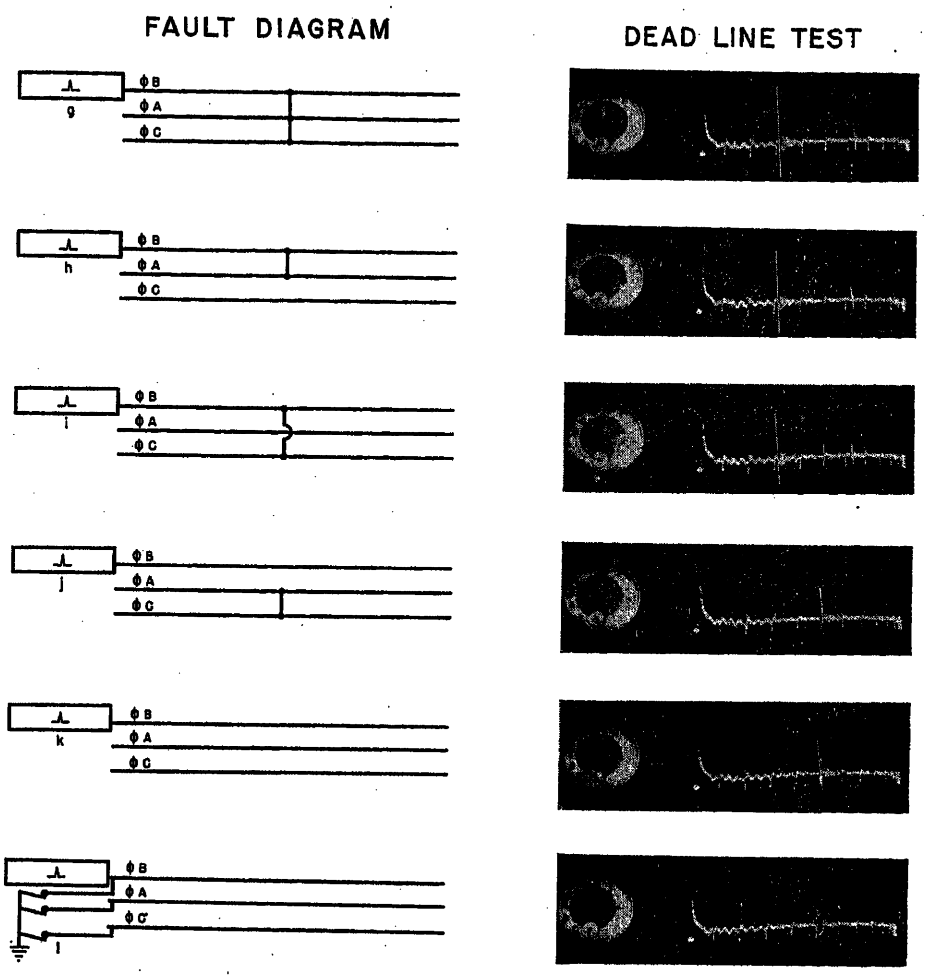


Figure 2. Dead-line tests of type C fault locator showing fault connections used

No live-line tests were made

Pulse Width and Type

In the interest of accuracy of measurement, it was desirable to use as narrow a pulse as possible. A d-c pulse of the order of one microsecond could be passed through an ordinary carrier coupling capacitor without undue losses and a d-c pulse was by far the easiest to produce. A radio-frequency pulse could be made longer than one microsecond and of any particular shape desired for easy recognition on a photographic record. However, it was felt that the reflection obtained from the type *C* locator would be pronounced enough so that a special shape of pulse would not be necessary. This fact became evident after improved coupling schemes had been developed. For the operation of the electronic counter, the magnitude of the pulse is of more importance than the shape. Therefore, the 1-microsecond d-c pulse was chosen.

Coupling

One of the most important factors to be decided was that of coupling to the line. Early experience with the type *A* locator[1] suggested that a loose common coupling to all phases of a station bus would not give best results for all faults regardless of the phase that was faulted. Also it suggested that close coupling to just one phase of a 3-phase line would not give equally good results for faults on the other phases.

Accordingly, a series of tests were arranged on the BPA 230-kv Ross–Longview line in which a series of connections were made at a point 32 miles from the Ross Substation representing various types of faults but with the line dead. Phase B was pulsed through the coupling capacitor at Ross Substation for each condition. In order to verify the assumption that these dead-line connections would appear practically the same to the fault locator as an actual fault, three of the combinations were repeated under arcing line to ground fault conditions. The results of these tests are summarized in Figures 1 and 2. Phasing shown in Figures 1 and 2 is that existing at the point of fault.

In these figures, the left-hand columns show the fault connections for each test. The square attached to the top phase represents the fault locator. The center column shows the photographic record made for each dead-line test. The right-hand column shows the record made for an actual arcing line to ground fault for the condition shown in the left-hand column. In these cases, the fault was produced by pulling a small copper wire into the line with a glass string. The traces shown have been touched up for reproduction purposes by inking over various reflection "pips". The regularly spaced negative "pips" appearing on each trace are range markers indicating each ten miles of line.

Figure 1 shows 3-phase, 2-phase, and single-phase grounds designated as A, B, and C respectively. Each case involves the pulsed phase and each trace shows a pronounced reflection from the ground. The reflection at 64 miles is the second return of the pulse after having completed two round trips from Ross to the fault and back. Note that in (c), a live-line arcing ground fault was established for purposes of comparison and that the two traces check each other. The slight difference in distance indicated was caused by the real fault being farther away from Ross than the dead-line fault connection. Figure 1 shows ground combinations at (d), (e), and (f) which do not involve the pulsed phase. Note that in each case the reflection is not good. In these traces, pronounced reflections from the open end of the line at Longview at 51 miles are present. Figure 2 shows short-circuited combinations at (g), (h), and (i) involving the pulsed phase. Note that in each case a pronounced reflection is present. A short-circuit combination which does not involve the pulsed phase is shown by Figure 2 (j). Note that the reflection from the fault is not good. Figure 2 (k) shows a trace obtained with no shorts or grounds. The reflection shown is from the Longview end of the line. Figure 2 (l) shows a trace obtained when the ground switch at Ross was closed grounding all three phases. The fault locator was connected to phase B as in all the previous tests through a carrier coupling capacitor on the line side of the carrier line traps about 50 feet away from the ground switch. Note that the different shape of the reflection from the Longview end at 51 miles consists of high-frequency oscillations apparently caused by the line connections at Ross.

The fact that every reflection shown extends both above and below the base line indicates that even though the pulse originates as a unidirectional pulse, its characteristics are modified by the system parameters and connection of the circuits through which it feeds and to which it is connected. This might suggest that a radio-frequency pulse would be affected by these reactances and that for greater efficiency, it is desirable to shock-excite these reactances with a d-c pulse.

Additional dead-line tests were made with a ground on B phase 32 miles from Ross, but with the fault locator coupled through carrier coupling capacitors to various combinations of paralleled phases. All parallel connections were made on the fault locator side of the coupling capacitors.

Photographic records of these tests are not reproduced in this paper but the results are as follows:

1. Pulsing phases A, B, and C in parallel showed a very slight reflection from the ground and the reflection from the Longview end greatly attenuated.
2. Pulsing phases A and B in parallel showed a small reflection from the ground and the reflection from the Longview end partially attenuated.
3. Pulsing phases B and C in parallel showed similar results to (2) above.
4. Pulsing phase A and phase C in parallel showed the reflection from the ground good although somewhat smaller than when phase B alone was pulsed, and the reflection from the Longview end was greatly attenuated.

The conclusion reached from these coupling tests is as follows:

> For pronounced reflections from a fault, the locator should be connected to a single phase which is involved in the fault and ground. One exception to this was noted in test 4 above for a single-phase-to-ground fault, which may be located by connecting to the two unfaulted phases in parallel and ground. This latter method of coupling may be necessary where coupling capacitors connected to only two phases are available or where a third capacitor carries a service such as carrier relaying which cannot be disturbed.

Recording Device

The technique for the automatic photographing of single sweep transients on an oscilloscope has been covered in previous discussion of the type *A* fault locator.[1] Until the satisfactory operation of an electronic counter as applied to the type *C* locator has been fully confirmed by experience, the use of the oscilloscope and camera will be continued. An electronic counter now on order will later be incorporated in a type *C* fault locator now being constructed for installation at Spokane Substation, together with the oscilloscope photographic recording equipment. In this way, the oscilloscope trace can be used to check the operation of the counter and it is hoped that the photographic recording equipment can eventually be eliminated.

In operation, the initial pulse will start the counter which records the number of 1-microsecond time intervals until it is stopped by a reflected pulse from the fault. The reading appears on a decade bank of lights which hold their indication until manually reset. The time necessary for the pulse to travel one mile and to be reflected back to the point of origination is 10.73 microseconds. The fault distance in miles will be obtained by referring the indicated time interval to a calibrating curve.

Relaying System

Various means for controlling the time of pulse application were considered for the type *C* fault locator. Economy of equipment dictated use of a relaying scheme which would switch the fault locator to one of the faulted phases on either line. Employing relatively inexpensive relaying components allows the use of the more complex and expensive electronic devices in this equipment, such as the oscilloscope, for fault location on more than one 230-kv line.

Limitations of available electro-magnetic relays including inertia, burden, and so forth, should not present too many problems in this application. However, we are hopeful that newer developments possibly in electronic relays will permit closer control of this fault locator.

Functional Details of the Type *C* Fault Locator

The functional details of the type *C* fault locator are shown diagrammatically in Figure 3. In this figure are shown a transmission line (1) connecting two substation busses, a fault (2), carrier current coupling capacitors (3) for each phase and coupling capacitor selector relay contacts (4) which are operated by the phase selecting relay system. The connections leading to a typical carrier communication equipment (5) and a conventional protective gap (6) for protection of the line tuning unit are also shown.

When a C-phase-to-ground fault occurs, the phase-C selecting relay operates the related coupling capacitor selector relay so as to first disconnect the carrier communication equipment and second, to connect the fault locator to this phase. Simultaneously, the pulse originating relay which operated contacts (7) is energized. This relay, together with the phase selecting relay, is timed to operate from 2 to $2^{1}/_{2}$ cycles after the start of a fault. The coupling capacitor selector relay contacts are timed so as to always close ahead of the pulse originating relay contacts (7). As these pulse originating relay contacts move towards closure, the voltage from the 10-kv power supply breaks down gap (8) and the reduced gap across contacts (7), applying a pulse through the phase C coupling capacitor to the line. Due to the constants of the coupling capacitor, the line, and the supply circuits, current will flow through the coupling capacitor for a time in the order of one microsecond. This completes the transmission of the pulse.

As long as the arc across gap (8) continues, the relatively large output capacitor (9) in the 10-kv power supply provides a shunt path to ground through which any reflection coming back to the station will pass. In order to disconnect this part of the circuit as quickly as possible after the pulse has been originated, an 8-ohm resistor (11) and gap (12) were provided through which the output capacitor will discharge in about ten microseconds. This gap (12) shorts out the 750-ohm resistor (13) during the discharge of the output capacitor so as to reduce the voltage fed to the receiving circuit through coupling condenser (14).

The phase selecting relay system in the fault locator is arranged so that all elements reset when the fault current is cleared. After gap (8) has fired, condenser (9) has discharged, and the arc across gap (8) extinguished, the voltage across condenser (9) is prevented from building up to full value immediately by resistor (33). This gives the pulse originating relay contacts time to reset before gap (8) would break down a second time. In this way, only one pulse is supplied to the line for each fault.

Gaps (12) and (8) extinguish simultaneously leaving the receiving circuit connected to the line through the 8-ohm resistor (11). The 750-ohm resistor (13) provides little shunting effect to the desired incoming signal yet assists in properly terminating the line from the carrier capacitor. Coupling condenser (14) together with coaxial cable (15) acts as a differentiating circuit to discriminate against low-frequency signal components attempting to enter the receiving circuit. The high-pass filter (16) provides proper termination for the coaxial cable and discriminates against all frequencies below 200 kc, in particular the carrier frequencies. In this way, even though the carrier transmitter connected to the line is operating at the time of fault, it will not interfere with the operation of the fault locator through stray coupling which inevitably exists around the coupling capacitor selector relay contacts. The carrier signal is interrupted only during the fault and is restored to normal as soon as the circuit breaker is opened and the relays reset. On a voice circuit, this provides very little more interference than the fault itself together with fault interruption by the circuit breaker.

A limiter amplifier (17) is provided in the receiving circuit which prevents the initial pulse from distorting the trace (in the case where an oscilloscope is used) or from saturating one of the following stages.

Breakdown of gap (8) also results in application of a positive pulse to the grid of a gas tube (18) through coupling condenser (19) and coaxial (20). This causes the gas tube to conduct and to supply a positive pulse to the oscilloscope (29) to start the sweep. The gas tube also racks up a new frame on the camera by operating the solenoid (22). The mechanical delay in the solenoid and in the camera due to inertia is sufficient to allow complete exposure of a frame before the film is moved. The gas tube also fires an argon light (23) within the oscilloscope light shield in such a way as to illuminate a watch face for recording the time of each sweep on each exposed frame. In addition, gas tube (18) actuates a bias control stage (24) which controls the bias on the time varied gain stage (25). The bias control stage allows the negative bias on the time varied gain stage to reduce gradually during the sweep so that the gain will increase during the sweep period. This allows reflections returning from a greater distance away which are attenuated more than earlier reflections to receive more amplification and appear more uniform on the record. Uniformity is desirable also for the operation of the electronic counter (31). When the gas tube conducts, a gate circuit (26) is driven which actuates a final rectifier detector stage (27). No pulse can reach the oscilloscope or the electronic counter until this detector stage is actuated as a result of the gas tube conducting. In this way, the electronic counter will not operate at the wrong time due to spurious pulses feeding into the receiving circuit across the capacitance of coupling capacitor selector relay contacts or through other stray coupling. The d-c plate voltage is removed momentarily by reset circuit (28) after completion of one chain of operation.

The camera (32) is a standard 16-millimeter motion picture camera with the shutter removed. Each frame is continually exposed to the oscilloscope screen but the screen is completely blanked out between operations.

Difficulty was experienced at first from light radiation from the cathode of the cathode-ray tube coming directly through the screen into the camera and clouding the film. After attempts were made to correct the trouble with light filters, this undesirable condition was finally overcome by lowering the camera several inches so that it was out of the direct line from the cathode. In order to prevent small quantities of stray light from eventually clouding the film, the camera is racked up automatically once an hour by a pair of contacts which are operated by a clock motor.

It is desirable in automatic models to use large storage condensers throughout the equipment to sustain the d-c voltages for a period of several cycles during possible reductions in station service voltage due to fault. Use of electron tubes of the cathode type which maintain satisfactory operating temperatures for several cycles (on a 60-cycle basis) after an interruption of the supply voltage also is recommended.

Installation at Spokane Substation

The high-voltage carrier coupling capacitors required for operation of the type *C* locator is a major item of expense. The installation of the device at Spokane Substation has been simplified by the presence of these capacitors on the necessary phases of each of the 230-kv lines terminated there.

The scheme as designed for Spokane uses phase selecting relay equipment which switches the fault locator equipment to the faulted line and phase. In the actual installation as shown in Figures 4 and 5, selecting equipment is provided for only the line-to-ground faults. Since the overwhelming preponderance of faults on the BPA 230-kv system are single-phase-to-ground faults, it was felt that the added complication necessary to recognize and properly switch the locator for multiphase faults was not warranted in this initial installation.

The phase selector relays are high-speed directional elements. In this application, the faulted phase is selected by comparing the direction of power transformer neutral current with phase current. The faulted line is determined by operation of an instantaneous overcurrent relay or by the operation of a directional comparison carrier ground auxiliary relay. The overcurrent relay is set above the maximum through fault current for each line.

Operating experience in this particular application may show the need for a more complex phase selecting scheme. If changes are found necessary, it is planned to use a negative-zero sequence current comparison scheme as in a previously reported system for single pole reclosing.[5]

A modification of the basic relaying scheme is shown for Spokane at the bottom of Figure 5. Here the coupling capacitor on phase A of each line carries the carrier relaying channel. On this channel, the carrier signal cannot be interrupted during fault as can voice channels. In order to avoid filtering the fault locator pulse out of the carrier relay equipment which would be connected in parallel during the fault locator operating period, it was decided to try the connec-

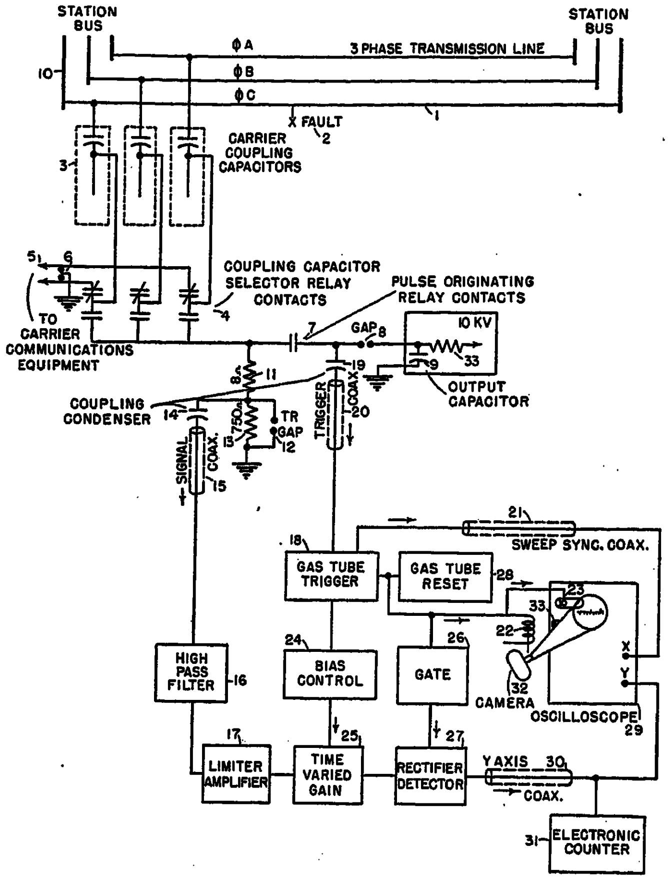


Figure 3. Functional diagram of type fault locator general

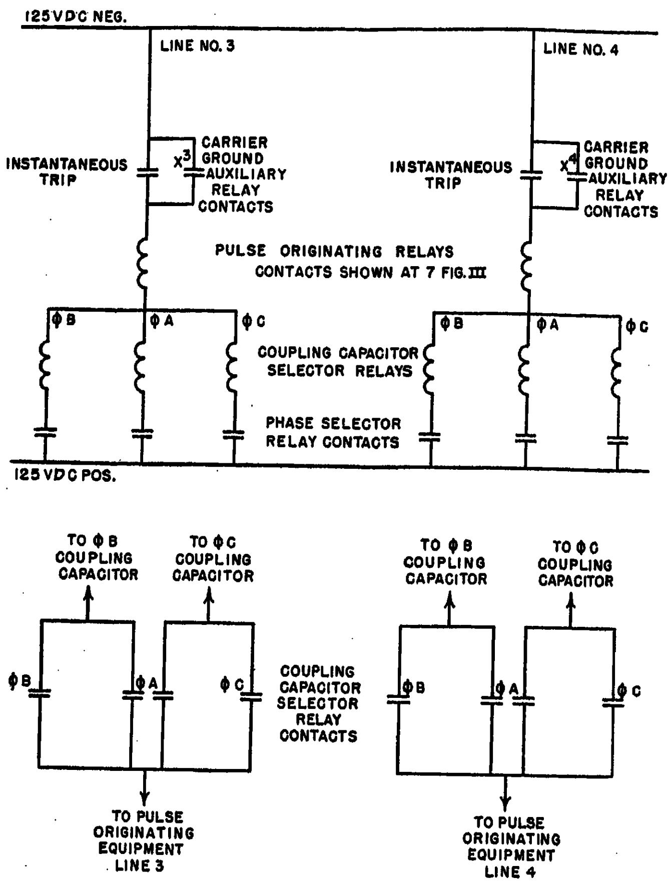

125 V DC NEG.
LINE NO. 3
LINE NO. 4
INSTANTANEOUS TRIP
X3
CARRIER GROUND AUXILIARY RELAY CONTACTS
INSTANTANEOUS TRIP
X4
CARRIER GROUND AUXILIARY RELAY CONTACTS
PULSE ORIGINATING RELAYS CONTACTS SHOWN AT 7 FIG. III
ϕB
ϕA
ϕC
COUPLING CAPACITOR SELECTOR RELAYS
ϕB
ϕA
ϕC
PHASE SELECTOR RELAY CONTACTS
125 V DC POS.
TO ϕB COUPLING CAPACITOR
TO ϕC COUPLING CAPACITOR
TO ϕB COUPLING CAPACITOR
TO ϕC COUPLING CAPACITOR
ϕB
ϕA
ϕC
COUPLING CAPACITOR SELECTOR RELAY CONTACTS
ϕB
ϕA
ϕC
TO PULSE ORIGINATING EQUIPMENT LINE 3
TO PULSE ORIGINATING EQUIPMENT LINE 4

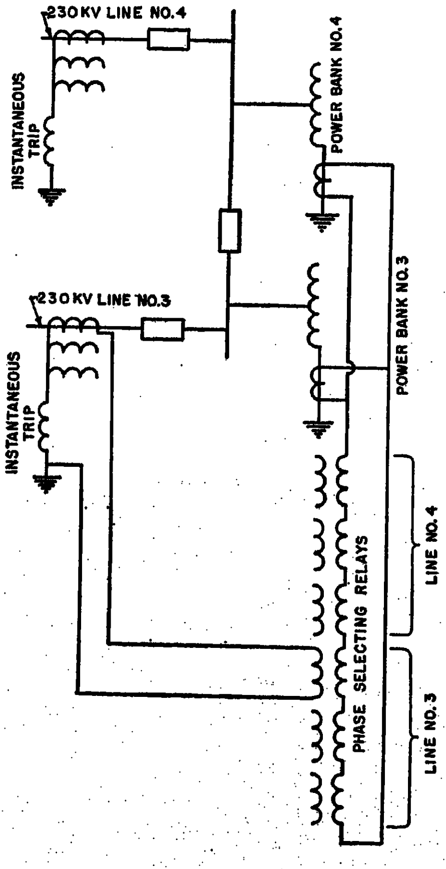
Figure 5. Schematic diagram of Spokane coupling capacitor selecting relay system

tion scheme indicated by test (4) in the section on coupling in which phases B and C are paralleled and pulsed for faults occurring on phase A.

A separate pulsing unit for each of the two 230-kv lines is provided for Spokane, one mounted on each dead end tower. Each unit includes a 10-kv power supply, a pulse originating relay and the associated gaps, three coupling capacitor selector relays, and coaxial fittings for terminating two coaxial cables connecting to the receiving equipment. The corresponding coaxials from these two pulsing units are connected in parallel so that only one receiving unit is required.

In addition to its primary function, the location of power line faults, it is felt that this equipment might prove to be a valuable tool in investigation of phenomena related to traveling waves and line attenuation on high-voltage transmission circuits. We also can see some possibilities for power line relaying with related devices. It also has been suggested that a continuously pulsing device of this kind using a high repetition rate might be used in the investigation of fault arc deionization time and lightning discharge duration.

Figure 4 (below). Schematic diagram of Spokane phase selecting relay system for single-line-to-ground faults



At the present date, we have made no detailed analysis of the data so far obtained on the BPA system on traveling waves and line attenuation.

References

1. A TRANSMISSION LINE FAULT LOCATOR USING FAULT-GENERATED SURGES, R. F. Stevens, T. W. Stringfield. *AIEE Transactions*, part II, volume 67, 1948, pages 1168–79.

2. THE "LINASCOPE"—AN ECHO-RANGING TYPE FAULT LOCATOR FOR HIGH-VOLTAGE LINES, J. R. Leslie, K. H. Kidd. *AIEE Transactions*, part II, volume 67, 1948, pages 1162–67.

3. THE LOCALIZATION OF FAULTS ON OVERHEAD LINES BY MEANS OF IMPULSE WAVES, S. Margaulies, P. Fourmarier. *Paper number 307*, International Conference on High Tension Electric Systems (Paris, France) 1948.

4. FAULT-FINDER, TYPE FF-1. Radar Engineers, Seattle, Wash.

5. RELAYS AND BREAKERS FOR HIGH-SPEED SINGLE-POLE TRIPPING AND RECLOSING, S. L. Goldsborough, A. W. Hill. *Electrical Engineering* (*AIEE Transactions*) volume 61, February 1942, pages 77–81.

Discussion

Y. J. Fujimura (Department of Water and Power, Los Angeles, Calif.): With the ever-increasing installation of high-voltage transmission lines of considerable length, often through territories not easily accessible, the necessity and the value of an improved method for quickly locating transient faults become apparent. The engineers of the Bonneville Power Administration have contributed a great deal toward the ultimate goal of an ideal fault locator[1] and this paper is welcomed as another step forward with possibilities of approaching the ideal requirements.

The Los Angeles Department of Water and Power is interested in some quick means of determining the location of transient faults which may occur on the 250-mile 230-kv Owens Gorge line to be constructed in the near future. Basically, we have considered schemes which depended upon one of the three sources of possible fault indication; by measurement of the fault current, by timing the traveling waves impressed on the line from an external source, and by timing the surge generated by the fault. None of the various schemes in its present state satisfies all the requirements of an ideal fault locator.

The paper indicates that this new method called type *C* is still in the experimental stage. Its two outstanding features are a new improved means of coupling to a live line and the possibility of obtaining records without the use of photographic films. We hope that they will succeed in developing an electronic counter and its auxiliaries which will operate only on the desired pulses. An electronic counter will eliminate film development and the necessity of skilled interpretation, and it will give more accurate indication with respect to distance.

What would happen if the lines were hit by a multiple lightning stroke? There is the possibility that subsequent strokes will generate surges which may interfere with the reflected pulse. It is true that the initial fault surge will have practically disappeared by waiting two cycles, but a lightning stroke may contribute surges which may occur several cycles after the initial stroke. If the authors can separate the reflected d-c pulse from any other pulses by some filter arrangement, the objection to interference will be eliminated.

Their experimental data from tests on the Bonneville Power Administration 230-kv Ross-Longview line are based only on solid faults on dead lines and on arcing faults on live lines. Appreciable reflection resulted by proper coupling to the faulted phases; however, on other types of faults involving high resistance to ground or between phases, the reflection may be too small to be identified on the scope or to operate the electronic counter.

Type *C* method also is limited to faults located more than a mile beyond the point of application of the d-c pulse. As the authors indicate, some ten microseconds are required for the output capacitor to discharge, which will block out the receiver for that period. Possibly a time delay circuit may overcome this difficulty.

REFERENCE

1. A TRANSMISSION LINE FAULT LOCATOR USING FAULT-GENERATED SURGES. R. F. Stevens, T. W. Stringfield. *AIEE Transactions*, volume 67, part II, 1948, pages 1168–79.

J. R. Leslie and **K. H. Kidd** (The Hydro-Electric Power Commission of Ontario, Toronto, Ontario, Canada): The pulse technique of fault location on power and telephone lines is receiving a great deal of attention and several groups are pioneering in the development of various instruments which operate on the same basic principle but which differ in detail. The Bonneville Power Administration is to be commended for the thorough manner in which it is investigating several of the possible methods.

The echo-ranging fault locator, utilizing a repetitive short duration d-c pulse, has become well established as an instrument for locating sustained faults.

The Hydro-Electric Power Commission of Ontario now has seven of these instruments located at strategic points in its operating area. These have been used with great success on a wide variety of telephone circuit faults. Operating experience on power lines has been limited, but the instruments have been of use in phasing in new circuits, checking for construction grounds, and in detecting faulty openers. Tests indicate that a variety of faults may be detected including phase-to-phase, phase-to-tower and phase-to-sky-wire short circuits, on open circuited phase conductors, and a phase conductor lying on or near the ground.

The Hydro-Electric Power Commission of Ontario also has in operation an experimental transient fault locator which is similar in principle to that described by Spaulding and Diemond but which differs in a number of details. The authors use a single short duration d-c pulse of about one microsecond duration, whereas The Hydro-Electric Power Commission has been experimenting with a short duration burst lasting about 40 microseconds and containing about ten cycles at a frequency of 250 kc per second. There are theoretical advantages to be claimed for either method and the choice must finally rest on actual operating experience. It is fortunate that the two techniques are being tried simultaneously under roughly similar circumstances, so that their relative merits should be clarified in the near future.

The short duration d-c pulse may be generated more simply than the wave burst and a large amplitude can be obtained much more readily. The wave burst equipment at present produces a voltage of about 500 volts peak to peak as compared to 10 kv utilized by Spaulding and Diemond, a ratio of about 26 decibels. A 1-microsecond pulse also is desirable in preference to a 40-microsecond pulse, in that the accuracy of distance determination is improved accordingly.

The d-c pulse has a much sharper front than has the burst and this is a desirable factor in that it produces a larger reflection from certain types of faults. Thus, a sharp fronted pulse is essential in the sustained fault locator in order to detect a phase conductor lying on high resistivity ground. However, in the case of an arcing fault on an energized line either shape is probably equally acceptable in this respect.

The attenuation along the line is greater for the shorter duration d-c pulse and it also may be more difficult to detect it in the presence of background noise. It is doubtful whether a 200-mile range can be attained with such a short duration pulse.

The main factor which may favor the burst is that the burst energy is confined to a narrower frequency spectrum than that of the d-c pulse. It is very desirable that the transient fault locator be operated without interference to or from carrier equipment on the same transmission line and possibly on the same coupling capacitor. Interference to the carrier receiver is not a problem since both the d-c pulse and wave burst are of such short duration. However, the carrier transmitter may interfere with the fault locator receiver particularly when they use the same capacitor. The present wave burst receiver utilizes a bandpass filter with a pass band of 100 kc centered on 250 kc and an attenuation peaking to 60 decibels at the carrier frequency of 172 kc and also rising to 30 decibels through the broadcast band. Careful attention must be paid to the transient response of the filter to assure short rise time and freedom from ringing.

Of course the simpler approach might be to shut off the carrier equipment for several milliseconds while the pulse is transmitted but it is feared that this would restrict the versatility of the instrument.

The two instruments also differ in a number of minor details. Spaulding and Diemond use a single pulse whereas The Hydro-Electric Power Commission uses a repetitive burst, sending out from five to eight bursts for a transient fault. The first method again gives simplicity and economy of equipment while the latter may be expected to give a clearer photographic record particularly in the presence of noise. The Hydro-Electric Power Commission equipment is used for observing sustained faults visually as well as transient faults photographically and hence the repetitive burst is necessary.

Both groups use a photographic recording technique at present and they appear to be equally anxious to get away from it. The electronic counter will be a most desirable advance but much experience will be needed before a suitable sensitivity is chosen. In some cases, it also may be necessary to render the counter insensitive to large fixed echoes from stations or junctions along the circuit under test.

The Hydro-Electric Power Commission group feel that the photographic technique is satisfactory so far as the operator's ability to interpret the record is concerned, especially as a photograph of the normal appearance of the circuit also is provided him. However, the time and trouble of processing the film is undesirable. Some thought is being given to magnetic tape or smoked chart recording, but the required writing speed may be too high. A magnetic tape record which the operator could play back onto the cathode-ray tube seems inviting.

L. R. Spaulding: In answer to Mr. Fujimura's first question, it is true that a lightning stroke, subsequent to the first one, which started a power arc might fall during the interval of operation of the type *C* fault locator and could cause interference. It would be more difficult to overcome this interference by use of a filter in this sytem than for a system using a radio-frequency or waveburst pulse. This is one thing in favor of the waveburst pulse.

Regarding high resistance faults, it does not appear likely that a high resistance fault will occur under transient or live line conditions on a 230-kv line. As a transient fault locator, the device depends upon a power arc for the reflecting discontinuity, and requires fault currents to accomplish the necessary relaying functions.

If a line fails to go back into service after an outage, a permanent fault may be located on the dead line by manually operating test switches provided for individual phase selection and pulsing. High resistance faults may be located either phase-to-ground or phase-to-phase by this means. The short duration d-c pulse is better for this purpose than a low frequency wave burst pulse.

As noted, the fault locator will not indicate faults in the first mile of line due to the time required to de-energize the transmitter and actuate the receiver. This is considered one of the limitations of the device. However, usually there are other visual indications for faults this close to a station. It was felt that the added complications necessary to overcome this deficiency were not warranted.

The comparison by Mr. Leslie and Mr. Kidd of the advantages and disadvantages of a d-c pulse versus a wave burst for echo ranging describes one difference between the Linascope and the type *C* fault locator. However, the greatest difference in the type *C* is in the use of a phase selecting relay system to connect the fault locator to the faulted phase. This results in a greatly increased reflection from the fault. It also provides a means of monitoring all phases on several lines with one set of equipment. When one conductor of a 3-phase line is pulsed and another conductor is involved in a fault, our tests show that the reflection from the point of fault is apt to be small or unusable, depending upon type of fault and the balance of the line constants.

In the discussion closure of Leslie and Kidds' paper,[1] they have indicated that their transient Linascope is connected to one phase only of a 220-kv bus through which four lines are paralleled. We believe our use of phase-selecting relays to be a distinct improvement in coupling means.

We recognize that the attenuation along the line is greater for a short duration d-c pulse than for a wave burst. However, the maximum line length to which it will be applied is about 136 miles. We do not plan to monitor more than one line section with the device, that is, station-to-station. It is at present temporarily installed at Spokane on the two 230-kv lines between Coulee and Spokane. Their lengths are 83 miles each. Very satisfactory reflections from the far end of these lines is obtained.

We have noted with interest certain advantages reported in favor of a radio-frequency, or wave burst pulse. Should our operating experience indicate the need, we shall give further consideration to this type of pulse.

REFERENCE

1. See reference 2 of the paper.

Electrical Design Features—Steam-Electric Generating Station P of the Pacific Gas and Electric Company

FRANCIS S. BENSON
MEMBER AIEE

A STEAM-ELECTRIC generating station known as Station P (Hunters Point) is located on the west shore of San Francisco Bay within the city of San Francisco, Calif. The original plant was completed during 1930, the installation consisting of one 35,000-kw 12-kv 3-phase 60-cycle condensing turbogenerator and a 3,500-kw turbogenerator (noncondensing and spinning type) for supplying power to the station auxiliary motors. The main generator supplied energy to an outdoor 12-kv bus from which feeders radiated to load areas.

The extension of Station P, completed during the spring of 1949, consists of the installation of two 100,000-kw 0.87-power-factor 13.8-kv condensing turbogenerators, and two 7,500-kw 0.7-power factor 4.16-kv condensing spinning turbogenerators installed in an extension of the turbine room. The new 100,000-kw generators supply electric energy at 13.8 kv to single-phase step-up transformers, the high-voltage terminals of which are directly connected to the 115-kv outdoor ring bus.

The electric energy leaves the 115-kv bus over three underground high-voltage pipe-type high-pressure nitrogen-gas-filled cables, each cable having a rating of 100,000 kva at 120 kv. The insulated conductors in the pipe are maintained at a pressure of 200 pounds per square inch. Two pipe-type cables transmit energy to Station H (Martin Station), and one cable supplies energy to Mission Substation, located in the downtown area of San Francisco.

The terminal position of each high-voltage underground cable at Station P is equipped with a grounding switch, in addition to the usual circuit disconnecting switch. This grounding switch is used to drain the capacitance charge off the cable after it has been disconnected from any source of energy.

115-Kv Ring Bus

The high-voltage (115-kv) ring bus is built outdoors; it consists of a number of steel structures equipped with insulators, disconnecting switches, and oil circuit breakers. Figure 1 is a single-line diagram of the ring bus. Each oil circuit breaker has a current rating of 1,200 amperes at 115 kv and an interrupting rating of 3,500,000 kva. Each circuit breaker also is equipped with six multitap bushing current transformers and two or four capacitor-type potential devices. No 115-kv potential transformers are used.

The ring bus is supplied with energy from two main transformer banks, each having a capacity of 111,750 kva and each consisting of three 37,250-kva 13.8/115-kv single-phase transformers which are outdoor, forced-air rated. The transformer banks are connected delta-Y, and the high-voltage winding neutral is solidly grounded. The transformer installation is equipped with transfer disconnecting switches on both the high- and low-voltage sides for electrically connecting a spare single-phase transformer in the position of any other transformer.

One 50,000 kva 3-phase transformer with $7^1/_2$ per cent plus or minus tap-changing-under-load equipment is installed to interchange energy between the 110-kv and 12-kv systems. This is an outdoor forced-air-cooled transformer rated 110/12 kv, connected Y-Y with a tertiary winding connected in delta.

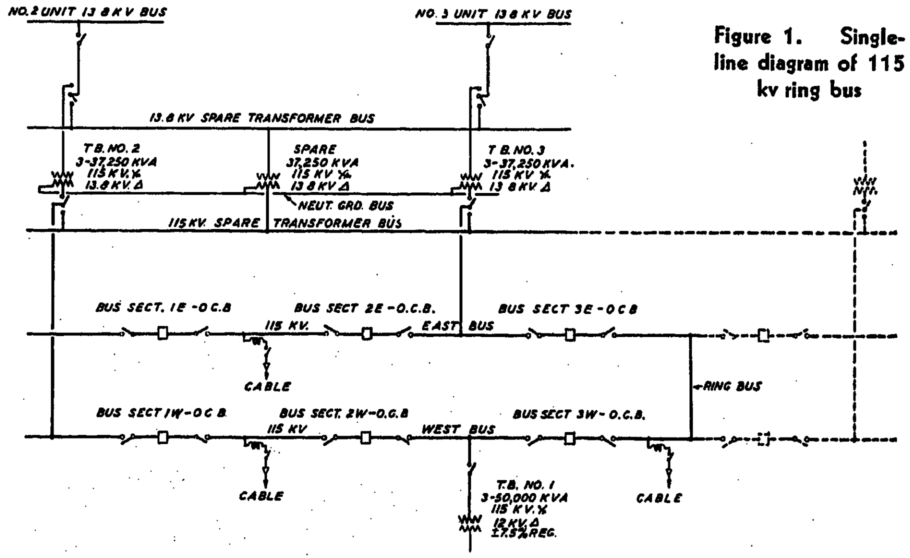

NO. 2 UNIT 13.8 KV BUS
NO. 3 UNIT 13.8 KV BUS
13.8 KV SPARE TRANSFORMER BUS
T.B. NO. 2 3-37,250 KVA 115 KV Y 13.8 KV Δ
SPARE 37,250 KVA 115 KV Y 13.8 KV Δ
T.B. NO. 3 3-37,250 KVA 115 KV Y 13.8 KV Δ
NEUT. GRD. BUS
115 KV SPARE TRANSFORMER BUS
BUS SECT. 1E-O.C.B.
BUS SECT 2E-O.C.B.
BUS SECT 3E-O.C.B.
115 KV EAST BUS
CABLE
RING BUS
BUS SECT 1W-O.C.B.
BUS SECT. 2W-O.C.B.
BUS SECT 3W-O.C.B.
115 KV WEST BUS
CABLE
T.B. NO. 1 3-50,000 KVA 115 KV Y 12 KV Δ 13.8 REG.
CABLE

Figure 1. Single-line diagram of 115 kv ring bus

Paper 49-208, recommended by the AIEE Power Generation Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Pacific General Meeting, San Francisco, Calif., August 23–26, 1949. Manuscript submitted May 23, 1949; made available for printing June 29, 1949.

FRANCIS S. BENSON is with the Department of Engineering, Division of Electrical Distribution, Pacific Gas and Electric Company, San Francisco, Calif.

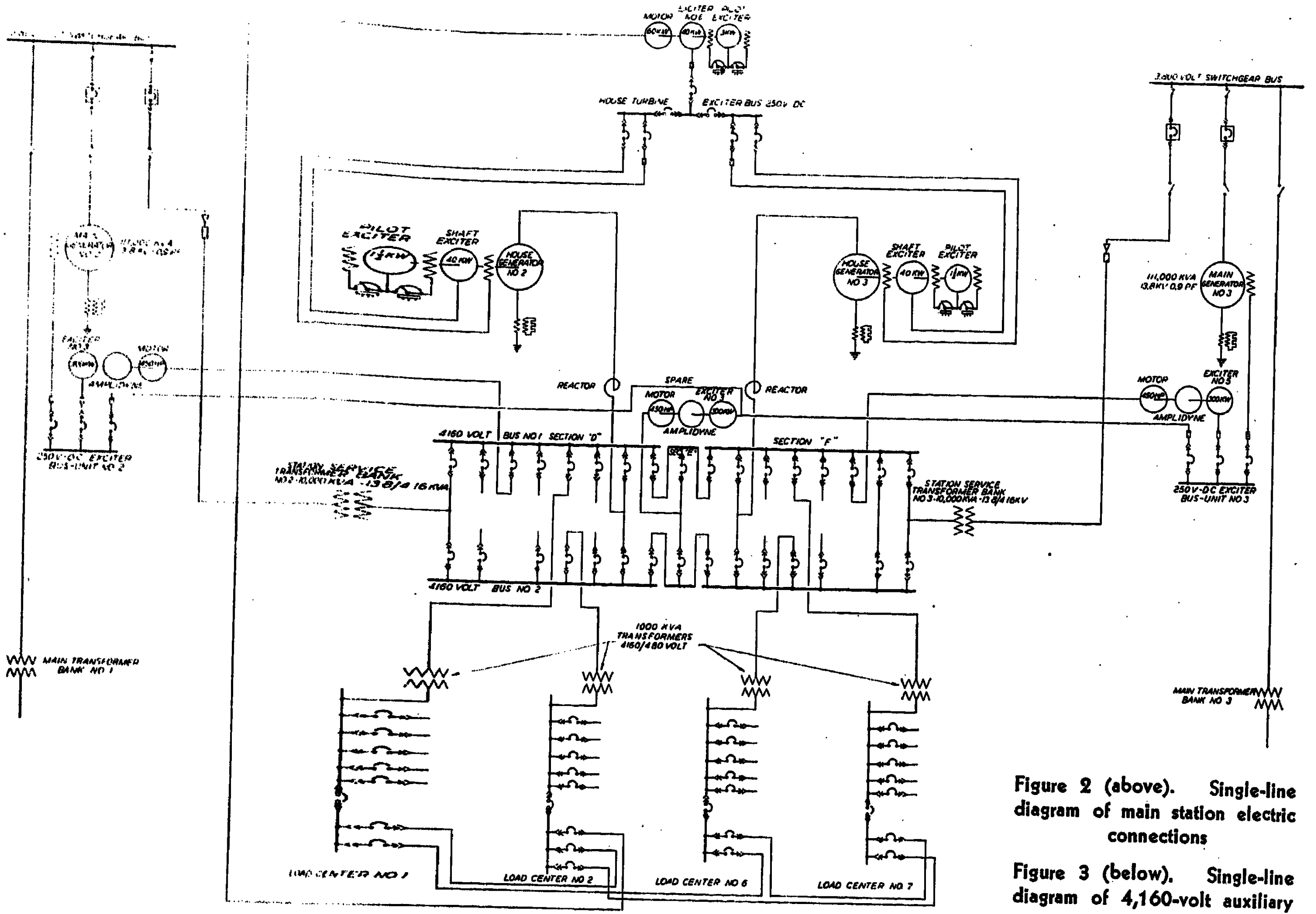
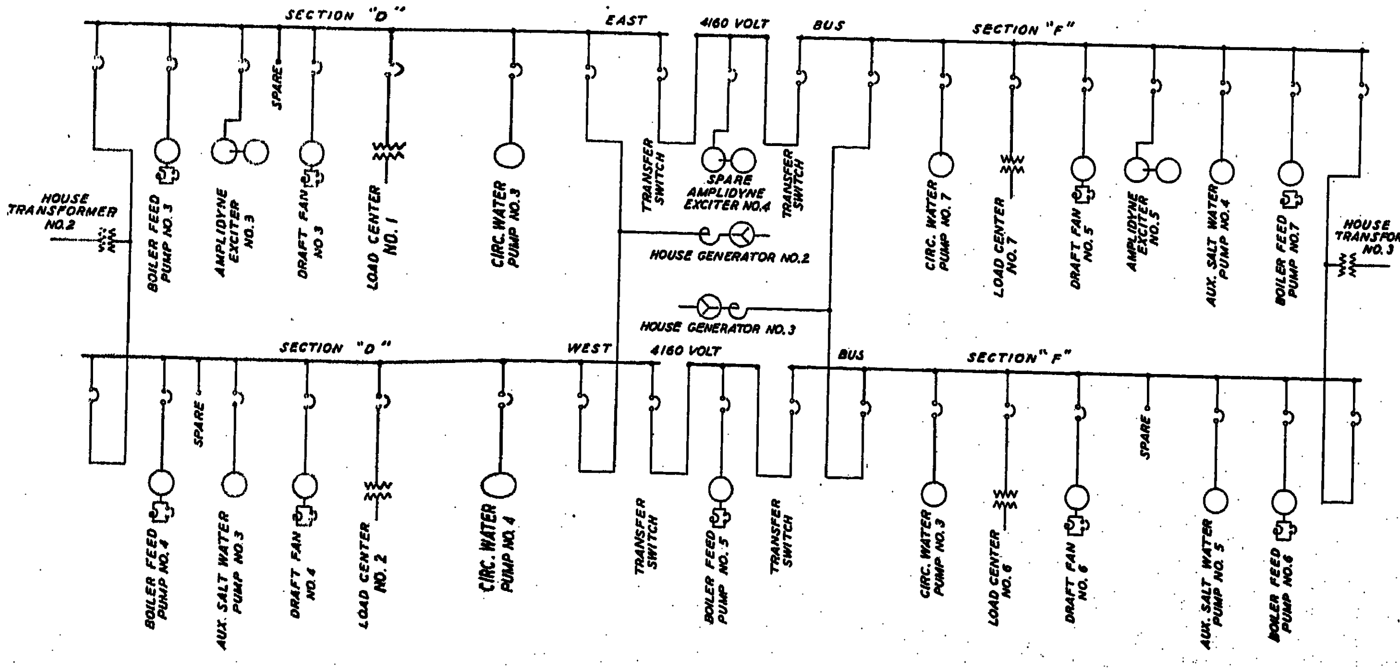


Figure 2 (above). Single-line diagram of main station electric connections

Figure 3 (below). Single-line diagram of 4,160-volt auxiliary power bus



Main 13.8-Kv Generators

The main generators are a single-winding type; they are hydrogen cooled, having a rating of 115,000 kva at 15 pounds per square inch hydrogen. Full-rated current is 4,820 amperes at 13,800 volts. Automatic voltage regulation is provided using amplidyne voltage regulators for each generator. The field voltage is 250 direct volts, supplied by a 1,200-rpm 450-horsepower motor-driven exciter of 300-kw capacity, direct connected to a 7.5-kw amplidyne pilot exciter. A complete spare motor exciter

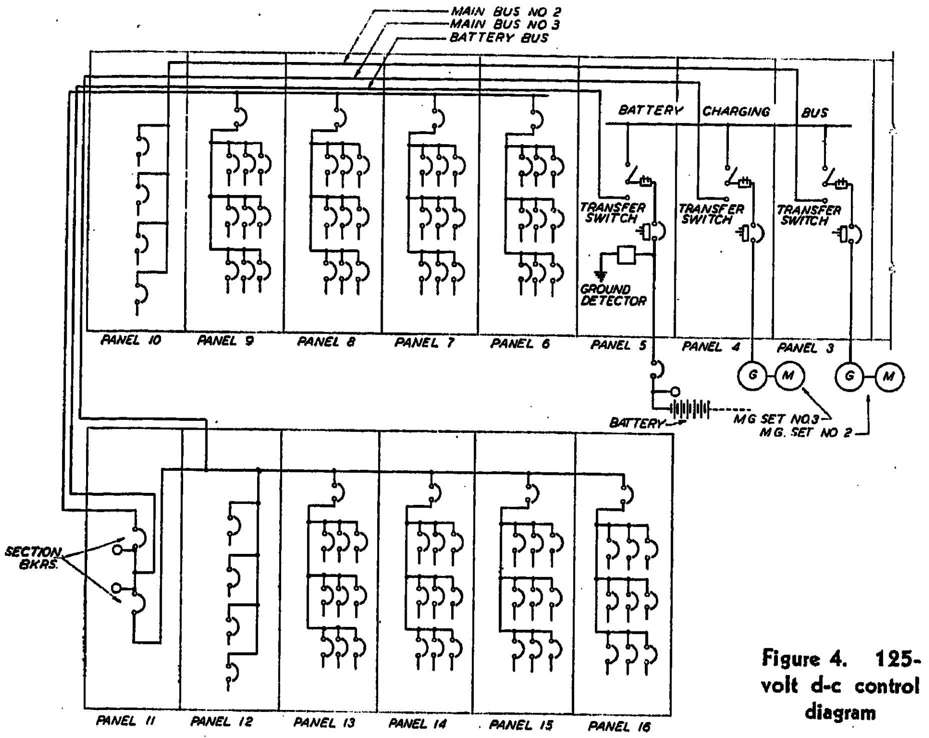


Figure 4. 125-volt d-c control diagram

including regulator is provided for the generators in case one of the generator motor exciters is out of service. Each exciter unit is equipped with a flywheel to maintain full exciter output for a short period of time if motor power fails. The voltage regulation system is designed for an excitation response of 0.5.

The terminals of the main generators are connected to the step-up transformers through a 13.8-kv 5,000-ampere isolated-phase bus, enclosed in metal ducts. A 5,000-ampere air-blast circuit breaker having an interrupting duty of 2,500,000 kva is installed in the 13.8-kv bus between the step-up transformer bank and the generator terminals. A single-line diagram, Figure 2, shows the main station electric connections. A 10,000-kva station auxiliary power transformer bank is connected to the 13.8-kv bus through a 2,000-ampere air-blast circuit breaker having an interrupting rating of 1,500,000 kva. These circuit breakers operate with an air pressure of 250 pounds per square inch. The 13.8-kv bus connections are such that the station auxiliary power transformers may receive power from the 115-kv bus when the main generator is out of service.

The neutral point of each main generator winding is grounded by elevated neutral equipment consisting of a 50-kva 7,620–120/240-volt single-phase transformer, the 240-volt secondary winding of which is shunted with a 50-kw resistor. An alarm relay and a double-scale voltmeter, one scale indicating 1/10 the value of the other scale, are connected across the 120-volt coil of the transformer secondary.

4,160-Volt Auxiliary Station Power System

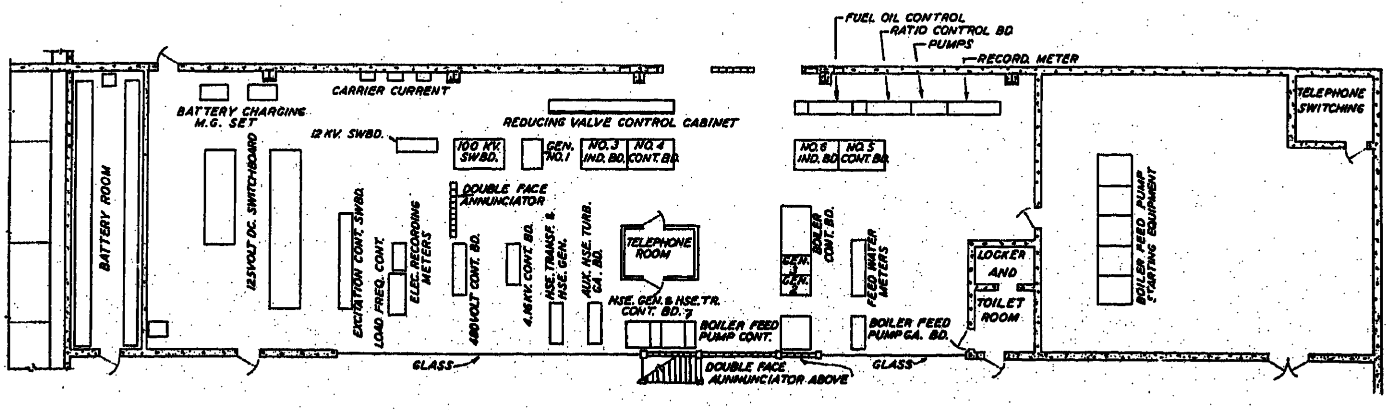
The 4,160-volt auxiliary station power supply is made up of two ring busses connected together by circuit breakers. Refer to Figure 3 for the single-line diagram of connections. One 7,500-kw house turbine and one 10,000-kva house bank is connected to each ring bus. Each ring bus is operated normally as a separate auxiliary power system, but the busses may be connected together for emergency operation. The house generator and the house bank each have sufficient capacity to supply the auxiliary power load connected to each ring bus section; one acts as a standby source of power to the other. The house turbines have a rating of 7,500 kw, 0.7 power factor, 10,714 kva, 4,160 volt, three phase, 60 cycles; each house bank has a rating of 10,000 kva, 13.8/4.16 kv, three phase, self-cooled and 13.333 kva, forced-air-cooled; the windings are connected delta-delta. The transformers are single-phase units; their high-voltage and low-voltage terminal connections are arranged with metal links to allow any two single-phase transformers to be quickly reconnected as an open-delta bank having a capacity of 7,600 kva, three phase, forced-air-cooled.

Each house generator has automatic voltage regulation and is equipped with a shaft exciter and pilot exciter, using 250 direct volts as a field voltage. The excitation system has a voltage response of 0.5. A 2,000-ampere 5-per cent metal-enclosed 5-kv indoor current limiting reactor is installed in the house generator lead to the ring bus to reduce the possible short-circuit current on each ring bus.

The 4,160-volt ring bus is made up of 5-kv metal-enclosed lift-up-type electrically-operated air circuit breakers, mounted in steel cells. The circuit breakers have a rating of 1,200 amperes for motors and 480-volt transformers, and 2,000 amperes for the house generators, house transformers, and bus section circuit breakers. They have an interrupting duty of 250,000 kva at 5.0 kv.

The ring busses are connected together through two bus section circuit breakers in series. A connection to the spare exciter motor is taken off the center posi-

Figure 5. Central control room arrangement



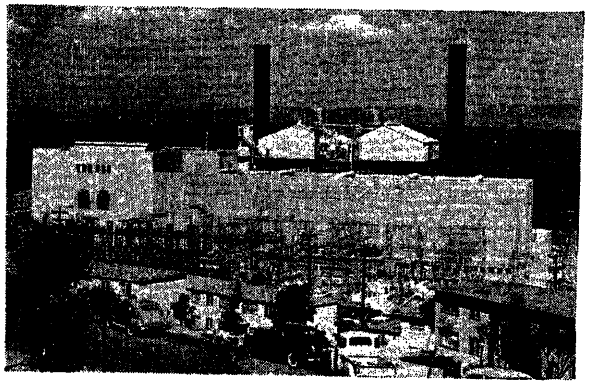


Figure 6 (left). Outdoor arrangement of 115-kv bus

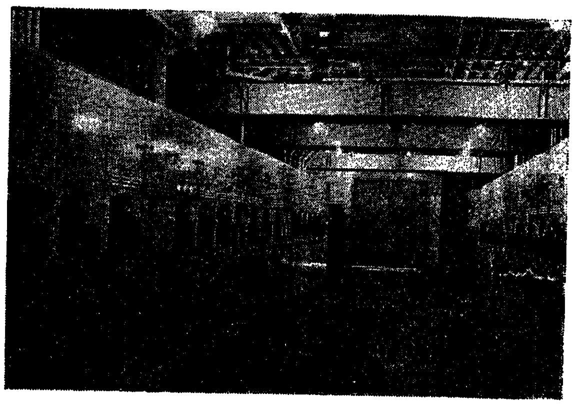


Figure 8 (right). 4,160-volt indoor auxiliary power bus

tion between two bus section circuit breakers, while a connection to boiler feed pump number 5 is taken off the center connection between the other two bus section circuit breakers. Figure 3 indicates this connection.

The following station auxiliary power load is supplied from the 4,160-volt busses:

- Four 1,000-kva 3-phase oil-immersed self-cooled 4,160/480-volt transformer banks.
- Four 400-horsepower circulating-pump motors (Main generating units).
- Three 300-horsepower auxiliary salt-water pump motors (Auxiliary cooling service).
- Four 2,000-horsepower boiler fan motors (One per boiler).
- Five 2,000-horsepower boiler feed-pump motors.
- Three 450-horsepower motor-driven exciters.

The small motors, 300, 400, and 450 horsepower, are started across the line at full voltage. The 2,000-horsepower motors are equipped with starting reactors and a starting circuit breaker connected in the neutral leads of the motor; six leads are brought out of the motor enclosure. These motors start at a reduced line voltage of 50 per cent.

The 4,160-volt auxiliary power supply is delta-connected; however, the house generator windings are Y-connected. The neutral point of the generators is grounded through an elevated neutral connection consisting of a 10-kva 2,400-120/240-volt distribution transformer having a 10-kw resistor connected across the 240-volt secondary winding. A double-scale voltmeter and an alarm relay are connected to the 120-volt coil of the transformer secondary winding.

480-Volt Auxiliary Power System

The 480-volt auxiliary power system is made up of 12 load centers placed at centers of motor concentrations about the plant; they are connected to each other by tie cables. The load centers are supplied with energy from four 1,000-kva 3-phase oil-immersed self-cooled 4,160/480-volt delta-connected transformers. This auxiliary power system consists of load centers which are interconnected as a sectionalized radial system. Each load center is metal enclosed, and is equipped with drawout circuit breakers; each load center bus is sectionalized into two or three sections connected together by bus-section circuit breakers. All air circuit breakers are 3-pole, electrically operated, equipped with thermomagnetic overcurrent protection; they have an interrupting rating of 25,000 amperes for motor and lighting transformer circuits and 50,000 amperes for main load-center tie circuits and bus-section circuit breakers.

Motors

All motors in sizes from 1 to 200 horsepower are 480 volts, and are splash-proof or drip-proof when installed indoors. Motors installed outdoors are totally enclosed, fan-cooled. Machine-shop motors only are the conventional open type. All 480-volt induction motors are full-voltage line-start type. All motor windings are required to have boiler-room or powerhouse treatment.

The 2,000-horsepower 4,160-volt 3-phase motors are totally enclosed, equipped with built-in water-to-air heat exchangers, which are mounted on top of the motor. A moisture detector relay is installed in each motor to indicate excessive moisture within the enclosure, which may be caused by a small leak in the heat exchanger tubes. These motors are equipped with starting reactors and starting circuit breakers connected in the neutral leads.

All motors are induction type, except the fuel-oil pump motors which are wound-rotor type, and are equipped with resistors to give full pump output at three speeds: 1,530, 900, and 450 rpm.

Lighting

Lighting circuits are 3-phase 4-wire supplied with energy from 3-phase indoor dry-type transformers, rated at 25 kva and $37\frac{1}{2}$ kva, and are connected 480-120/208 volts, delta-Y. The neutral point of the secondary winding of the transformers is solidly grounded. All lighting circuits are connected between phase wires and neutral; they are pro-

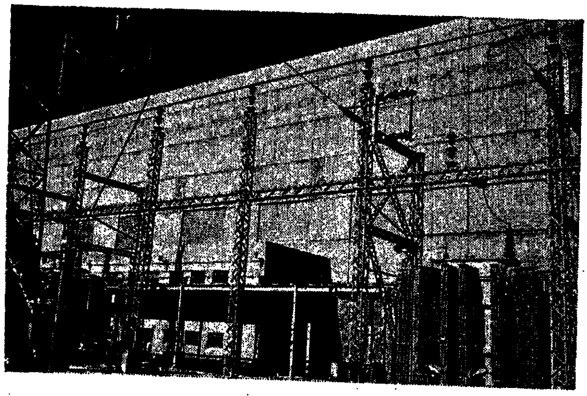


Figure 7 (left). 13.8 / 115 - kv step-up transformer arrangement

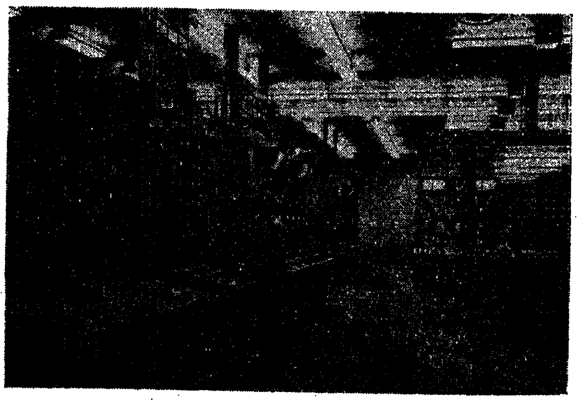


Figure 9 (right). Central control room arrangement

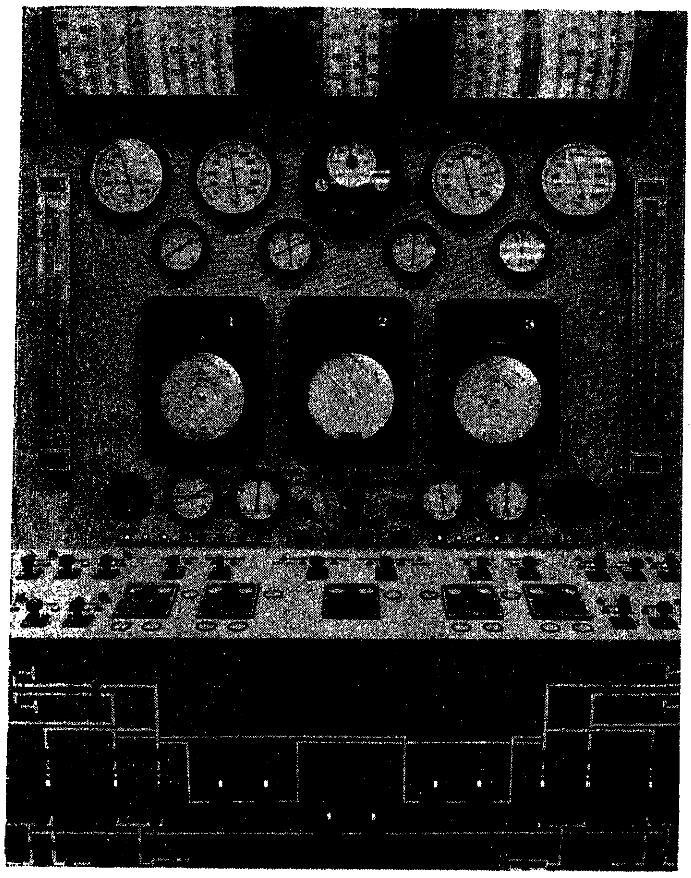


Figure 10. Master control board

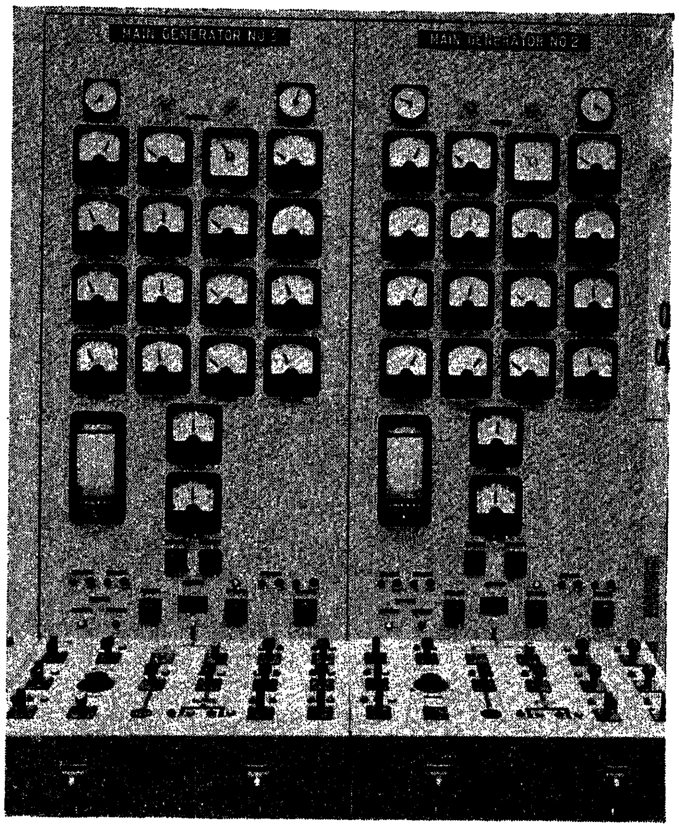


Figure 11. Main generator control panels

tected by single-pole 250-volt a-c thermal circuit breakers. The lighting neutral is carried throughout the plant as an insulated conductor.

The switchboard control room and offices are lighted with surface-mounted fluorescent lighting units enclosed in prismatic glass fixtures, which project some light against the ceiling, eliminating a marked lighting contrast between walls and ceiling. All other lights within the plant area are Mazda lights mounted in prismatic glass fixtures.

An emergency d-c lighting system is installed; certain lighting circuits normally operating on alternating current and using Mazda lamps are switched automatically over to the 125-volt d-c battery system, in case the a-c power supply fails, by double-pole double-throw electrically-operated industrial type contactors. These circuits are returned automatically to the a-c power supply when a-c voltage returns to normal. Small motors, less than 1-horsepower single-phase and 3-phase, are connected to the 208-volt 3-phase power supply.

125-Volt D-C Battery and Control Service

The 125-volt d-c control power system is supplied with power from a 60-cell 640-ampere-hour (8-hour rate) chloride accumulator type storage battery. Two motor-generator battery charging sets are installed; each generator is a diverter pole type with 25-kw capacity, two wire, whose maximum ceiling voltage is 140 direct volts.

The 125-volt d-c switchgear is metal enclosed with sectionalized busses for the control circuits so that all control power is not lost if one bus fails. The battery and charging-set bus connections are such that either battery-charging motor-generator set can be used to trickle charge or gas the battery, while the other set is connected to the control bus.

All 125-volt d-c circuits are protected with thermal circuit breakers or fuses of proper rating. Refer to Figure 4.

Circuit and Equipment Protective Relays

A modern steam plant requires many auxiliary relays in addition to those used for circuit and equipment protection. Each of the three main 110-kv underground cable circuits is protected by three high-speed directional overcurrent relays, one high-speed directional overcurrent ground relay, and pilot carrier-current ground relays.

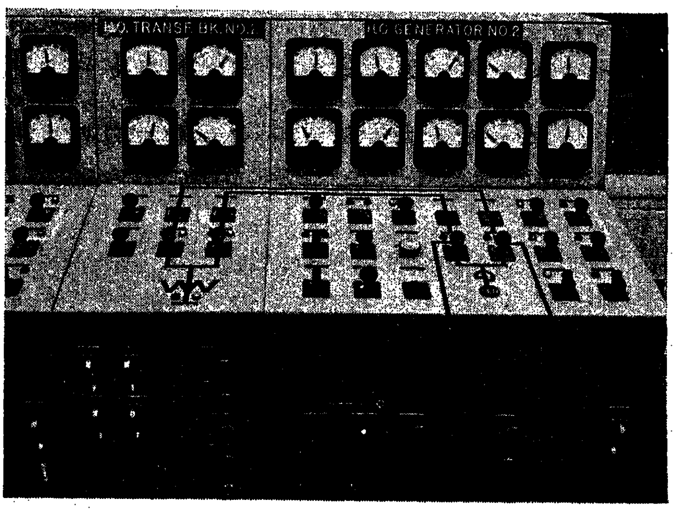


Figure 12. House generator and house transformer board

The main step-up transformer banks, 13.8/115-kv (121,750 kva) are protected with differential relays (10-cycle type) and 4- to 15-ampere overcurrent standard time, inverse relays for back-up protection. The 115/12-kv step-down 3-phase transformer bank (50,000 kva) is protected by differential relays and 4- to 15-ampere overcurrent standard-time inverse relays. The 115-kv outdoor bus is a ring bus; the 115-kv cable and transformer bank protection overlap each other.

The main generators are protected by differential relays (1-cycle type).

The house transformers are protected by differential relays (10-cycle) and 4- to 15-ampere overcurrent standard-time inverse relays for back-up protection. The house generator is protected by differential relays (1-cycle type).

The 4,160-volt ring bus is protected by two sets of bus differential relays, one set for each half of the ring bus overlapping the other; therefore only 1/2 of the ring bus will be lost if a short circuit occurs on the other half of the ring bus. All 4.16-kv motors are protected with 2- to 8-ampere overcurrent long-time inverse relays, and equipped with instantaneous overcurrent elements having a rating of 20 to 80 amperes.

If the 4,160-volt motor protection fails and a short circuit is maintained on the motor feeder because its circuit breaker fails mechanically, certain overcurrent relays (4 to 15 amperes), acting as short-circuit current relays, will close their contacts and trip the house bank, 4.16-kv circuit breakers and one generator circuit breaker supplying power to that half of the ring bus which is in trouble. The other generator circuit breaker remains closed. Therefore, only 1/2 of one ring bus is de-energized when a circuit breaker fails to open due to mechanical trouble.

During normal operation, the 4.16-kv house transformer bank and house generator operate in parallel with the high-voltage (115-kv) transmission system. In case of severe transmission system disturbances, the 4.16-kv auxiliary power bus will be separated from the main 115-kv system and operate as a separate power supply for the station auxiliaries, which then will be supplied with electric energy by the house turbines. The auxiliary bus separates from the transmission system automatically when one of three protective relays close their contacts. The first relay is a frequency relay, adjustable type, set to close its contacts when the frequency drops to 58 cycles. The second relay is a directional over-current type, which closes its contacts when a predetermined value of kilovolts-amperes flows from the 4,160-volt bus to the 13.8-kv bus. The third protective relay is an out-of-step relay, which closes its contacts after the third swing during system disturbances; the function of each relay is to separate the auxiliary bus from the transmission system during abnormal disturbances.

Fire Protection

A modern generating plant requires fire-fighting equipment especially adapted to the fire hazards at hand. In general, the plant fire protection equipment consists of four separate forms of suppression equipment; they are: fixed CO_2 gas, mechanical foam equipment, a standby hydrant and hose system using fixed fog nozzles, and portable first-aid fire equipment.

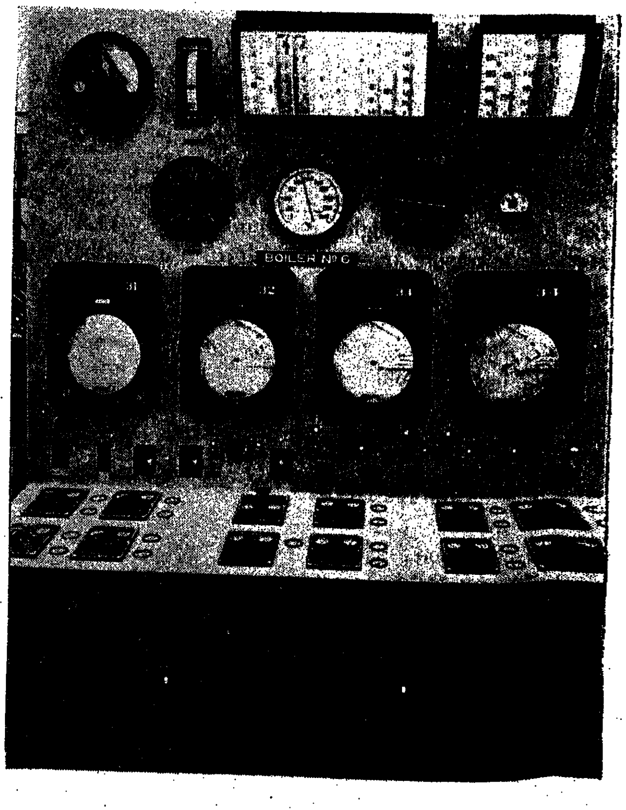
The portable first-aid equipment consists of 1-quart and 1-gallon carbon tetrachloride extinguishers, stored-pressure type using nitrogen gas at 100 pounds per square inch to project the liquid from the extinguishers, and $2\frac{1}{2}$-gallon foam extinguishers located near turbine control-oil storage tanks and around the fuel-pump area.

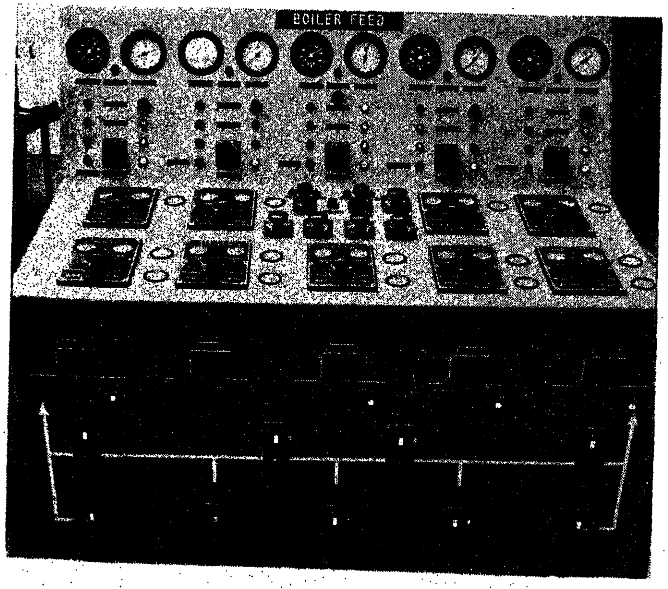
The lubricating-oil and control-oil tanks for the main turbines and house turbines are protected by a fixed carbon-dioxide system, consisting of piping CO_2 gas tanks, and rate-of-rise thermostats. The thermostats are mounted within the tanks near the top; when their contacts close, CO_2 gas is discharged into the oil tanks in sufficient quantity to maintain a concentration of gas of not less than 25 per cent for 30 minutes.

The main generators are hydrogen-cooled; therefore, no fire protection is required. The house generators are air-cooled and are protected by a fixed system of carbon-dioxide gas consisting of piping, nozzles, CO_2 tanks, a timing device to discharge the gas at regular intervals to maintain a 25 per cent concentration of gas within the generator casings for a time period of not less than 30 minutes. Rate-of-rise thermostats installed within the

Figure 13 (below). Boiler feed-pump motor control board

Figure 14 (right). Boiler number 6 control board





generator casing close their contacts when excessive temperature occurs.

A fixed mechanical foam system protects the fuel-oil tanks. The foam is projected on top of the floating roof of each tank through pipes and nozzles installed for that purpose. Additional foam nozzles will project foam around the outside of the tanks within the area surrounded by the levee. The fuel-oil pumps installed under the boilers also are protected by mechanical foam projected from hose lines which are located in that area.

A hydrant and hose system is installed both indoors and outdoors to provide general water-fog fire protection for all installations, including the 115-kv switchyard, 12-kv switchyard, main transformers, and indoor equipment. Each hydrant is equipped with 100 feet of $1^1/_2$-inch rubber-lined cotton-jacketed hose having a fixed-fog nozzle connected thereto. Water pressure at 100 pounds per square inch is maintained by two 1,000-gallon-per-minute pumps, one pump motor-driven and the other steam-turbine driven. The motor-driven pump is started by push button, from the control room.

The warehouse is protected by an automatic sprinkler system, and a flow device mounted in the main water-pipe connection to the warehouse closes an alarm contact if water flows and also starts the fire pump to increase the water pressure.

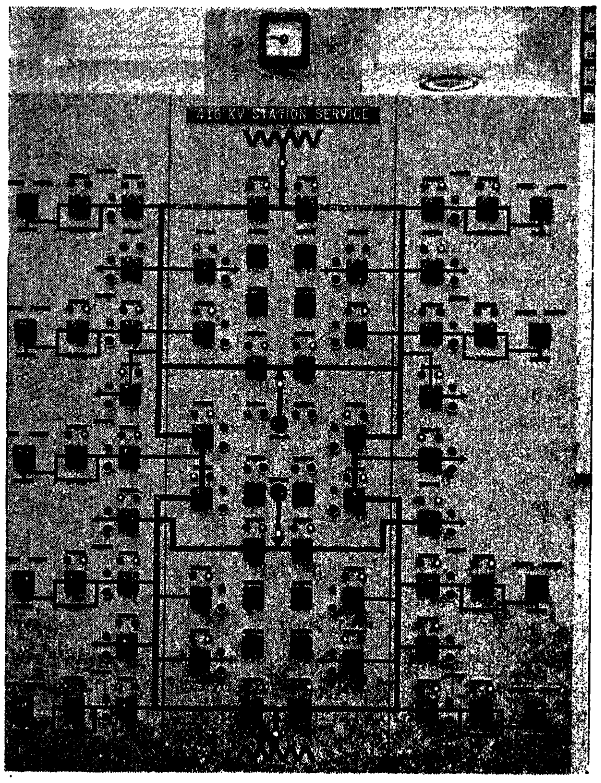


Figure 15. 4,160-volt remote control board

Central Control Area

The general design of this station is such that it is built around a central control area. The boilers, turbines, boiler feed pumps, fuel pumps, 110-kv feeders, and auxiliary power busses are all controlled from this area. See Figure 5. The central control area is not completely soundproofed but is open to both the boiler firing aisle and the turbine bay, as the operators become sensitive to changes of pitch or intensity of normal generating plant noise and often become aware something is amiss and requires attention before the instruments and gauges give a substantial change in indication. A large glass-enclosed telephone room is placed at the center of the control area near the vitally important switchboards, so that the operator can read the instruments and telephone the information to necessary offices. Vital control boards, such as the master panel (which controls all the boilers), main generator panels, house-generator and house-bank panels, and boiler feed-water control panels are arranged around three sides of an open square with the telephone room in the center. This is a planned arrangement to provide operators with a direct view of the important equipment, boilers, turbines, and boiler feed pumps as well as the control panels; it also provides quick access to the equipment when supervising the automatic functions of the plant. The control panels themselves are designed so that operation of each part of the equipment is logical and is oriented correctly with respect to its actual location about the plant. Instruments, gauges, control handles, and lights are deliberately arranged in unsymmetrical patterns to allow quick visual identification by the operating staff. The arrangement of the master panel is such that the boilers, circulating water pumps, and condensate pumps are controlled here.

A generating plant using a central control scheme requires the installation of automatic control equipment such as combustion control, boiler feed-water control, fuel control, and automatic transfer equipment for circulating water pumps, condensate pumps, and many others.

Motor-driven pumps also are arranged for automatic transfer to standby pumps fed from other sources in case of loss of power, motor failure, too high or too low a pressure, too high or too low a liquid level, or excessive temperature. Such transfer operations occur automatically when the initiating contacts close, without requiring attention or supervision from the station operators.

Annunciators and Alarm Circuits

A centrally controlled plant requires an extensive alarm system, as the many automatic devices must report their operation to the central control area. Therefore a complete annunciator and alarm system is installed, giving both audible and visual indication. The annunciator boxes are large, giving good visibility from a distance of 30 to 40 feet; their electric circuits are designed so that the audible alarm (horn or bell) may be shut off while the annunciator box remains lighted; the horn or bell are then available to announce other operations. This plant has many transfer devices which also require annunciator boxes whose function is to indicate that a transfer has been initiated and has been completed swiftly and accurately. The operation of some devices requires immediate attention, while others indicate a routine operation; therefore, some annunciator boxes are equipped with colored glass to call attention to the degree of urgency of the alarm. An annunciator box with red glass indicates that immediate attention must be given to the device initiating this alarm. Annunicator boxes with white glass indicate an operation of lesser importance than those indicated by red glass, and attention may be deferred until the general emergency period is over.

Annunciator boxes with yellow glass indicate that certain standby operations or transfers have been completed; these are called to the operators' attention for their information.

A semiautomatically controlled steam plant cannot function reliably unless the personnel understand and are familiar with abnormal operation of the many varied devices. The annunciator system is designed so that each box indicates the function of a separate device which may be the result of a routine or abnormal operation. This serves as an effective method of continually reminding the personnel of the many and diverse plant functions that must be supervised and controlled.

Communication Equipment

An extensive communicating system is required for large steam plants. It is used in conjunction with the annunciator system for plant maintenance and operation.

Station P has a plant system and connections to two outside telephone systems. One installation of telephones is connected to the Pacific Gas and Electric Company's private telephone system, which has a network covering the central and northern part of California. This system is used for both routine business and dispatching requirements.

The company's private system also is interconnected with the plant interphone system. The latter system is automatic, dial type, selective ringing, selective talking, and capable of handling five conversations simultaneously.

The other outside telephone system is that of the local telephone company; four trunks have been installed to handle routine business in San Francisco.

A code call system is installed in the plant, in conjunction with the private telephone system, for calling station personnel to the nearest telephone.

A 2-wire telephone circuit has been installed within the station for maintenance purposes. Many polarized 2-wire outlets have been installed throughout the plant, from the top of the boilers to the remote areas on the first floor of the turbine room. Maintenance personnel plug their telephone hand sets into convenient outlets and thus have a common communication circuit for their use.

Conclusion

The electrical control of this plant is arranged to co-ordinate the general plant design of automatic control of major steam generating and fuel equipment and automatic transfer of many other motor-driven devices. This requires personnel who are adequately trained and resourceful, as they must correctly interpret the function of a device initiating an alarm in relation to other devices about the plant. The central control area design necessitates use of the many advantages of automatic control for equipment functions, and the use of an extensive annunciator system to acquaint the operators and keep them informed of the normal equipment functions, as well as calling attention to abnormal or faulty operation.

The electric installation was designed and installed by the Stone and Webster Engineering Company for the Pacific Gas and Electric Company.

Discussion

V. F. Estcourt (Pacific Gas and Electric Company, San Francisco, Calif.): The author is to be congratulated for having prepared a very comprehensive description of the electrical design features of the Pacific Gas and Electric Company's Station P. I have been asked to comment on this paper with particular reference to some of the automatic control features, and the centralization of a great many of these controls in one control room.

Before discussing the Station P control layout in more detail, it is essential to understand the fundamental operating objectives underlying this type of design. Due to the fact that the Pacific Gas and Electric Company's generating facilities are divided about equally between steam and hydro, seasonal variations in rainfall and water storage make it necessary to operate the steam plants over very wide load ranges, that is, from approximately 10 to 100% of their rated capacity. They also must be prepared to pick up large blocks of load in a matter of seconds in the event of any interruption to the incoming power generated from hydroelectric sources and delivered over long-distance transmission lines.

The first major experience of the Pacific Gas and Electric Company with the application of full automatic decentralized control for steam plant operation dates back to the year 1934 in connection with the 120,000-kw 1,400-pound, 750 degree Fahrenheit—750 degree Fahrenheit reheat Station A in San Francisco. As a result of several years of development and experimental work, this plant operated successfully on full automatic control under operating conditions ranging from base load to wide load range operation at rates of load change in excess of 1,500 kw per second with either natural gas or fuel oil firing. The first plant with a large amount of centralized control was installed in 1940 and subsequent installations such as Station P have followed the same general pattern.

Automatic control, as applied to steam plant operation, is intended to accomplish the following basic objectives:

1. The establishment of uniform operating patterns by incorporating these into the control devices, so that not only is maximum operating efficiency and reliability obtained, but individual operators are prevented from introducing variations from the optimum pattern which has been determined by careful tests.

2. Reduction of psychological stress upon the operator to a minimum so that his mind is free to concentrate upon the overall picture, with particular reference to trouble conditions, rather than have his attention diverted by the need for handling manually numerous operating adjustments and load changes.

3. The final objective is to combine the aforementioned advantages of automatic control into a layout which will permit the centralization of a large proportion of these controls, instruments, and alarm signals in one main control room. By means of carefully planned centralization, the advantages of full automatic control are all the more accentuated and the net result is more reliable operation, less operating errors, and a smaller operating crew, with resultant lower operating costs. In fact, with a decentralized control job, it is doubtful whether as much automatic control can be justified for the reason that more operators are required and their respective duties and responsibilities are correspondingly less. However, the problem of communication and general co-ordination of operating activities is thereby greatly aggravated, particularly during trouble conditions.

As has been pointed out by the author, the major equipment at Station P, which includes the boilers, turbines, and boiler feed pumps, is grouped around the control room; the firing aisle, control room, and turbine deck are at the same elevation, thereby providing the operators in the control room with direct vision of and close access to these major components of the plant.

This has the effect of keeping the operators inherently centralized in one location with a relatively short-radius area to patrol for the purpose of inspecting and handling such equipment. Much of the advantage of a centralized control room will be lost if consideration is not given to the orientation of major equipment with respect to this central control room, for the reason that it will require additional operators stationed around the important items of equipment due to their inaccessibility from the control room. If it is necessary to keep a number of operators in decentralized locations, then it is difficult to justify a highly centralized control layout.

The author has referred to the automatic starting of standby pumps. This has to do particularly with various items of auxiliary equipment located at plant elevations other than the control room, and which are therefore inaccessible to the control operators in the control room. Two or three shift operators are all that are required to patrol and inspect these minor auxiliaries which are located on the lower floor of the turbine room and boiler room and in the yard outside the main building. This would not be possible without a very reliable system for the automatic starting of standby equipment plus an adequate alarm system to warn the operators in the control room as to certain types of operating trouble. Our practice is to use two initiating sources for automatically starting any piece of standby equipment. These sources are usually undervoltage at the motor terminals plus pump discharge pressure or tank level as the case may be.

Bringing most of the controls into a central control room, plus other necessary related features, probably has added approximately 50 per cent to the total investment cost of such a control layout as compared with a completely decentralized job. This additional investment cost is more than offset by the saving in shift operating labor and greater operating reliability. Furthermore, as a result of our operating experience at the Kern Steam Plant and also at Station P, it is believed that we can make an appreciable reduction in the cost of controls in our future installations by eliminating from the control room certain controls and control panels which are in effect a duplication of local controls elsewhere in the plant. We were afraid to do this at either Kern or Station P, due to lack of sufficient experience with the operation of some of these controls under severe operating conditions. Therefore, we were quite generous with respect to the amount of remote controls which were brought into the control room. However, the actual performance of the various automatic features has come up to our most optimistic expectations, and we now are satisfied that some of these remove controls located in the main control room can be eliminated without impairing operating reliability. It is therefore anticipated that the advantages that have been realized through centralized control can be accomplished in the future at a substantially lower investment cost.

There are three control operators stationed on the main operating floor, and they are headquartered in the control room. Due to the fact that the operator has a full view of the firing aisle from the control room and also, due to the visibility and close proximity of the boiler feed pumps and main turbines to the control room, these same three operators are able to take care of patrolling this major equipment. These control operators are trained to handle both the electrical and mechanical equipment. Some of them were originally electrical operators but probably about two-thirds of them were only mechanically trained.

The nucleus of the shift operating organization at Station P was transferred from other centralized control plants where these men had already received a combination electrical and mechanical training. The balance of the organization has been built around this nucleus. Our present practice, in the case of a fairly large job such as Station P, is to assign two full-time instructors for a period of months for the purpose of training the shift operating organization. One of these instructors devotes his entire time to electrical instruction and the other to mechanical instruction; and of course, the operating background of these two instructors makes them particularly qualified to handle their respective assignments, and it also is necessary that they work with each other in order to properly co-ordinate this program instruction.

W. Ridgway (General Electric Company, San Francisco, Calif.): It is immediately evident upon reading this paper that many aspects of this plant show the most modern engineering practices. I should like to elaborate on several features of particular interest.

The unit type main generator excitation system with its high inertia motor driven exciter set provides the most reliable and effective equipment available. The exciter set is designed to meet or exceed the performance characteristics of a direct-connected exciter with maintenance advantages of a separate motor-generator set operating at lower speed.

An amplidyne generator is direct-connected to the shaft of the exciter set and is controlled by a static circuit to provide regulation of the main generator armature voltage. A feature of this static circuit is a lower limit of excitation which is automatically varied proportional to the generator kilowatt loading. This permits underexcited operation of the turbine generator with a reasonable margin of excitation over the steady-state pull-out point for any given loading condition. Local system conditions indicate that this may be a particularly valuable operating tool at this station.

The metal-enclosed isolated phase bus runs and station-type air blast circuit breakers afford fully co-ordinated interconnections between the generator terminals and the house and main transformer banks. Each phase of the bus runs is enclosed in a square aluminum duct. It is dust-tight and watertight and provides complete protection from phase-to-phase faults. Each continuous length of duct is completely sealed and all circulating current paths are broken by insulating joints. Each length is provided with a hinged door for ready access to the interior for inspection.

The station-type air blast circuit breakers are completely metal enclosed and are furnished with a main and stand-by air compressor. Phase isolation is maintained throughout the equipment. Internal illumination of the disconnect switch compartments has been provided for the convenience of the operator. All current transformers are provided with terminal blocks located in a metal-enclosed compartment which is accessible without opening any live compartment. The circuit breaker mechanism is designed for 5-cycle interruption and is provided with complete interlocking for safety of the equipment and operating personnel. Use of 250 pound-per-square-inch air for circuit breaker operation permits smaller air receivers for a given volume of air and results in improved arc interruption.

The hydrogen-cooled turbine generators are rated at 15 pound-per-square-inch hydrogen pressure. Suitable gas seals have been provided for 25 pound-per-square-inch operation should the additional generator capacity available at the higher pressure be desired.

In his discussion, Mr. Whelchel has thoroughly covered the place of house turbines in modern steam plant design. Suffice to say here that in some of the newest plants the house generators and house transformer banks between the generator terminals and the auxiliary station power bus have been omitted entirely. Thus no switching is necessary at generator voltage and the main transformer bank is fed direct from the generator terminals. Power for station auxiliaries is obtained from a transformer bank connected to the high-voltage system.

This simplified construction has permitted considerable economies in steam plant initial investment and may be employed where the integrity of the high-voltage system is unquestioned. As more plants similar to Station P are completed, this integrity should be realized on the Pacific Gas and Electric system.

C. C. Whelchel (Pacific Gas and Electric Company, San Francisco, Calif.): Mr. Benson's paper includes a description of the auxiliary station power supply made up of house turbinegenerators and house transformer banks. Since house turbinegenerators have found their widest application on the West Coast, some additional comments on why they are required, may be of interest.

Primarily the function of the house turbinegenerator is to supply independent 60-cycle full-voltage power to the station auxiliary motors during system disturbances. Their need is therefore dictated by the electrical design and operation of the system.

The Pacific Gas and Electric Company, like some others on the West Coast, has large blocks of hydro-electric generating capacity located at considerable distances from the load. Initially, and to some extent now, this power was brought in over long distance high-voltage lines with an electrical system design which essentially amounted to "synchronizing at the load" with the reliability of generation tied up with its associated transmission line. This form of system design could be and perhaps was carried further in the relatively lightning-free California territory than elsewhere. Although having lower first cost, it was necessary with such a design to take other steps to protect service—one of these being "quick pick-up" main steam generating units with their house turbinegenerators located near the system electrical load center.

The greatest usefulness of house turbinegenerators occurs when the main steam units are operating at light load and due to the loss of a large block of generating capacity elsewhere, it is desired to load the steam units in the shortest possible time. During such disturbances the system voltage and frequency will have fallen to such an extent that the auxiliaries in the steam plant must be transferred to the house unit in order to continue to operate satisfactorily. By this transfer of plant auxiliaries the main unit is in a position to pick up from light load to more than 3/4 load in 30 seconds or less, regardless of low-system voltage and frequency. To the degree that house units contribute to the economical solution of the system electrical problem which has been described, then to that degree it is believed that they are justified.

We recognize that there are power systems in this country that upon the sudden loss of generation automatically reduce load to maintain system frequency at a predetermined minimum on the assumption that the least over-all disturbance occurs when load and generating capacity are approximately matched.

The success of this method of operation plus splitting up the system electrically in case of trouble depends on the characteristics of the particular system under consideration. As conditions permit, this approach to the problem is receiving attention by the Pacific Gas and Electric Company. The objective on any power system is to design the steam stations as simply as possible to meet system requirements. As integration and growth continues, the need for additional "quick pick-up" steam stations with their house turbinegenerators will probably tend to decline. The future trend of steam station design here as well as elsewhere is expected to be in the direction of simplification of equipment and plant layout and still lower station heat rates.

Matching Electrical Characteristics of McNary Generators and Transformers with Bonneville Power Administration Transmission System

B. V. HOARD
MEMBER AIEE

LEWIS WEINER
ASSOCIATE AIEE

CARRYING out the planned orderly development of the Columbia River for power, navigation, reclamation, and flood control purposes, the Federal Government has selected the McNary damsite as the next major project. The site is located on the main stem of the lower Columbia River between the present Bonneville and Grand Coulee dams and near Umatilla, Oreg. The dam will produce slack water approximately 59 miles up the river to a point approximately 32 miles above the mouth of the Snake River. The ultimate installed capacity of the power plant will consist of 14 generating units rated at 73,684 kva each at 95 per cent power factor, thereby permitting the plant to generate 1,127,000 kw at rated power factor, including an overload of 15 per cent which the generating units are being designed to handle. Present plans call for the installation of 10 to 12 of these units with the balance of the machines to await the development of other up-stream plants which will provide sufficient increased storage reservoir capacity to justify installation of the additional units.

The McNary project, being constructed on a navigable stream with important navigation as well as power features, will be under the authority of the Corps of Engineers, United States Army, and hence will be designed, constructed, and operated by that agency. The Bonneville Power Administration (BPA) has the responsibility of transmitting and marketing the power and providing payout of costs of the transmission system and those costs of the generating station chargeable to power purposes for all Federal multipurpose projects within the Columbia River Basin. Hence, the Administration will receive the power generated at McNary and will provide the transmission network necessary to deliver this power to the distant load centers. It is vitally interested in the cost features of the dam and generating plant properly chargeable to power.

The engineers of both Federal agencies recognized that the electrical characteristics which are built into the generating and transformer equipment at McNary would have a great effect upon the over-all economy and the operating characteristics of the complete system, and that close co-operation between the two agencies would obtain a combination of generating plant and transmission system which would adequately serve the needs of the area and obtain the least total cost to the Federal Government. Accordingly, the agencies have worked closely together to this end. The Corps of Engineers requested the BPA to study and recommend the characteristics for the generators and transformers to be installed at McNary. This request resulted in the following:

1. The generators should have a transient reactance not greater than 32 per cent on the generator rating of 73,684 kva at 95 per cent power factor.

2. Continuous damper windings should be provided that will give a ratio of X_q''/X_d'' not greater than 1.1, as determined by tests at air-gap excitation corresponding to rated current, power factor, and voltage with X_q'' determined from X_2.

3. The exciter response should be not less than 1.5.

4. The transformers should have a reactance of not more than 8 per cent on the bank rating of 169,500 kva with two generators connected to a bank.

Following these recommendations, cost figures were compiled by the Corps of Engineers which indicated that the transient reactance for minimum-cost machines for the McNary installation would be 44 per cent on a base of 73,684 kva. With an additional installed cost of $1,550,000 for 14 machines, it would be possible to obtain machines having the recommended transient reactances of 32 per cent. Similarly, reactances for minimum cost transformers, each bank of which is designed to carry the output of two generators, would be 13.5 per cent on a base of 169,500 kva, but this value could be reduced to as low as 8 per cent with an additional cost of $590,000 for seven such banks of transformers. Other additional costs would be incurred with low-reactance units because of the greater physical size of such equipment. These costs included greater investment in the powerhouse and crane and an equivalent capitalized value due to lowered generator efficiency. The total increased cost for reduced reactances for both machines and transformers was calculated to be $3,200,000 including all these factors as compared with the cost of minimum-cost equipment. Obviously, a strong justification was needed for the expenditure of $3,200,000 for special characteristics for the McNary generators and transformers; therefore the Corps of Engineers requested the BPA to prepare this justification.

Paper 49-209, recommended by the AIEE Transmission and Distribution Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Pacific General Meeting, San Francisco, Calif., August 23–26, 1949. Manuscript submitted May 13, 1949; made available for printing June 29, 1949.

B. V. HOARD and LEWIS WEINER are both with the Bonneville Power Administration, Portland, Oreg. The authors wish to express their appreciation to R. C. Schuknecht, L. K. Fisher, and E. C. Starr for their assistance in the preparation of this paper.

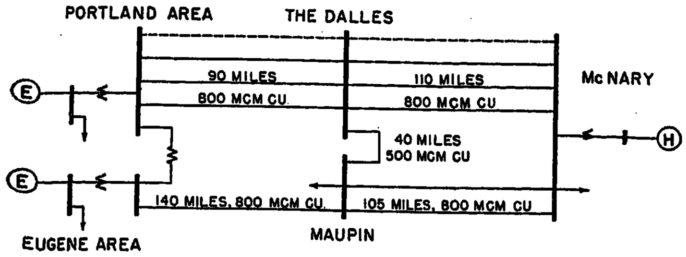


Figure 1. Transmission system for McNary generation

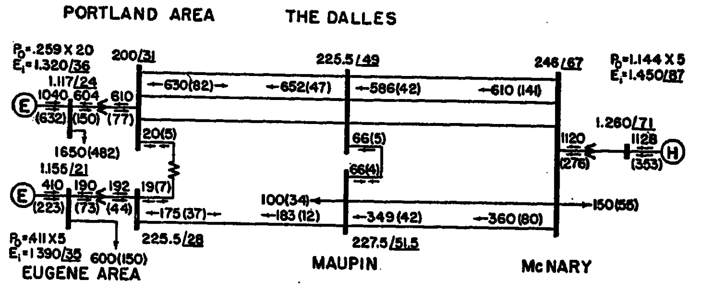


Figure 1(A). Load flow diagram 1

McNary reactances $X_d'=32$ per cent on 73.68-megavolt-ampere base; X of transformer=8 per cent on 170-megavolt-ampere base; 1,128-megawatt output at McNary; maximum swing—104.5 degrees

System Studied

The most satisfactory measure of the benefits to be gained from lower reactance facilities at McNary is a comparison of the maximum power which can be transmitted over the power system with high- and low-reactance terminal equipment, and a determination of what additional transmission facilities must be installed with high-reactance equipment to obtain the same stability as with low-reactance equipment. These factors were obtained through use of the Administration's a-c network analyzer.

In making the study, a simple 3-machine equivalent system was devised, Figure 1, which could be expected to act reasonably close to the actual system. The major components of this simplified system are indicated geographically in Figure 6; and specific details of assumed reactances and inertia factors are as follows:

McNary Characteristics

Generators*:

44 per cent X_d' on 73.68-megavolt-ampere base, $WR^2=186\times10^6$ pound feet squared

32 per cent X_d' on 73.68-megavolt-ampere base, $WR^2=219\times10^6$ pound feet squared

14 units at 70 megawatts, 73.68 megavolt-ampere, 95 per cent power factor

Maximum loading, 1,128 megawatts (15 per cent overload)

Transformers:

7 units at 170 megavolt-amperes each (for two generators)

$X=8$ to 13.5 per cent on 170-megavolt-ampere base

Portland Area Characteristics

Generator:

$X_d'=40$ per cent on 1,500-megavolt-ampere base
$H=4$

Transformer:

$X=30$ per cent on 1,500-megavolt-ampere base (includes equivalent system)

Eugene Area Characteristics

Generator:

$X_d'=40$ per cent on 500-megavolt-ampere base
$H=4$

Transformer:

$X=30$ per cent on 500-megavolt-ampere base (includes equivalent system)

---

* WR^2 includes estimated values for generator plus water wheel.

This system consisted of the McNary generators and equivalent receiver-end machines at Portland and Eugene, with the latter two equivalent machines representing the composite effect of all generators in the two areas which experience has shown will act as a group for faults which are far removed from these areas. The transient reactances and inertia factor selected for these receiver-end machines are an approximation of the weighted average for all generators and synchronous condensers considered. The reactances of the receiving-end transformers also were based on average values of all transformers in the area with an allowance for the reactance of the distribution and transmission system between the actual generating plants and the high-voltage receiving-end busses shown in Figure 1. The transmission network consisted of essentially three 200-mile 230-kv lines from McNary to the Portland area, utilizing 800,000 circular-mil copper equivalent conductor, with a sectionalizing station at The Dalles, 110 miles from McNary; two 105-mile 230-kv lines utilizing this same conductor from McNary to Maupin, and one similar line from Maupin to the Eugene area, a distance of 140 miles. A 40-mile 230-kv line connects The Dalles and Maupin, and the equivalent impedance between the Portland and Eugene areas represents the impedance of transformer and 115-kv lines between the 230-kv busses in these areas. From a study of the normal operating power factors of the individual generators in the Portland and Eugene areas and including the effects of synchronous condensers, it was possible to approximate the power factor at which the equivalent generators used in the study should operate.

The power factor of the receiving-end loads was then adjusted so that the proper generator power factors were obtained. This process might be considered comparable to the adding of static capacitors on the system for the purpose of limiting the reactive requirements on the generating plants. Admittedly, the simplified network cannot be expected to perform exactly as the actual system, but, as will be subsequently shown, the net error should be small in view of the comparison method used to obtain results.

In previous studies it had been established that the most severe fault for the system would be near McNary on one of the lines to The Dalles in spite of the fact that the circuits between The Dalles and Portland would be more heavily loaded than those between McNary and The Dalles. Accordingly, a 3-phase fault was assumed at McNary on one of the lines to The Dalles. The faulted line was assumed to be cleared simultaneously at both ends in $3^1/_2$ cycles and reclosed at both ends in 21 cycles through the use of microwave relaying and high-speed reclosing. This clearing and reclosing time is about the ultimate which might reasonably be expected by the time the McNary generation is installed.

Procedure

The first step in the procedure was to obtain a load-flow diagram of the system using low-reactance generators and transformers with the 14 machines at McNary loaded to 1,128 megawatts, including the overload capability of 15 per cent, then to check the transient stability of the system for the fault assumed. The result of this procedure is shown in Figure 1(A) which was transiently stable for the fault assumed with a maximum swing between machines of 104.5 degrees. It will be shown later that this swing was very close to the transient stability limit since an increase in loading of the order of one per cent of the output of McNary would result in instability. In Figure 1(A) the output of McNary was 1,128 megawatts. The three McNary-The Dalles lines carried 610 megawatts and the three lines from The Dalles to Portland carried 652 megawatts. The power factor of the output of the Portland and Eugene area equivalent generators was held at 85 and 88 per cent, respectively, and the voltage at the high-side bus at McNary was held at 246 kv, which value had been established previously by other network analyzer studies.

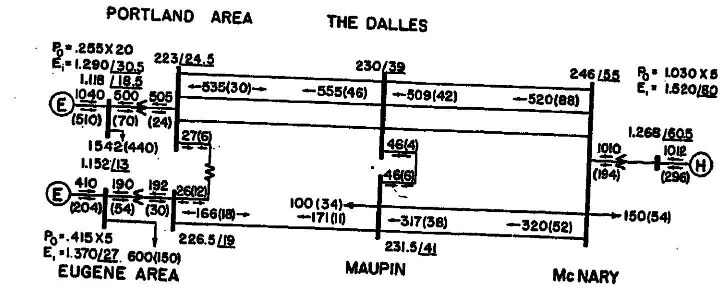


Figure 2. Load flow diagram 2

McNary reactances $X_d'=44$ per cent on 73.68-megavolt-ampere base; X of transformers = 13.5 per cent on 170-megavolt-ampere base; 1,012-megawatt output at McNary; maximum swing—102.5 degrees

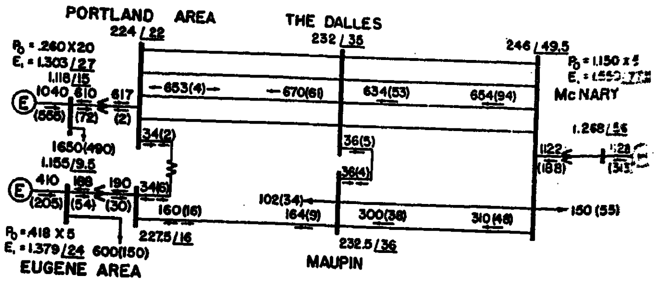


Figure 4. Load flow diagram 4

McNary reactances $X_d'=44$ per cent on 73.68-megavolt-ampere base; X of transformer = 13.5 per cent on 170-megavolt-ampere base; 1,128-megawatt output at McNary; maximum swing—101.5 degrees

The second step in the procedure, starting with the identical system as used before, was to substitute high-reactance transformers and generators at McNary for the low-reactance equipment used in the preceding study, and determine how far down the loading on McNary must be dropped to gain the same degree of system stability. All local loads and receiver-end generators were held constant except that the local load at the Portland area was varied to control the output of the McNary plant. Assuming the same fault, clearing time, and reclosing as previously, it was found that the output at McNary had to be decreased from 1,128 to 1.012 megawatts if high-reactance equipment is installed at McNary and no new transmission lines are constructed. The loading conditions with high-reactance equipment and the output of McNary reduced to 1.012 megawatts are shown in Figure 2. This loading had a maximum swing of 102.5 degrees, essentially the same as for Figure 1.

Assuming identical transmission systems, the permissible loading of the generating station was decreased 116 megawatts, or 10.3 per cent, by the use of high-reactance equipment as compared to the use of low-reactance equipment. The permissible loading on the transmission system from McNary to Portland and Eugene was decreased from 978 to 862 megawatts by the use of high-reactance equipment, a decrease of 11.9 per cent.

The next phase of the problem was to determine what additions must be made to the system so that the generating station can be loaded to the 15 per cent overload value of 1,128 megawatts with transient stability if high-reactance equipment is installed.

Accordingly, a fourth line section was assumed installed between The Dalles and Portland, the most heavily loaded portion of the system, and the McNary plant was loaded to 1,128 megawatts by increasing the local load at the Portland area. The resultant loading is given in Figure 3. For the fault, clearing time, and reclosing assumed previously, this system was insufficient as it was unstable.

Then a fourth line was assumed installed between McNary-The Dalles-Portland area, and the system again balanced to 1,128-megawatts output of McNary, as shown in Figure 4, using essentially the same substation loads as in Figures 1 and 3. This loading proved to be transiently stable with a maximum swing of 101.5 degrees.

An additional study was made to demonstrate that the differences in the angle of maximum swing obtained for loadings corresponding to Figures 1, 2, and 4 do not represent any significant difference in system stability, and that these loadings are very close to the transient-stability limit. To prove this the system loading shown in Figure 4 was increased by only 20 megawatts in the Portland area to obtain the conditions shown in Figure 5. This load increase amounts to less than 2 per cent of the output of McNary, but this slight addition resulted in a change from a stable condition with an angle of maximum swing of 101.5 degrees to an unstable condition. This indicates that the system loadings in Figures 1, 2, and 4 are such that the system stability is practically identical and very close to the transient stability limit.

From these load studies it was concluded that if high-reactance equipment is purchased for McNary it will be necessary to build an additional 230-kv line from McNary to The Dalles to the Portland area, a distance of 200 miles.

Summary of Network Analyzer Studies

1. With three 230-kv lines between McNary and the Portland area and low-reactance equipment at the generating station, the system is transiently stable for the full 1,128-megawatt output of McNary including the 15 per cent overload.

2. With the same transmission system as given before, but with high-reactance equipment at the generating station, the output of McNary must be reduced to 1,012 megawatts to assure transient stability, a decrease of 116 megawatts or 10.3 per cent of the total output of the generating station. The corresponding decrease in the permis-

Figure 3. Load flow diagram 3

McNary reactances $X_d'=44$ per cent on 73.68-megavolt-ampere base; X of transformers = 13.5 per cent on 170-megavolt-ampere base; 1,128-megawatt output at McNary; system unstable

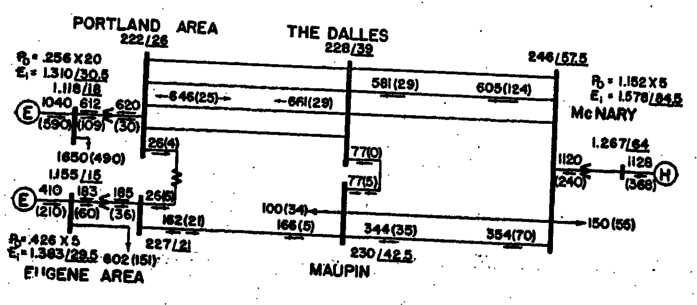
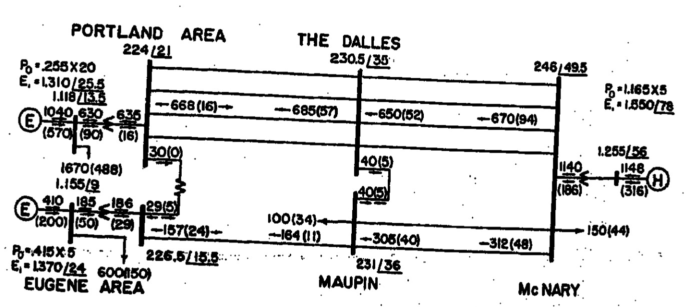


Figure 5. Load flow diagram 5

McNary reactances $X_d'=44$ per cent on 73.68-megavolt-ampere base; X of transformer = 13.5 per cent on 170-megavolt-ampere base; 1,148-megawatt output at McNary; system unstable



sible loading on the lines between McNary and the coast was 11.9 per cent.

3. To assure system stability for 1,128-megawatt output of McNary using high-reactance generators and transformers, it will be necessary to construct a fourth 200-mile line from McNary to The Dalles to Portland.

4. System loadings in all of these cases are essentially identical from the point of view of transient stability and are very close to the transient-stability limits.

5. While the method used in the individual studies may have inherent error due to over-simplification and inability to get exact equivalents of the receiving-end portion of the system, the conclusions of the study are formed by obtaining incremental capability of the system through comparison of two or more studies; therefore possible inaccuracies should almost all cancel out.

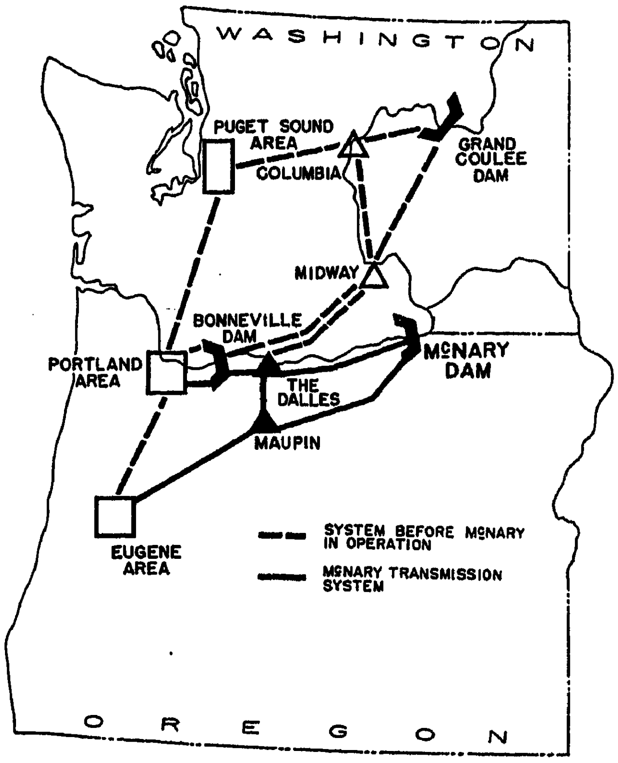


Figure 6. Geographical location of McNary Dam and associated transmission system

Cost Analysis

A cost analysis of the system was made for two cases, the first having low-reactance equipment and one less transmission line as per Figure 1, the second having reactance corresponding to minimum-cost generators and transformers but with the additional transmission line as per Figure 4. In order to minimize possible inaccuracies, the method of obtaining incremental costs in the transmission system and in the generating station was used to determine the benefit to cost ratio. A summary of this cost study is shown in Table I. Information for costs in the generating station was supplied by the Corps of Engineers and included factors for increased powerhouse and crane cost and capitalized values of energy lost due to reduced machine efficiencies when low-reactance equipment was being considered. For the transmission system a capitalized value for the increased transmission losses with one less line was included.

The results of this study indicated a very large saving in the transmission system and a net over-all saving to the government of over $7,000,000 through purchase of low-reactance equipment at McNary even though the value of increased equipment cost would be over $3,000,000. The benefit to cost ratio is of the order of 3.2 which would indicate that from an economic standpoint still lower machine and transformer reactances could be justified. This was considered but the physical size of the equipment became a determining factor. For example, transformers with 8 per cent reactance are the largest which could be conveniently shipped as a unit any larger size would have to be assembled in the field.

There are physical limitations in the generating unit also which resulted in the manufacturers recommending against going to any larger size. However, the high ratio of benefits to cost obtained through lowering the reactances of the McNary generators and transformers does appear to fully justify obtaining low-reactance equipment for this plant.

Table I

Line Cost Analysis		
Cost of additional 230-kv line with four circuit-breaker positions based on 1948 cost values using 800,000 circular-mil equivalent copper conductors:		
McNary-The Dalles section, 110 miles at $48,000 per mile including right-of-way and clearing	$5,280,000	
The Dalles-Portland section, 90 miles at $62,000 per mile, including right-of-way and clearing	5,580,000	
Four 230-kv circuit-breaker positions at $218,000	870,000	
Cost of additional transmission system in lieu of low-reactance powerhouse equipment		$11,730,000
Subtracting capitalized value of additional line losses by having one less line in service. Incremental loss of peak firm and peak secondary power from Figures 1 and 4 = 12,400 kw. Average value per peak kilowatt incremental loss: $5.67 per kilowatt-year. Interest, replacement, and operations and maintenance 4.8 per cent. 12,400×5.67/0.048		1,470,000
Net system savings available for reducing powerhouse equipment reactances		$10,260,000
Powerhouse Equipment Cost Analysis		
Increased cost of 14 generators because of reduced reactance and increased installation cost	$1,550,000	
Increased cost of seven transformers because of reduced reactance and increased installation cost	590,000	
Capitalized value of lower generator efficiency due to low-reactance generators. Estimated reduction in efficiency, 0.4 per cent. Value at $185 per installed kilowatt. $185×70,000×14×0.004	730,000	
Estimate of increased powerhouse and crane cost due to low-reactance equipment	300,000	
Increased cost chargeable to low-reactance equipment		3,170,000
Net Over-All Saving to the Government Through Use of Low-Reactance Units at McNary		$ 7,090,000

Conclusions

It is felt that the results of this study will be of major interest to the industry, for while most engineers agree that low-reactance equipment should be used, the question of justifying such equipment economically nearly always presents a major problem. It is recognized that each project becomes an individual problem to be studied in accordance with the peculiar conditions arising, for it is the combined system, including both transmission and generation, which must be studied. This paper presents, in our opinion, a very satisfactory method of investigating and solving this type of problem. We feel this method can be applied successfully in many cases.

Discussion

W. A. Morgan (Bureau of Reclamation, Denver, Colo.): The authors are to be congratulated on a fine paper. This paper brings to our attention the modern, analytical approach of solving power system design problems which the a-c calculating board makes possible.

In the not-too-distant past, designers of hydroelectric power systems were forced to include considerable margin in generator characteristics. It was known that low reactance and high short-circuit ratio were necessary and desirable for stability reasons on a system with long transmission lines, and usually it was more engineering "judgment" than technical data that formed the basis for selection of generator characteristics. Now with greater use of the a-c calculating board it is possible to more accurately predetermine system performance and to take advantage of faster relaying and circuit breaker operation.

The Bureau of Reclamation recently had occasion to work with the Corps of Engineers in determining the characteristics for the generators of the Fort Randall and Garrison Power Plants of the Missouri River Basin power system. The Bureau made the study using its a-c network analyzer which is located in Denver. The Bureau investigated previously proposed general methods of evaluating incremental system performance improvement and the incremental cost per kilowatt of the increased power limit. However, this general method of approach could not be justified for the Missouri River Basin power system. A study of specific savings in designs similar to that outlined by the authors, was the basis for justifying a certain amount of lower-than-normal generator and transformer reactance in the case of the Missouri River Basin power system.

Figures 1 and 2 of this discussion will, I believe, give you a graphic picture of the effect of lower-than-normal generator reactance and thereby augment both the author's paper and my discussion with illustrations. Figure 1 shows a power-swing curve of a particular fault condition which produced instability on the system. In this case, normal calculated values of generator reactance and inertia were used and the system was *unstable*. You will note that the Garrison generators tend to diverge from or pull out of step from the rest of the generators which swing together. The ordinates represent relative angular displacement in electrical degrees. The time axis is in cycles, and in this case we assumed $20^{1}/_{4}$-cycle reclosing of the circuit breakers after the fault.

Figure 2 shows the same fault-and-load conditions except that the generator transient reactance has been lowered to 85 per cent of normal calculated value and the inertia increased to 116 per cent of normal. In this case the system is *stable* with fast reclosing of the breakers. The effect of reducing the generator reactance in improving system stability is apparent from these illustrations.

There are a few questions I would like to ask the authors, because I believe the value of these meetings sometimes is brought out by discussions:

1. It is noted that 800,000 circular-mil copper equivalent conductor is to be used on the McNary-Portland 230-kv transmission lines. This is a larger than usual size and we have been interested in these large sizes. Apparently this choice of size is based upon the most economic design considering losses and initial investment. What average kilovolt-ampere loading, power rate for losses, and installed cost for the conductor were used to obtain this size conductor?

2. A system having a transmission distance of 200 miles with no intermediate loads would appear to be in the range of consideration of voltages higher than 230 kv, or to 230-kv series capacitors. Were these types of transmission line design considered as an alternate to the straight 230-kv transmission system in the economic studies?

3. It is stated that a 3-phase fault was assumed for the studies. The proportion of 3-phase faults to total faults is less for lines without overhead ground-wires than those with overhead ground-wires and, of course, the total number of faults is greater. Studies that the Bureau of Reclamation have made have indicated an appreciable difference in power limit for different kinds of faults, depending upon the clearing time. Was any consideration given to the different types of faults?

4. The reliability of some power systems will not allow consideration of fast reclosing circuit breakers. However, where fast reclosing circuit breakers are used, it would appear that the maximum benefits would be sought. It is appreciated that experience data is not completely available as to the minimum reclosing time allowable from an arc deionized air standpoint. However, studies by the Bureau indicate considerable benefit to system stability by reducing reclosing times below 20 cycles. Were reclosing times faster than 21 cycles considered in the studies by the authors?

The use of the a-c calculating board in analyzing power system problems is very valuable. It is the purpose of these questions to bring out the importance of the assumptions that are made in the studies as they are fundamentally important in getting the proper answers and factual

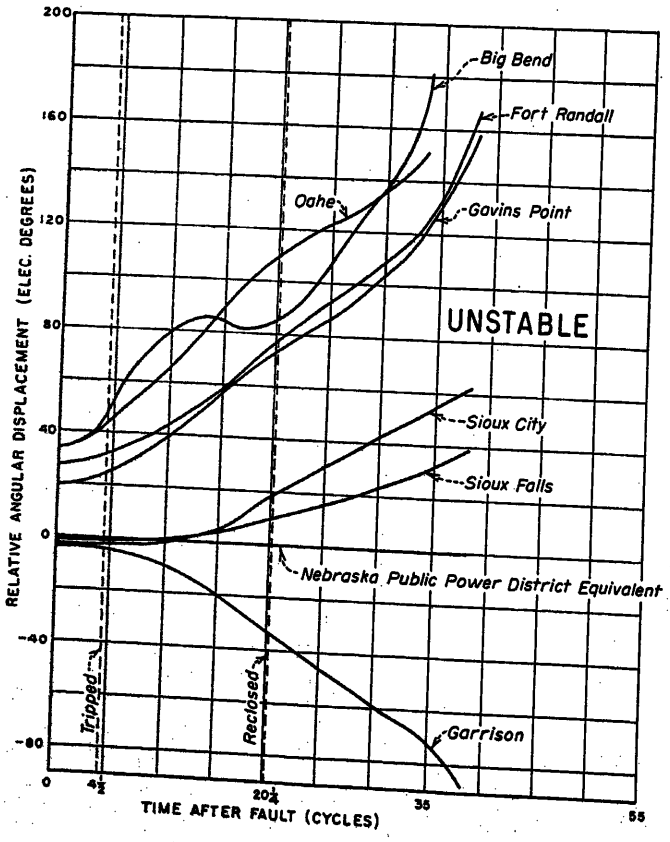
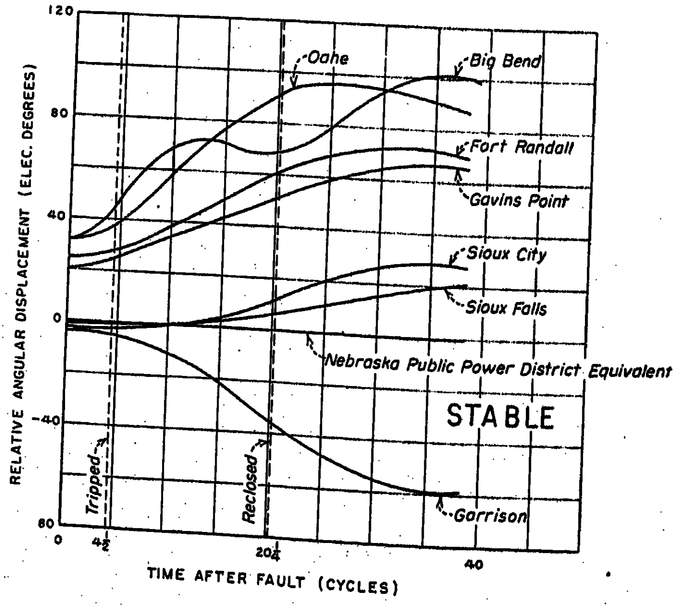


Figure 1 (left). Power system unstable with normal calculated values of generator transient reactance assumed

Figure 2 (right). Power system stable for the same fault and load conditions as for Figure 1 except that the generator transient reactance was reduced to 85 per cent of normal calculated value



data upon which to base engineering design decisions.

B. V. Hoard and **Lewis Weiner:** It is of interest to the authors to learn in the case of the Fort Randall and the Garrison Power Plants in the Missouri River Basin that decreasing the transient reactance to 85 per cent of normal values had the effect of making a system which was quite unstable into one which was reasonably stable. They also are pleased to learn that an approach was made to solve that problem, which in several ways was similar to that discussed in the paper.

The very important and timely questions which were raised by Mr. Morgan are taken up in the order in which he listed them.

1. A rather extensive cost comparison was made between 500,000 and 800,000 circular-mil copper equivalent conductors for the McNary-Portland 230-kv transmission lines before selection of the larger conductor was made. The total initial cost of a line utilizing the larger conductor including right-of-way was estimated to be from 15 to 20 per cent greater than for a 500,000 circular-mil copper equivalent conductor, depending to some extent on the terrain, right-of-way and clearing costs. Using this figure and evaluating losses at $15 per kilowatt year, transmission costs were identical for the two conductors at loadings around 140,000 to 150,000 kva. At loadings above this value, cost analyses favor the use of the larger conductor. It is expected that peak loads of 200,000 kw per line at annual load factors of 70 to 80 per cent will be realized. With future addition of other developments to the system, still higher peak loads on these lines are expected.

2. The possibility of utilizing a transmission line voltage of 287 kv for McNary was given some consideration and study. A cost analysis indicates that for distances of 200 miles only a very slight benefit can be gained by use of this higher voltage and then only if no intermediate loads have to be served. Since intermediate loads and eventually a large intermediate generating station at The Dalles had to be considered and since the Bonneville Power Administration system is at present a 230-kv system, it was decided to continue with the level at which we have had experience, as being more economical than the higher voltage. Series capacitors were no longer given serious consideration when it was discovered that this particular 230-kv system could be loaded close to the economic limit without unduly risking instability. On the other hand, several 230-kv series-capacitor installations are being considered elsewhere on the Bonneville Power Administration system, and one experimental installation is expected to be in operation in the latter part of 1950.

3. It is agreed that the transient-stability limit of a system will depend to some extent on the type of fault, and the power limit will be somewhat less for a 3-phase fault than for a double-line-to-ground fault which might have been used as a criterion. However, for the purposes of this study, we feel that the assumption of the more severe fault was proper for the following reasons:

(a) Five years will have elapsed between the time this study was completed and the time the first McNary generator will be energized, and eight or more years will have passed before the station will be essentially completed. Estimates so far in the future of load division in the various areas must necessarily be approximate. For this reason the final details of the transmission system cannot be decided at this time, as there undoubtedly will be some changes in the assumed transmission system.

(b) It follows that the final transmission system was not determined by this preliminary study, but only the pattern of development was selected, that is, low-reactance equipment and fewer transmission lines as compared with high-reactance equipment and more lines. Economically the results were decidedly in favor of low-reactance equipment.

(c) Aside from the simplicity of using 3-phase faults when making the study on the analyzer, it is customary in preliminary studies, where many essential factors are still indeterminate, to use a more severe criterion than might be used for the final design when with elapsed time the load pattern and magnitude have been established. As a matter of necessity, the final plan will be influenced to considerable extent by the development of other future generating stations, but, whatever their influence, we feel that the proper system pattern has been established for the McNary development, and this pattern certainly would not have been changed by assuming a somewhat less severe fault than a 3-phase fault.

From the standpoint of the final design, the McNary plant will be a major source of power in a fairly large and widespread network, and any instability would seriously affect a rather large heavily populated area. Furthermore as the Bonneville Power Administration system increases in scope, restoring service after a break-up will become increasingly difficult and take a longer time. It is not known at this time whether a simple 3-phase fault or a double-line-to-ground fault will be used as a criterion for the final design; or whether something more complicated might be considered, such as a double-line-to-ground fault on two circuits followed by successful reclosure on one circuit and reclosure on a permanent fault and then lockout of the circuit breakers on the second circuit. We have had a considerable number of simultaneous faults on double-circuit lines which makes such a criterion a distinct possibility.

4. The authors did not consider reclosing times faster than 21 cycles, although it is admitted that reclosing time has a considerable effect upon stability where a fault on one of two parallel lines is involved. On the other hand, as the number of parallel circuits is increased, as will be the case for McNary, the outage time becomes less important. Also, in view of the lack of experience along the line of the safe minimum reclosing time, it was deemed best to be realistic in the study until additional experience indicated that appreciably shorter reclosing times would still allow sufficient deionizing time for extinguishing the fault arc. We, of course, are heartily in agreement with most engineers that shorter reclosing times should be tried, and, if proved successful, should be used for system planning.

The authors believe that the system developed for the transmission of the power from McNary meets two very important criteria in that normal loading conditions are near the economic limit, and the system is essentially stable for all but multicircuit faults. While the paper presents a method of evaluating the benefits of lowered generator and transformer reactances, the authors would like to emphasize that each particular situation must be investigated in line with the conditions peculiar to it. For example, in a recent investigation of the future transmission system of another large generating station, it was shown that due to the relatively short transmission distance to the load, plus necessary strong ties to other existing generating stations, the additional stability resulting from low-reactance equipment could not be utilized from a practical standpoint and therefore reactances corresponding to minimum-cost equipment were recommended.

A Proposal for Broad-Band Coupling of Power-Line Carrier Equipment

R. H. MILLER
ASSOCIATE AIEE

Synopsis: This paper proposes the development of broad-band multichannel power-line carrier systems. The particular problem of a band stopping filter, for use in power lines, to make such a system practicable, is discussed, and a possible filter structure for this application is proposed.

THE APPLICATION of carrier equipment to power lines has led to the development of methods of line treatment in order that the effects of station busses, tap lines, and other discontinuities that may exist in the power system will have a minimum effect on the operation of carrier-current equipment that may be applied to such power lines. Early installations of power-line carrier equipment made use of bus coupling, which required a minimum of equipment but resulted in carrier channels which were greatly affected by switching setups on the power system. The ideal circuit for application of line carrier equipment is a single isolated, untapped line. In such a case, the attenuation of a line is relatively uniform and increases almost linearly with frequency. Tap lines or untrapped extensions introduce reflections, which at odd quarter wave lengths appear as low impedance points with correspondingly high attenuation.

Line Traps Used for Blocking Carrier

In order to minimize the effects of busses and tap lines, resonant traps have been developed which are placed in the lines on which carrier is to be applied, between the points of coupling and station busses and in tap lines at the points where they connect to the through line.

The resonant traps in common use on power-line carrier systems consist of air-core inductance coils with conductor sufficiently large to carry line current and tuned with capacitors to parallel resonance at the frequency of operation of the carrier equipment. In the case of 2-frequency duplex carrier systems, the traps are double tuned to give two points of high impedance corresponding to the two frequencies at which the carrier terminals are adjusted to operate. Figure 1 shows schematic diagrams of typical single- and double-frequency line traps.

Line traps of the types presently available are sharply tuned devices and present high impedance to the carrier signals at the points of resonance, but fall off rapidly on either side of the resonant point so that the attenuation to the side bands is much reduced. Figure 2 shows typical resonance curves taken from field tests of both single- and double-frequency traps. It can be seen from these curves that any errors in line-trap tuning reduces the effectiveness of the trap in attenuating the carrier signals in the direction that suppression is desired, and reduces the isolation of the carrier channel from effects of reflections of tap lines, low impedance busses, and so forth.

Blocking Filters

The development of broad-band stopping and broad-band coupling equipment for power-line carrier systems would make possible increased utility of power lines for carrier purposes, making feasible the simultaneous operation of several duplex voice channels, along with relaying and supervisory signals, on one line. Present coupling methods become complicated with a large number of tunable elements which require precise field adjustment when more than one or two carrier frequencies are applied to a line. With more than two frequencies in use, additional line traps are necessary, in series with the line, making a cumbersome and expensive installation.

In proposing broad-band coupling for power-line carrier systems, it is realized that the cost of a band stopping filter, which would block a relatively wide band of frequencies, would be somewhat greater than the present resonant-type traps however, with the possibility of using several simultaneous carrier channels with only one set of blocking filters at each end of the line to which the carrier is applied, an over-all economy seems probable.

In order to investigate the feasibility of designing filters which would block a relatively broad band of carrier frequencies, several filter structures have been studied.

The simplest filter which would have the properties desired is a conventional constant K type of low pass filter,[1] shown in Figure 3. The formulas for element values for a filter of this type are

$$L=\frac{R}{\pi f_c} \tag{1}$$

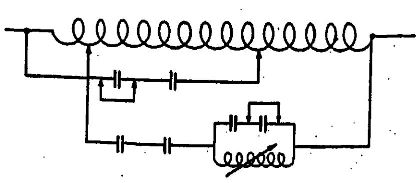


Figure 1. Schematic diagrams of typical power-line carrier line traps

Single-frequency trap above, 2-frequency trap below

Paper 49-210, recommended by the AIEE Carrier Current Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Pacific General Meeting, San Francisco, Calif., August 23–26, 1949. Manuscript submitted May 24, 1949; made available for printing June 29, 1949.

R. H. MILLER is with the Pacific Gas and Electric Company, San Francisco, Calif.

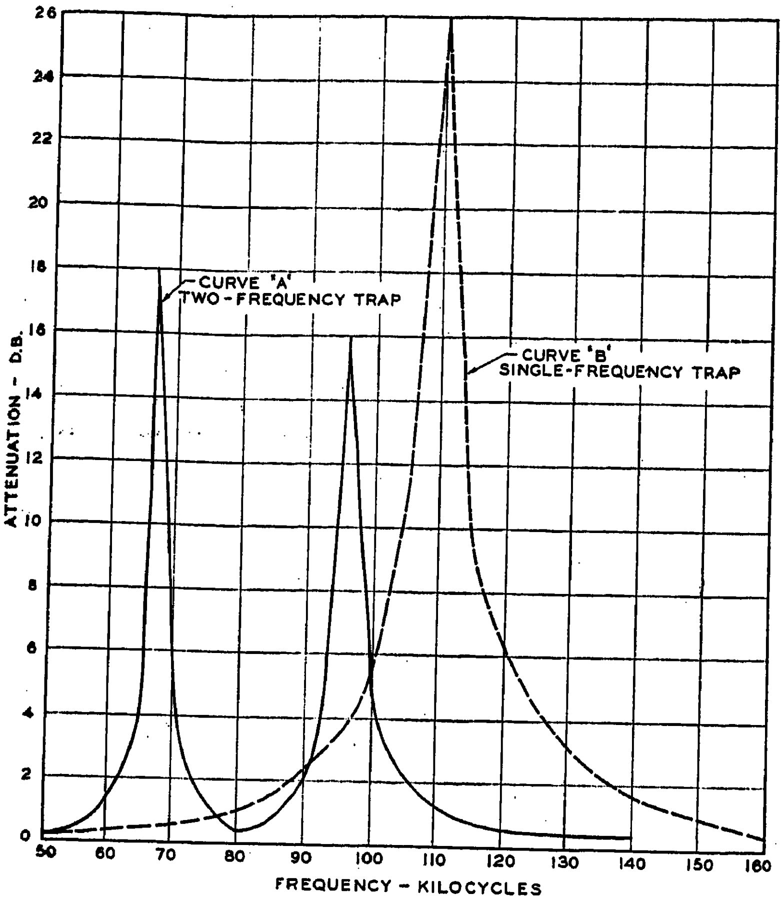


Figure 2. Attenuation characteristics of single and double-frequency carrier line traps

$$C=\frac{1}{\pi f_0 R} \qquad (2)$$

This type of filter requires components, both series inductance and shunt capacitors, of such magnitudes as to be impractical.

Of the other filter structures, the constant-*K* band elimination filter, Figure 4, seems to present possibilities for practical development. The formulas for element values for this type filter are

$$L_1=\frac{(f_1-f_2)R}{\pi f_1 f_2} \qquad (3)$$

$$C_1=\frac{1}{4\pi(f_2-f_1)R} \qquad (4)$$

$$L_2=\frac{R}{4\pi(f_2-f_1)} \qquad (5)$$

$$C_2=\frac{f_2-f_1}{\pi f_1 f_2 R} \qquad (6)$$

This type of filter gives an attenuation characteristic as shown in Figure 5, and can be designed to attenuate relatively broad bands with values of C_2 available in present sizes of coupling capacitors.

A practical filter of the band elimination type, used for blocking power-line carrier signals, would take the form shown in Figure 6.

A blocking filter of this type to cut off frequencies from 100 to 150 kc for a 600-ohm line has been calculated from equations 3, 4, 5, and 6 to require the following components: L_1 = 0.637 millihenry, C_1 = 0.00267 microfarad, L_2 = 0.956 millihenry, and C_2 = 0.00177 microfarad.

The calculated value of C_2 is higher than that available for 230-kv capacitors commercially available; however, the shunt arm of the filter can be designed for a higher impedance than that of the transmission line with the sacrifice of some attenuation, particularly at the mid-frequency of the rejection band, but without materially altering the properties of the filter otherwise. In this manner the capacitance required in the shunt arm can be reduced. In the practical case, if the term *R* in equations 5 and 6 is doubled, L_2 will be doubled and C_2 halved, which brings the value of C_2 into the range of commercially available components. Doubling the value of L_2 should not present any particular problem in mechanical or electrical design as the power-line current is not carried by this element.

An experimental filter of the type shown in Figure 6 was built to determine its effective attenuation band. This filter was built using the constants given above, for cutoff frequencies of 100 and 150 kc. Facilities were not available to construct such a filter with the current and voltage ratings that would be necessary for power-line carrier work; however, a filter using the same constants should be identical when built for the current and voltage ratings that would be required for power-line carrier applications, with the possible exception that the *Q* of the inductors might be somewhat different than those used in the experimental filter. However, the *Q* of air-core coils used in such power-line filters probably would be of the same order of magnitude as those used in the experimental filter. Figure 7 gives curves showing the rejection band of the experimental band elimination filter, both for 600-ohm shunt impedance, curve *A*, and for 2.5 × 600-ohm shunt impedance, curve *B*. These curves indicate a rejection band approximately 30 kc wide, with attenuation of 15 decibels or greater. This band is of sufficient width for three simultaneous carrier telephone channels, with almost complete blocking in the unwanted direction.

Curves *B* of Figure 7 shows the effect of increasing the shunt-arm impedance in order to reduce the size of shunt capacitors. The effect of such an increase of shunt-arm impedance is primarily a re-

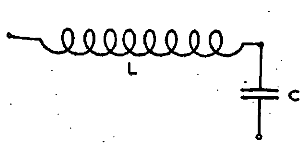


Figure 3 (left). Elementary low-pass filter

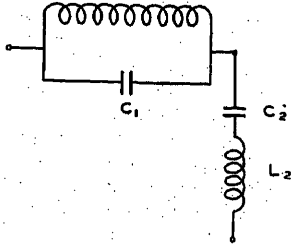
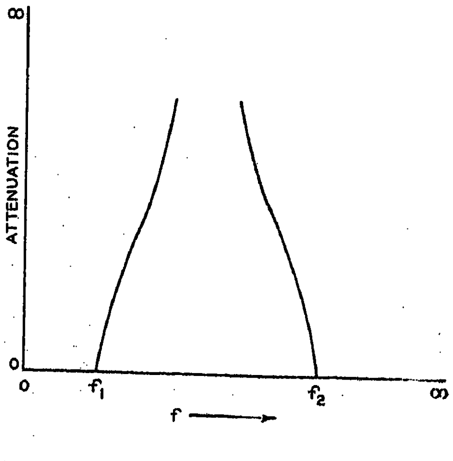


Figure 4 (right). Elementary band-elimination filter

Figure 5. Attenuation characteristics of band-elimination filter (ideal)



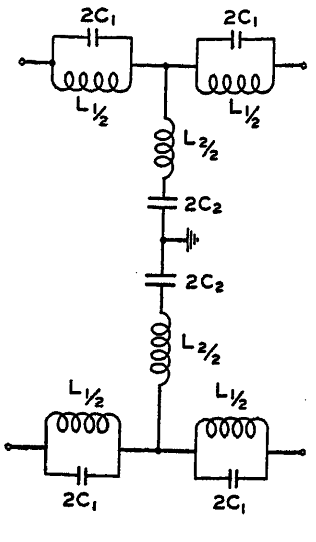


Figure 6 (above). Schematic diagram of practical band-elimination filter

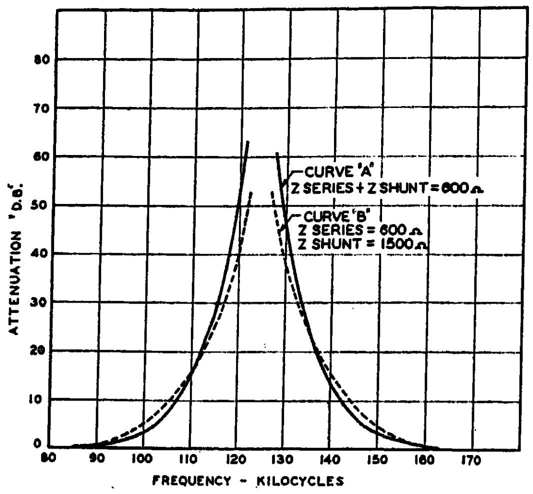


Figure 7 (right). Attenuation characteristics of experimental band-elimination filter

duction of peak attenuation at mid-band, with a slight increase of the attenuation band for values below 20 decibels.

Multichannel Carrier Systems

The development of a broad-band elimination filter would make feasible the use of power lines for multichannel carrier telephony by methods well established in the telephone industry. Group modulation of several low-frequency carrier channels similar to a standard "C" carrier system[2] could be used to translate frequencies upward to the power-line carrier frequency band, and these channels could be coupled by means of a band-coupling filter to the power line. Similarly, the received signals, by means of a group demodulator, would be translated downward in frequency and separated by receiving-band filters into their respective channels. A block diagram of such a proposed multichannel power-line carrier system is given in Figure 8. It would be necessary to design the band-coupling filter, indicated in Figure 8, so that the coupling capacitor would be the terminating element of the filter.

Conclusions

It is recognized that considerable developmental work will be required before such a broad-band power-line carrier system can be made commercially practicable; however, the need for such development exists due to increasing requirements for communication in the operation of power systems, and the impracticability of coupling more than one or two channels of carrier to power lines by means of conventional equipment.

One of the major obstacles to such a broad-band system is the fact that resonant line traps attenuate only a narrow band of frequencies. It has been the purpose of this paper to suggest a possible method of approach to the design of a band stopping filter which will attenuate a broad band of frequencies and at the same time be relatively simple in structure and within the limits of economic feasibility. As a further suggestion, equipment of the type discussed in this paper well might be developed for a band from 150 to 300 kc, leaving the present 50- to 150-kc band for applications where conventional equipment can fill the requirements.

It is not felt that the multichannel carrier system proposed would present major difficulty in design, as no radical innovations are proposed and similar systems have been used in telephone practice for many years. It is hoped that sufficient interest will be stimulated so that development of broad-band multichannel power-line carrier systems will result, to meet the needs of those power systems where additional requirements exist beyond those that can be met with present equipment.

References

1. Electrical Engineers Handbook (book), Harold Pender, Knox McIlwain. Third edition, John Wiley and Sons, New York, N. Y., pages 7-46—7-52.

2. Electrical Communication (book), A. L. Albert. Second edition, John Wiley and Sons, New York, N. Y., page 449.

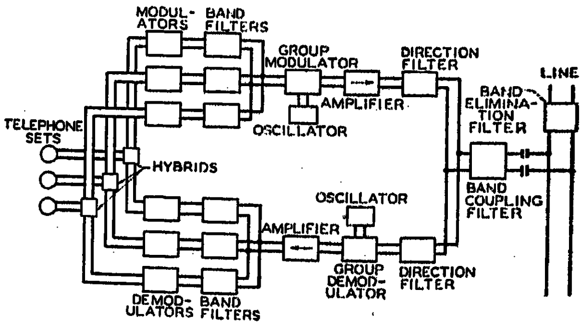


Figure 8. Block diagram of proposed multichannel power-line carrier telephone terminal

Discussion

William U. Dent (Westinghouse Electric Corporation, Los Angeles, Calif.): I agree with Mr. Miller that the use of band elimination filters and broad-band coupling would be of considerable assistance to the communication engineer who is confronted with the necessity of providing additional carrier facilities on lines that are already crowded. This is particularly true on long lines where it would be difficult or costly to provide channels by other means.

Suggestions of this general type have been made several times in the past, but each time it has been found that the need for this type of equipment was not sufficient to justify its cost. Today the situation may be different, particularly in some parts of the country, because of the crowding in the carrier spectrum and the need for additional circuits, but the major problem is still one of economics, rather than engineering.

For example, it is estimated that the band elimination filter suggested in this paper for 2-phase trapping on a 230-kv circuit would cost approximately three to four times as much as two double-frequency traps. To this must be added the cost of the band-pass coupling equipment and the coupling capacitors which would probably be slightly higher than for 2-frequency equipment. These figures do not include the necessary development costs which are difficult to estimate at this time.

From this it can be seen that band elimination filters of the type described and band coupling equipment can only be justified if at least four channels can be used on the circuit involved.

The suggestion for a multichannel carrier system may be the answer to this, since the filter circuits would permit such an operation, but again the question of economics arises.

In the final analysis, what is desired is a system that will permit the use of more channels of carrier at a cost per channel that can be justified. To accomplish this, it must be shown first that the quantity of equipment involved on a nation-wide basis is sufficient to cover the development costs without running the cost per channel to an excessive figure. In the past this has not been the case, but the approach to the problem suggested by Mr. Miller, together with the present conditions, may justify additional studies, and should be considered very thoroughly.

E. W. Kenefake (General Electric Company, Syracuse, N. Y.): It is commendable that the author has picked the subject of broad-band blocking for investigation, and it should serve to stimulate considerable interest in a different approach to the application of power-line carrier equipment. Although his blocking filter, as proposed, would be rather expensive, it is probably an essential element of the general application scheme proposed by the author.

We have done some investigating of the blocking problem, and also of the coupling problem, for approaches similar to this, but as yet sufficient information has not been collected to really evaluate the system. For the author's consideration and any others interested, we have noted some phases of the band blocking and band-coupling problem which were not covered in this paper. A quick check indicates that the attenuation achieved with the band elimination filter described by the author would be roughly the same attenuation which could be obtained with a single line trap having the same inductance in the line as employed in the band elimination filter. We would welcome the author's comments on this comparison. If this is true, then effectively the same characteristics could be obtained without the use of a shunt arm which involves a rather expensive coupling capacitor. Using inductances of these values in transmission lines raises some new technical problems which also will require considerable study.

A further problem involved in using a shunt arm to achieve greater attenuation at the edges of the band is the impedance discontinuity at the cutoff frequencies. This is a problem because carrier transmitters and receivers working in conjunction with varying impedances would suffer somewhat performance-wise, so that the full bandwidth might not be useful.

There has been considerable discussion of the need for 3-phase blocking of power lines in order to achieve effective trapping. This logically arises from observing the effect of opening circuit breakers and disconnect switches on the attenuation of carrier-current signals. Some better evaluation of the effectiveness of trapping all three phases or using band elimination filters in all three phases versus one or two phases would be useful information.

It is probably too early to attempt to draw any economical conclusions from this paper, but it would seem that band elimination filters would have to provide considerably better blocking of carrier signals than is possible with line traps to justify the expense of the shunt arm in the band elimination filter. In our studies, the impedance of the bus or power terminal equipment on the performance of a band elimination filter is more pronounced than with line traps. Because it is expected that quite wide variations would be experienced in the terminal impedance, it poses quite a problem for the proper application of band elimination filters as a universal tool in the over-all coupling circuit.

L. R. Spaulding (Bonneville Power Administration, Portland, Oreg.): Mr. Miller's proposal for broad-band coupling of power-line carrier equipment is both interesting and timely.

We, at the Communications Laboratory of the Bonneville Power Administration, also have encountered the need for such a coupling arrangement. Under certain conditions we have observed a pronounced loss of modulation in the received envelope of a carrier signal, which we attributed, in part, to highly selective wave traps and, in part, to highly selective line tuning units. The sidebands are subjected to greater losses than the carrier in these circuits.

Under other conditions we have observed a pronounced degree of over-modulation in the received envelope which was apparently due to the transmission of sidebands directly through a station in the path of the carrier circuit, even though sharply tuned line traps were installed on each side of the station. Under this condition the path through the station would either be much better than the path provided by the station bypass circuits, or would cause a different degree of phase shift in the carrier, or both.

We have considered the use of isolated overcoupled secondaries[1] on existing traps and line tuning units for the purpose of broadening them, and have conducted limited experiments of this kind. The amount of broadening practicable by this means does not appear to be suitable for a wide band of carrier channels but might well be adapted for improved operation of a single channel.

REFERENCE

1. RADIO ENGINEERS' HANDBOOK (book), F. E. Terman. McGraw-Hill Book Co., (New York, N. Y.), 1943, page 154.

R. H. Miller: Mr. Dent has given consideration to the economic aspects of the development of a broad-band carrier system, such as proposed in this paper. It is realized that the blocking filters, as proposed would require considerable development expense and would be more expensive than resonant traps. I agree with Mr. Dent that a careful study should be made to determine the economic feasibility of the proposal.

Another factor to be weighed along with the purely economic consideration is the greater utility of the carrier frequency band which should be possible with the proposed system. In many locations, the carrier spectrum is already congested and additional communications requirements will have to be met either by wire, radio, or new development in the power-line carrier art. In such cases, the economic comparison would be with alternate methods of communication rather than with conventional power-line carrier systems. It is agreed that the economy of such a system would be realized where several channels of communication would be required on one line.

Mr. Spaulding's comments are quite pertinent. It is interesting to learn that other operators have experienced limitations due to the use of highly selective wave traps. Although no experimentation with isolated, overcoupled secondaries, as mentioned by Mr. Spaulding, was carried out, this possibility of increasing the rejection band of line traps was considered, and it did not appear that sufficient advantage would result to justify a detailed investigation. Mr. Spaulding's results apparently bear out this conclusion.

Mr. Kenefake suggests that attenuation equal to that obtainable with a band elimination filter can be obtained with a single line trap using the same amount of series inductance as would be required for the band elimination filter. Our experimental results indicated considerably more attenuation with the band elimination filter, and also that a wider band of frequencies were attenuated than were feasible with a single trap. Considerably more work should be done on this problem to determine the most desirable blocking arrangement. The band elimination filter was suggested for this purpose merely because it appeared to be the simplest structure that would provide the required characteristics, but the field was by no means completely explored.

Concerning the impedance discontinuities at cutoff frequencies, mentioned by Mr. Kenefake, these effects can probably be minimized by designing the blocking and coupling filters so that they are complementary. This procedure is commonly employed in designing high- and low-pass line filters for telephone carriers.

I agree that the question of 3-phase blocking should be further investigated, and in some cases it is probably highly desirable.

Subsequent to the presentation of this paper, information was received from Mr. F. J. Bartholomew of Vancouver, B. C., that the British General Electric Company has developed a multichannel power-line carrier system, very similar to that proposed in this paper, and that such systems have been in satisfactory operation for several years, using band filter coupling, and providing up to eight duplex channels over one carrier system. Consequently, it appears that a carrier system such as was proposed in this paper is feasible and that such systems are already in service in England. Information has not been received as to whether or not the British have investigated broad-band stopping filters, however, and information on this would be of interest.

Apparently, the main purpose of the paper has been accomplished, in that considerable interest in this topic has been aroused, and it is hoped that serious study and development work will result.

Magnetic Heating of Transmission Line Clamps

V. L. CRABB
MEMBER AIEE

J. M. SHEADEL
ASSOCIATE AIEE

Synopsis: Magnetic heating of transmission line clamps is reduced by the use of nonmagnetic parts. An economical method is the use of a non-ferrous body. Of the three methods used to measure clamp power loss, the multiturn coil circuit is recommended. The results of tests made with the multiturn coil show the marked difference between the power loss of the all-ferrous clamp and that of the nonferrous-body clamp. If clamp and conductor losses are known, the temperature rise of a conductor inside a clamp can be estimated.

MAGNETIC heating of suspension and strain clamps has received increased design consideration because of higher transmission line currents. The temperature rise of conductors with sustained load currents of several hundred amperes may be excessive inside all-ferrous clamps.[1,2] At these currents, a decrease in clamp magnetic power loss to reduce temperature rise may justify, economically, the purchase of more expensive nonmagnetic clamps. Some purchasing specifications list the maximum power loss which a clamp may have at a given current.

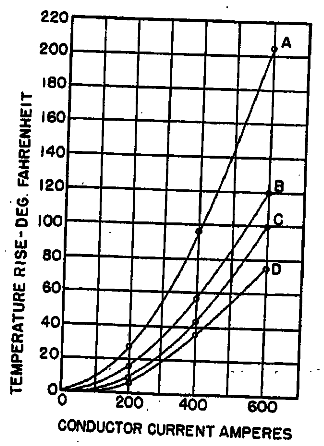


Figure 1. Typical temperature rise above ambient of 1.12-inch seat diameter suspension clamp and 477,000-circular-mil steel-reinforced aluminum cable (ACSR)

Curve A. All-ferrous suspension clamp
Curve B. Ferrous body and nonferrous keeper piece, U-bolts, and nuts
Curve C. Cable alone in air
Curve D. Nonferrous body suspension clamp

Typical temperature-rise and power-loss curves, Figures 1 and 2, show the marked difference between the all-ferrous and the nonferrous-body clamp designs. For the all-ferrous clamp a line current of several hundred amperes is required to produce a significant loss or an appreciable temperature rise.

Power Loss in Clamps

An all-ferrous clamp which surrounds a conductor carrying alternating current has hysteresis and eddy-current losses. Substitution of nonmagnetic material for certain parts of the clamp increases the reluctance to the magnetic force. The resulting decrease of flux in the magnetic loop reduces power loss and heating. Figure 3 shows the application of this method. Measurements on a clamp with a nonmagnetic body show a power loss of about 1/20 of that of the all-ferrous design. Measurements on a clamp with all parts nonmagnetic show no power loss.

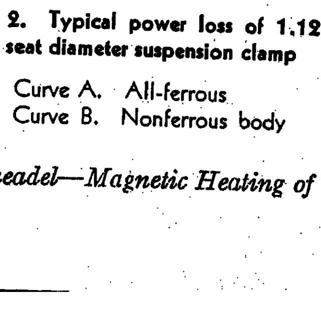


Figure 2. Typical power loss of 1.12-inch seat diameter suspension clamp

Curve A. All-ferrous
Curve B. Nonferrous body

Economic reasons have directed the use of suspension clamps with all ferrous parts or those with nonferrous body and other parts ferrous. Hence, laboratory tests have been made principally on these two types of clamps. Because of galvanic corrosion, it is desirable to have bronze-body clamps with copper conductor and aluminum-alloy-body clamps with aluminum conductor.

Methods of Power-Loss Measurement

CURRENT TRANSFORMER METHOD

High current is passed through a conductor by means of a current transformer. The power loss of a clamp is determined by subtracting the other circuit losses from the total input power. These other circuit losses, which include core loss of the current transformer, I^2R loss in the windings, and I^2R loss of the conductor, are high compared to the small losses in nonferrous clamps; therefore, this method is not accurate when small clamp losses are measured.

An alternative is to use a low power factor wattmeter with its current coil supplied from a current transformer on the conductor and its voltage coil connected across the clamp.[4] This requires a special meter since the voltage usually is less than two volts.

Paper 49-212, recommended by the AIEE Transmission and Distribution Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Pacific General Meeting, San Francisco, Calif., August 23–26, 1949. Manuscript submitted May 23, 1949; made available for printing June 30, 1949.

V. L. CRABB and J. M. SHEADEL are both with the Ohio Brass Company, Barberton, Ohio.

The authors wish to thank Mr. W. R. Cress of the Ohio Brass Company for his assistance in supervising many of the laboratory tests.

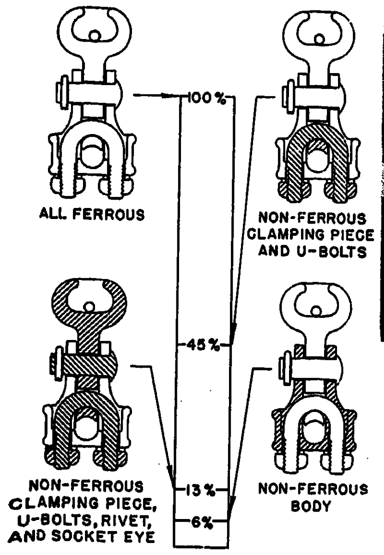

100%
45%
13%
6%
ALL FERROUS
NON-FERROUS CLAMPING PIECE AND U-BOLTS
NON-FERROUS CLAMPING PIECE, U-BOLTS, RIVET, AND SOCKET EYE
NON-FERROUS BODY

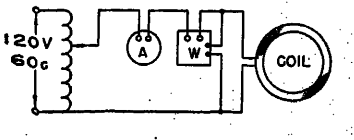
Figure 3. Power-loss reduction with nonmagnetic parts for 1.12-inch seat diameter suspension clamp

CALORIMETER METHOD

For this method a conductor is placed in an insulated water bath. With the test current flowing in the conductor, the time to raise the temperature of the known amount of water a given number of degrees is measured. To reduce error caused by heat transfer between the water bath and surrounding air, the temperature of the water is raised from below room temperature to an equal number of degrees above room temperature. From the data obtained, the watts dissipated in the water are calculated. Two tests are made, one with conductor alone and one with the clamps attached; the power loss of the clamps is the difference of the two values. This method is slow and inconvenient, and requires special equipment.

MULTITURN COIL METHOD

In place of a single conductor, a multiurn coil of insulated wire may be used. The schematic diagram of this method is hown in Figure 4. The clamp to be measured is mounted on the coil as illusrated in Figure 5. With the same ampere-turns, a multiturn coil and a single onductor have equal magnetic force. For example, a clamp placed on a coil of 00 turns carrying one ampere per turn nd the same clamp mounted on a single onductor carrying 200 amperes have equal magnetic power loss.



gure 4. Schematic diagram of power-loss measurement circuit

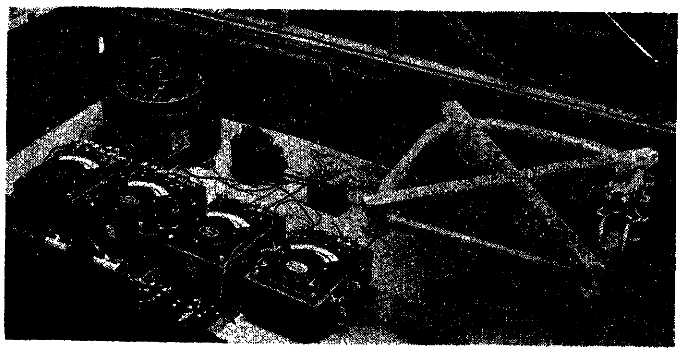


Figure 5 (above). Power-loss measurement circuit

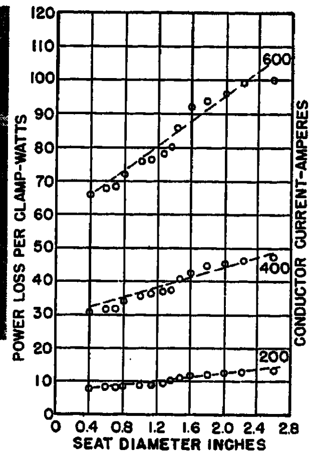


Figure 6 (right). Power loss of all-ferrous suspension clamps versus seat diameter and conductor current

The power loss of a clamp is determined by subtracting the loss of the coil from the loss of the coil and clamp. The coil shown in Figure 5 is wound on a square form to accommodate four clamps per test.

The testing of several clamps at one time improves accuracy when low-loss clamps are measured, since the ratio of clamp loss to coil loss is increased.

Example: To measure the power loss of a 1.12-inch seat diameter nonferrous-body suspension clamp, place four clamps on 150-turn coil. At four amperes (equivalent to 600-ampere line current):

Total input power	52	watts
Coil power loss	31	
	—	
Four clamps power loss (difference)	21	watts
One clamp power loss (21/4)	5.25	watts

The cross-sectional diameter of the coil is determined by the size of the clamp; the number of turns is determined by the power source and required ampere-turns. The coil can be designed for use with a 0–120-volt 60-cycle source, and the shape can be formed to fit the curves of strain clamps.

The power-loss data in this paper were measured with the multiturn coil circuit because of its simplicity and accuracy. The watts loss of other conductor attachments also may be determined by this method. Since normal heating of the line conductor is not present, the multiturn coil is not suitable for temperature-rise measurements.

Power-Loss Values

All power-loss tests were made with 60-cycle current. Figure 6 shows loss measurements of all-ferrous suspension clamps as a function of seat diameter and conductor current. With increasing seat diameter there is a proportional increase in the length of the clamp and the volume of metal; hence, power loss increases.

Figure 7 shows loss measurements of nonferrous-body suspension clamps. For this design the magnetic loss occurs primarily in the steel U-bolts and nuts. The flux path for the U-bolts and nuts includes the air gap between the nuts. For larger clamps, this air gap is longer, and the reluctance is greater. Hence, the power loss of nonferrous-body suspension clamps decreases with increasing seat diameter.

Figure 8 shows power-loss measurements of all-ferrous strain clamps. For a given current a strain clamp has more power loss than a suspension clamp with the same seat diameter, because the former has a larger volume of metal.

Figure 9 shows power-loss measurements of nonferrous-body strain clamps. The power loss of these clamps is small and is determined by the number and size of the U-bolts. The 0.46-inch seat diameter strain clamp has two U-bolts, and the 0.68-inch and the 0.84-inch seat diameter strain clamps have four U-bolts. This accounts for the increase in magnetic loss of the two larger sizes.

Measurements on one size of clamp may vary from the average by plus or minus 10 per cent. These variations result from the air gaps and nonmagnetic galvanizing between the ferrous parts

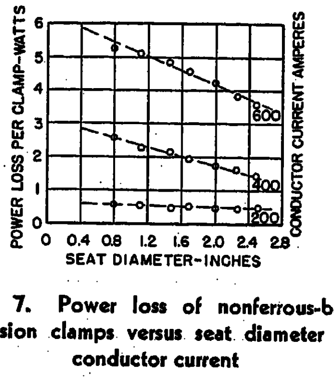


Figure 7. Power loss of nonferrous-body suspension clamps versus seat diameter and conductor current

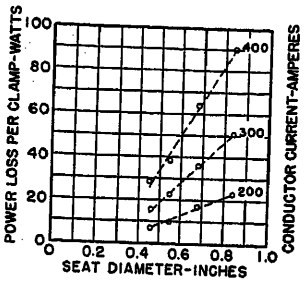


Figure 8 (left). Power loss of all-ferrous strain clamps versus seat diameter and conductor current

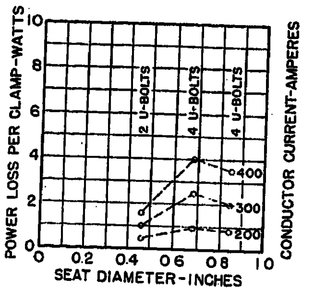


Figure 9 (right). Power loss of nonferrous-body strain clamps versus seat diameter and conductor current

As a clamp is expanded from the minimum to maximum conductor sizes that it accommodates, its magnetic path and series air gaps lengthen. This decreases the power loss.

Temperature-Rise Measurements

Temperature-rise tests were made with both aluminum and copper conductors and both all-ferrous and nonferrous-body clamps. The test conductor formed the secondary loop of a high current transformer, and clamps were placed at least six feet apart, see Figure 10. There was little air movement over the test arrangement. Temperature rise was measured with iron–constantin thermocouples placed between outer strands of the conductor. Values were recorded for both the conductor in air and the conductor inside the clamp. With a constant value of 60-cycle current, several hours were required to reach stable temperature values. Room temperatures were between 70 and 90 degrees Fahrenheit.

As shown in Figure 1, nonmagnetic parts reduce temperature rise. The added heat dissipating area afforded by a nonmagnetic clamp more than compensates for the small clamp power loss; therefore, the temperature of conductor inside the clamp is less than that of the conductor in air. The converse generally is true for the all-ferrous clamps.

Based on test data, a relationship has been developed between clamp and conductor power loss and conductor temperature rise inside the clamp. Temperature rise of a given conductor inside a clamp can be estimated by determining a "Temperature-rise index" for the combination. The temperature-rise index for a given current is calculated from the magnetic power loss of the clamp and the I^2R loss per foot of the conductor by the following equation

$$TRI = K_1 \times P_{cl} + K_2 \times P_{co}$$

where

TRI = Temperature-rise index
K_1 = Clamp loss coefficient
P_{cl} = Magnetic power loss of clamp
K_2 = Conductor loss coefficient
P_{co} = I^2R loss per foot of conductor at 50 degrees centigrade

K_1 and K_2 are given in Table I for seven sizes of suspension clamps. These coefficients apply equally well to magnetic and nonmagnetic clamps and to copper and aluminum conductors.

When armor rods are used, the equivalent resistance of the conductor and armor rod inside the clamp must be calculated. This resistance is used in determining P_{co}.

After TRI is determined, the temperature rise may be obtained from Figure 11. Approximately 85 per cent of our test values lie within 10 per cent of this curve.

Example: To determine the temperature rise of 477,000-circular-mil steel-reinforced aluminum cable (*ACSR*) inside a 1.12-inch seat diameter suspension clamp (all-ferrous) at 400 amperes. The power loss of the clamp, from Figure 6

$P_{cl} = 36$ watts

Calculating power loss of conductor per foot

$$P_{co} = 400^2 \times 0.216/5280$$
$$= 6.55 \text{ watts}$$

The clamp loss coefficient, from Table I

$K_1 = 0.95$

Conductor loss coefficient, from Table I

$K_2 = 1.9$

Calculating temperature-rise index

$$TRI = 36 \times 0.95 + 6.55 \times 1.9$$
$$= 46.6$$

Referring to Figure 11, the temperature rise is 93 degrees Fahrenheit. The temperature rise from actual test, Figure 1, is 96 degrees Fahrenheit.

The estimates by this method for lines in service probably are conservative because of improved radiation factors after weathering and because of air movements over the clamps.

Conclusions

Several methods for measuring power loss in conductor clamps have been compared. The multiturn coil method is recommended for its simplicity and accuracy.

The reduction of magnetic loss in suspension and strain clamps is practicable. The use of a nonferrous body with all other parts iron and steel has proved to be an economical procedure.

Measured power loss values for all-ferrous clamps show that several hundred amperes are required before a significant loss is found. Measured loss values for nonferrous-body clamps show no significant loss.

Because of the magnetic loss characteristics of the clamps, a conductor inside all-ferrous suspension and strain clamps generally will be warmer than the con-

Table I. Coefficients for Suspension Clamps and Conductors Used in Determining Temperature-Rise Index

Suspension-clamp Seat Diameter Inches	K_1 Clamp Loss Coefficient	K_2 Conductor Loss Coefficient
0.7	1.1	2.35
0.8	1.1	2.35
1.0	0.95	1.9
1.12	0.95	1.9
1.4	0.7	1.6
1.8	0.55	1.5
2.25	0.45	1.45

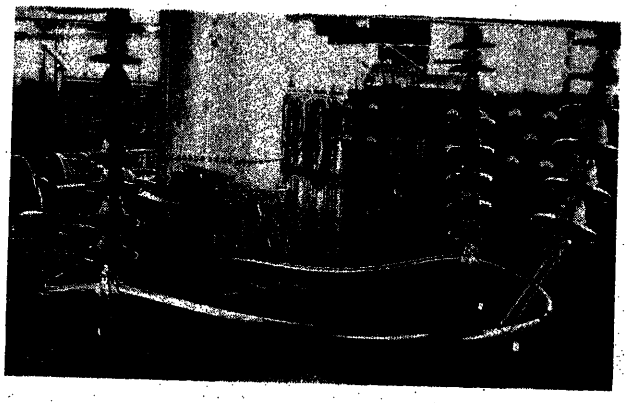


Figure 10 (below). Test arrangement for temperature rise measurements

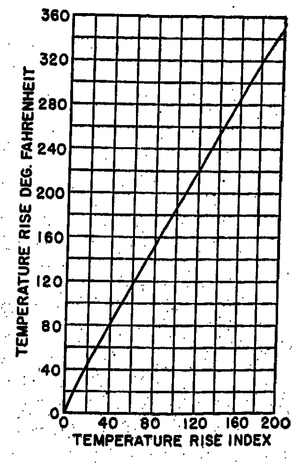


Figure 11 (right). Curve for predicting temperature rise above ambient of suspension clamps ductor in air; a conductor inside nonferrous-body suspension and strain clamps will be cooler than the conductor in air.

A requirement that the clamp magnetic loss be less than 10 watts per clamp at a conductor current of 400 amperes will assure a low power loss and a low temperature-rise characteristic.

For a given current the temperature rise of the conductor inside a clamp is dependent on the conductor I^2R loss and the clamp magnetic loss. From these losses an empirical method of estimating conductor temperature rise values inside suspension clamps has been developed.

References

1. Effect of Magnetic Heating on Cables, W. H. Burleson. *Electrical World* (New York, N. Y.), volume 119, number 12, March 20, 1943, pages 100–102.

2. Joints and Hardware Limit Overhead Conductor Ratings, Leonard M. Olmsted. *Electrical World* (New York, N. Y.), volume 127, number 1, January 4, 1947, pages 42–5.

3. Non-Magnetic Clamps Increase Power Savings, J. M. Sheadel. *Electrical World* (New York, N. Y.), volume 128, number 25, December 20, 1947, pages 86–90.

4. Suspension Clamp Power Loss Tests, Alfio Bissiri. *Electrical World* (New York, N. Y.), volume 129, number 1, January 3, 1948, pages 58–62.

No Discussion

Lightning Stroke Damage to Aircraft

J. H. HAGENGUTH
MEMBER AIEE

Synopsis: The paper describes a series of tests made in the period 1938 to 1942 to determine in the laboratory the effects of lightning strokes on the safe operation of aircraft. Due to wartime restrictions, publication was deferred. Investigated were: the burning of holes in the metal skin of planes and other metallic parts of planes; damage to airplane fuel tanks; breaking of safety glass in windshields; the effect of current flow on ball bearings; effect of impulse sparks on the vision of pilots; and tests on nonmetallic airplane parts. The tests showed that the probability of damage from lightning strokes to airplanes of sufficient magnitude to prevent further operation and to result in loss of the airplane is extremely small and can be discounted.

THESE investigations were carried on in collaboration with the National Advisory Committee for Aeronautics under the direction of Dr. K. B. McEachron to determine whether or not lightning strokes represent a danger to airplanes. It was thought at the time that some unexplained airplane accidents may have been caused by lightning. Any conclusions drawn from the tests are those of the author.

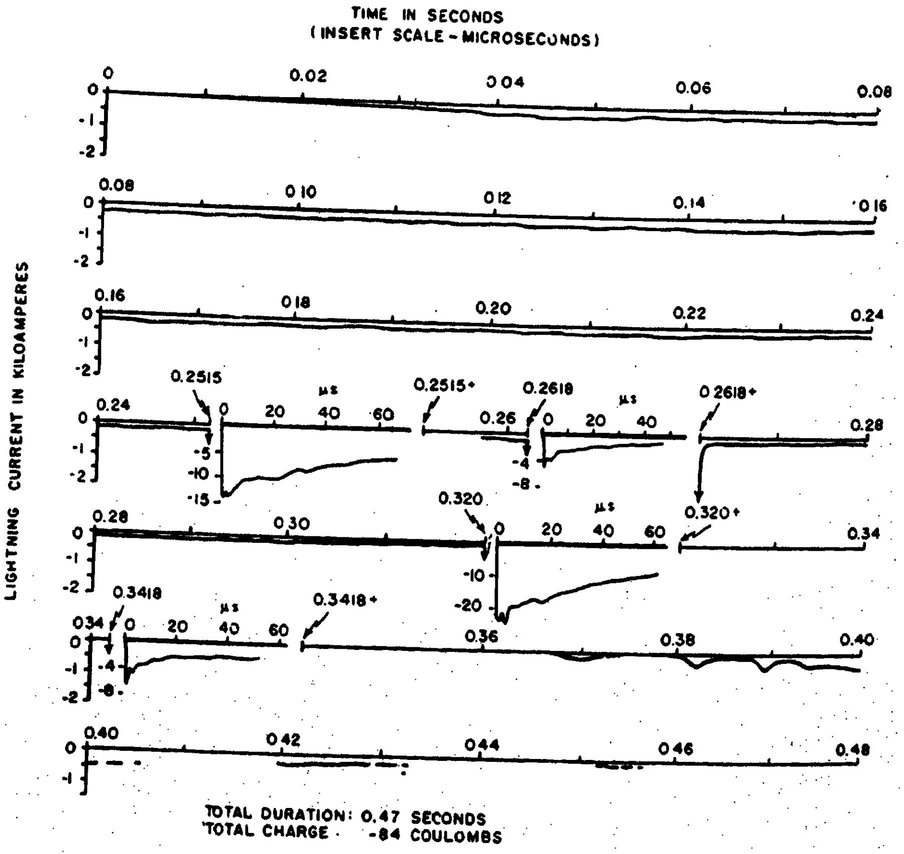
From various lightning investigations it is known that lightning strokes essentially consist of two characteristic forms. The so-called current peaks have relatively high peak amplitudes–average: 15,000 amperes; maximum: 200,000 amperes.

There may be as many as 42 current peaks in a lightning stroke; however, the average stroke has only two current peaks. In the interval between current peaks, so-called continuous current flows through the lightning channel with amplitudes ranging from a few thousand amperes to probably less than one ampere. The duration of these continuing discharges may be as long as a second and a half, while the duration of the current peaks is only of the order of between 20 and 100 microseconds. This describes the normal cloud-to-ground stroke.

Many of the lightning discharges, however, occur between clouds or within the clouds. There is very little known about these discharges, but photographic evidence and theoretical considerations seem to indicate that cloud-to-cloud discharges probably are of the continuing-current type only. Any current peaks are expected to be of low amplitude, probably less than 10,000 amperes. The oscillogram of a typical cloud-to-ground stroke to the Empire State Building[1] in New York City is shown in Figure 1.

There were two reasons for selecting certain elements of the plane for study: to gain statistical data from planes actually struck by lightning as to the type of lightning stroke and its characteristics responsible for the damage observed; and to study the possibilities of lightning damage not before encountered.

Figure 1. Composite plot of continuing currents and current peaks of a lightning stroke to the Empire State Building, New York, N. Y.



Paper 49-214, recommended by the AIEE Air Transportation, and Transmission and Distribution Committees and approved by the AIEE Technical Program Committee for presentation at the AIEE Pacific General Meeting, San Francisco, Calif., August 23–26, 1949. Manuscript submitted January 21, 1949; made available for printing June 30, 1949.

J. H. HAGENGUTH is with the General Electric Company, Pittsfield, Mass.

The tests described in this paper were made by the personnel of the High Voltage Engineering Laboratory of the Pittsfield Works, General Electric Company. The author wishes to express his appreciation, in particular to C. R. Craig, W. C. Lesher, C. McIntosh Jr., R. A. Johns, J. W. Flowers, R. B. Gustafson, I. B. Johnson, A. F. Rohlfs, and M. E. Noon.

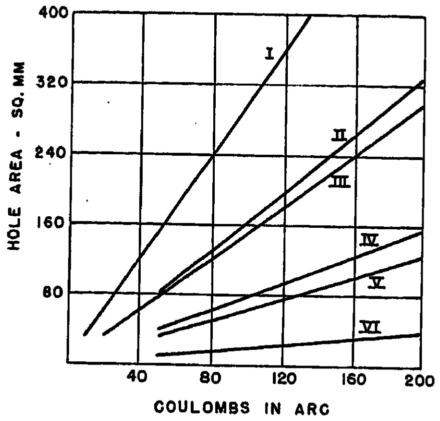


Figure 2. Relation between coulombs in the arc and the size of hole burned in metal sheets. Arc current amplitude ranges between 50 and 1,000 amperes

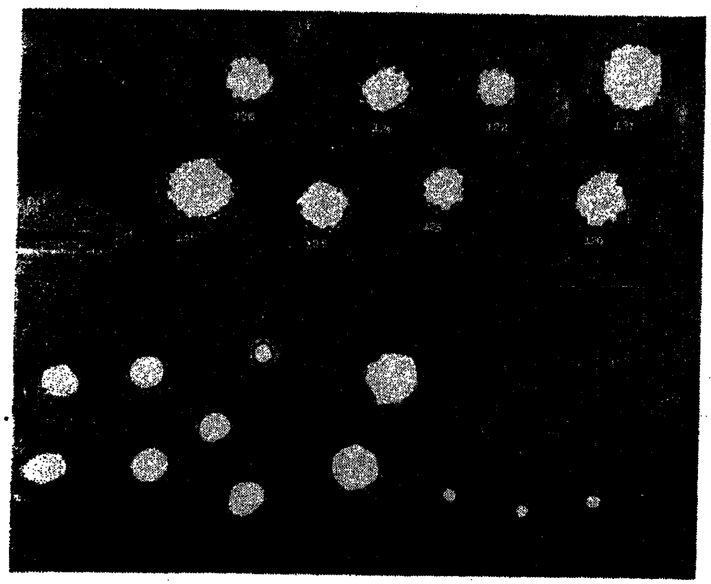
I. Stainless steel–10 mil
II. Galvanized iron–15 mil
III. Copper–20 mil
IV. Stainless steel–40 mil
V. Aluminum–51 mil
VI. Aluminum–100 mil

Subject to study were the following items:

1. Burning of holes in the skin and other metallic parts of airplanes or distortion of metal parts.
2. Damage to airplane fuel tanks.
3. Breaking of the safety glass in windshields.
4. Effect of current flow through ball bearings of the control system.
5. Effect of the light from a lightning stroke on the vision of a pilot.
6. Effect of exhaust gases.
7. Nonmetallic airplane parts.

Figures 3. Samples of holes burned with long-time laboratory discharges

Top: 20-mil copper sheet
Bottom: 25-mil aluminum sheet



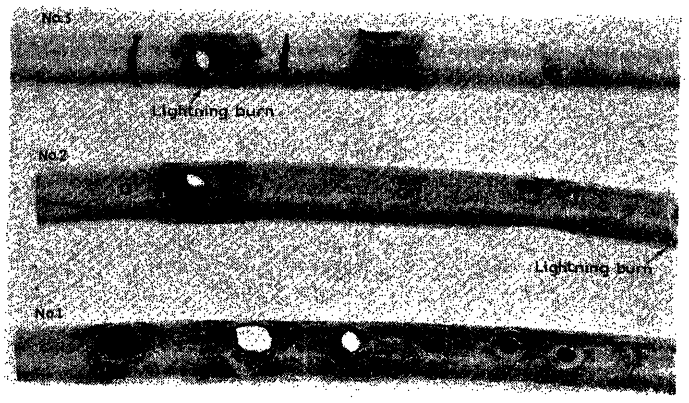


Figure 4 (right). Holes burned in aluminum rudder bow tubing by actual lightning stroke and by laboratory arcs. 25-mil wall thickness, 1-inch tubing

Burning of Holes in Metal Parts of Airplanes

In a previous paper[2] the effect of lightning on metal sheets was discussed for copper and steel sheets. It was shown that the principal effect of lightning on metal sheets is from the continuing type of discharge, the result being a hole burned in the metal. The work was extended to aluminum and steel sheets using the test circuit described previously. Figure 2 shows the results for all metals tested.

The type of metal appears to have very little influence. In every case the size of the hole burned is directly proportional to the coulombs or ampere-second flow of the arc current into the sheet. The size of the hole burned in a sheet of a given thickness can be expressed by two equations which give fairly accurate results

$$A = 25.3 \times C \times t^{-0.9} \text{ for } t = 0 \text{ to } 35$$

$$A = 245 \times C \times t^{-1.54} \text{ for } t = 35 \text{ to } 150$$

where

A = area of hole burned in square millimeters
C = coulombs in arc
t = thickness of sheets in mils

The appearance of the holes burned differs. Copper produces bead-like globules at the edges; also, stainless steel, with a reduced number, while aluminum rarely had any beads. For aluminum, a crater-like partially burned area usually occurred, in addition to a well-defined hole. The area of this crater is approximately 2:1 for 25-mil sheets and about 6:1 for 100-mil sheets. The difference in the appearance of the holes burned is shown in Figure 3.

The more exposed regions in a plane are the relatively sharp edges of control equipment, such as rudders and flaps. The metal at the trailing edges of these devices sometimes is formed with circular tubing or with trapezoidal sections offering a double surface opposite to the lightning stroke.

Typical lightning burns and test burns are shown in Figure 4 on 1-inch diameter aluminum tubing with 25-mil wall thickness. The size of the hole burned in the surface facing the test electrode is approximately equal to that burned in sheets of the same thickness. Up to approximately 50 coulombs, only a single hole is burned. For higher coulombs, holes also are produced on the opposite side of the tubing. The worst type of hole burned by natural lightning is shown in Figure 5. In this case the tubing was almost burned in two. It is estimated that this damage was caused by a lightning stroke conducting approximately 300

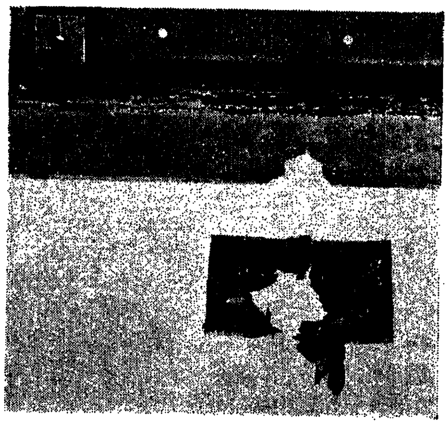


Figure 5 (right). Holes burned in a wing edge (top) and a rudder bow (bottom) by natural lightning. Estimated 300 coulombs in lightning stroke for both cases

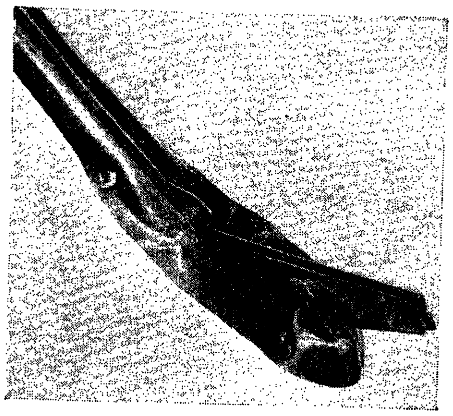


Figure 6. Rudder protector tip damaged by natural lightning stroke consisting of at least one high current peak in excess of 100,000 amperes and a continuing discharge of probably 500 coulombs

coulombs. However, even with the severe damage inflicted in this case, the plane remained under control.

Other parts on planes burned by lightning strokes are pitot tubes, used for determining the velocity of the plane. It is conceivable that the accuracy of the measurements made is impaired by holes burned in strategic places of the tubes. This is, however, not considered to have a serious effect on the safety of the plane.

In the cases where holes are burned in the metal sheets, it is impossible to tell whether or not current peaks were involved in the stroke, since current peaks leave only small bead-like marks which would be obliterated by the holes burned with continuing discharges.

In the case of the protector tip, shown in Figure 6, there is evidence that the stroke causing the damage was a combination of high current peaks and continuing discharges. The fact that the metal was burned greatly at the end of the protector tip indicates a continuing discharge of approximately 500 coulombs. The fact that the static mast itself is pinched together and that its support was lifted from the rudder bow, indicates that at least one current peak in excess of 100,000 amperes was involved. Also, the front of this current peak must have been relatively steep to concentrate most of the discharge flow into the plane proper at the tip end of the rudder, where the high current density of the spark between rivets and the structural members of the rudder caused sufficient pressure to rupture the bond between the members and caused the upturn of the edge metal. However, even with such a severe stroke, the damage caused was insufficient to prevent safe operation of the plane.

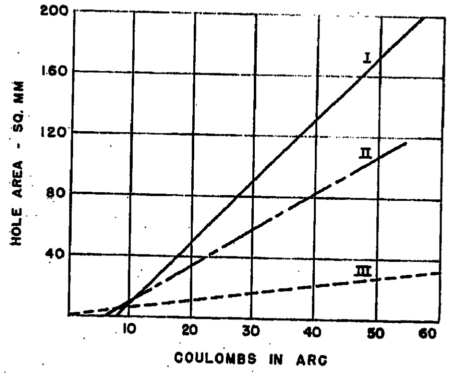


Figure 7. Effect of contents of 10-mil quart tin cans on holes burned by d-c arcs

- I. Empty cans
- II. Cans filled with Sovasol, arc above liquid level
- III. Cans filled with Sovasol or water, arc below liquid level

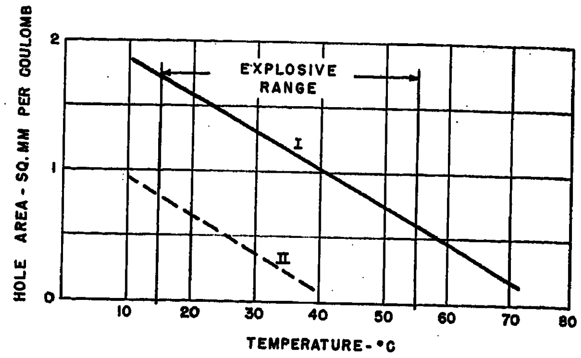


Figure 8 (right). Effect of temperature of liquid on hole size burned by d-c arc in 10-mil quart tin cans

- I. Arc above liquid level
- II. Arc below liquid level

The type of damage described requires the flow of considerable charges. The smallest hole that could be produced in a 25-mil aluminum sheet in the laboratory was 15 square millimeters, requiring approximately 20 coulombs. Also, in order to burn holes, the heat must be fairly concentrated. If the current in the stroke decreases to too low a value, the same number of coulombs required to burn a hole through the metal for the larger current will only scorch the sheet, because of the much longer time required for the flow of current. Heat can be dissipated more readily to the surrounding areas. It is, therefore, inconceivable that the relatively minute currents involved in corona discharges on planes traveling through highly active lightning-storm regions could cause burning of holes.

One rather interesting fact which follows from the damage on planes as described, is the fact that lightning strokes appear to have a tendency to hang onto the point contacted without materially shifting the point of contact.

For instance, in the case of the protector tip with 500 coulombs of charge, it it estimated that the duration of this stroke was at least of the order of one second. This follows from evidence obtained at the Empire State Building[1] and elsewhere[3] that continuing discharges, at least to ground, are of the order of 500 amperes and less, and there is no evidence that cloud-to-cloud discharges of the continuing kind would carry higher currents of long duration.

A plane traveling at the rate of 200 miles per hour would cover approximately 290 feet in one second. Considering the action of the wind under thunderstorm conditions, it is remarkable that the stroke terminal remained fixed to the one point of the plane.

Damage to Airplane Fuel Tanks

The burning of holes in the metal skin of gasoline tanks was investigated, particularly in view of the added danger of burning of the gas, or even explosion. Ordinary tin cans of 10-mil wall thickness, and small aluminum tanks of 51-mil wall thickness were used. Furthermore, the effect on double-wall tanks was investigated.

Sovasol and sovatone were used for

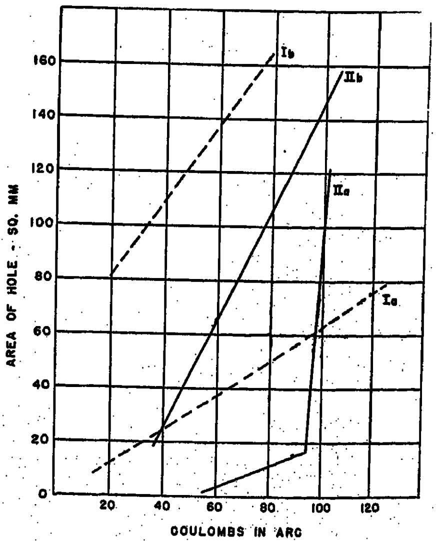


Figure 9. Aluminum alloys may show variations in hole size burned by d-c arcs. Sheet thickness: 51 mils

- I. Aluminum alloy *A*
- II. Aluminum alloy *B*. Curve (*a*) hole area curve (*b*) hole and surrounding melted area

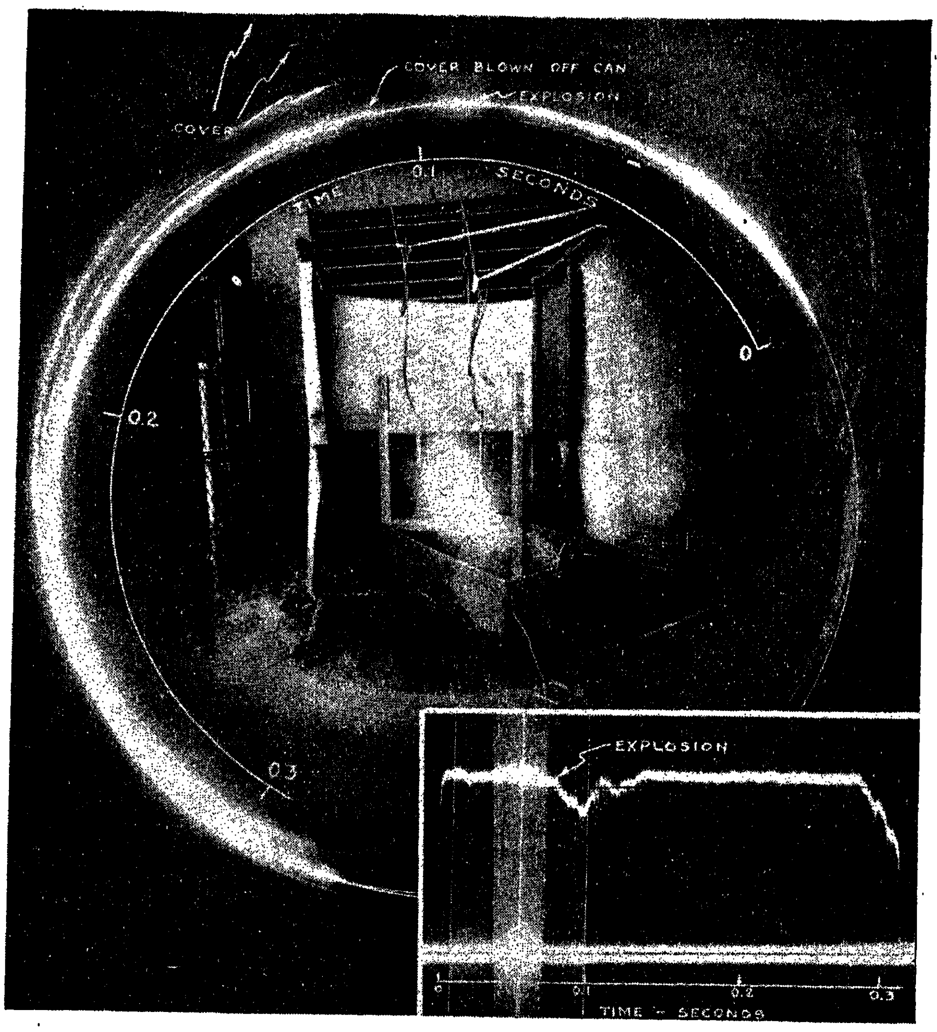


Figure 10. Photograph of arc test on gasoline tanks

Center: still picture. Circle: reproduction of rotating lens photograph showing the arc duration and the flight of the cover blown off by explosion

preliminary tests because their explosive range lies between 15 and 50 degrees centigrade, convenient for outdoor testing at Pittsfield. Since the explosive range of 100-octane gasoline is between −20 and −40 degrees centigrade, artificial cooling of the tanks with dry ice was required previous to the tests.

The results obtained differ considerably from those obtained with sheets previously tested in the following respects:

A marked effect was found on the size of the hole which could be burned in the tin cans, depending on whether the can was empty, partly filled with liquid with an arc applied to the space above the liquid, and completely filled with the arc applied below the liquid level. This is illustrated in Figure 7.

Sufficient data were available from tests with sovasol and tin cans to determine that the holes were largest at the lower end of the explosive range and gradually decreased as the upper range was approached. This is particularly pronounced for the case of the cans where the arc was held below the liquid level, as shown in Figure 8.

Under "Burning of Holes in Metal Parts of Airplanes," it is stated that the hole size is almost independent of the type of metal used. The principal difference for aluminum was that in addition to a hole, a much larger melted area was produced surrounding the hole. With the types of alloys used for the test tanks, however, the relations for the size of hole burned, as well as the melted area, were considerably changed, as shown in Figure 9. The alloy used for the gas-tank tests produced holes of about 1/10 the size previously obtained up to about 60 coulombs, and then rapidly increased to about double size at 100 coulombs. This seems to indicate that certain alloys may be available where even greater values of coulombs may be required to burn holes into the sheet.

However, when the tanks were partially filled with gasoline, at a temperature within the explosive range, holes were burned in some cases of about the same size as the melted area in the empty tanks. In other cases, no holes were produced applying arcs of 73 coulombs well above the point where holes could be produced. These examples show that under such conditions, the holes burned in metal sheets cannot be accurately evaluated as to the coulomb content of the arc causing the damage.

The explosive effects observed during these tests can be summarized as follows:

With tightly closed tin cans, violent explosions could be produced if the arc were struck above the liquid level, resulting in blowing the top off the can, or, in some cases, ripping open the seam. The small aluminum tanks were bulged considerably.

When the gas tanks were vented by even small holes, any explosion which occurred usually would vent itself by means of a violent flame of high intensity without any further visible evidence of structural damage. However, evidence indicates that the vent opening must be of sufficient size. The amount of gas available for the explosion undoubtedly would determine the size of the vent necessary to prevent an explosion.

When the arc was applied to the can or tank below the liquid level, no explosions were observed, although frequently but not always, fires were started.

Photographs were taken with a medium speed Boys camera[4] to show the duration of the arc, supplemented by magnetic oscillograms to determine the coulombs in the arc. A typical picture and oscillogram are shown in Figure 10, which shows the close correlation between arc light and current flow. This picture is of special interest because the cover of the tin can was blown off and can be seen in various positions during its flight in the top left-hand corner.

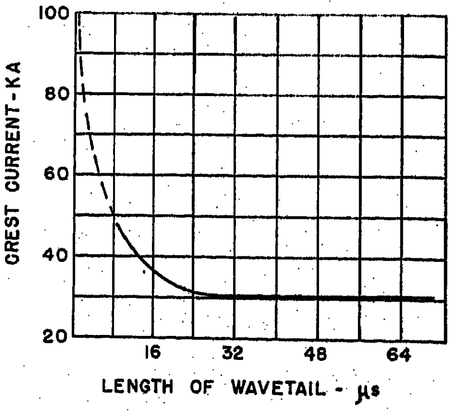


Figure 11. Effect of length of surge current wave tail on amplitude required to break ordinary 1/8-inch-thick window glass

The effectiveness of double walls to prevent arcs coming in contact with the volatile liquids in gasoline tanks was investigated, using stainless steel sheets of various thicknesses and spacings between sheets without the presence of gasoline or other liquids. These tests were made applying an arc of approximately 110 coulombs to one sheet, the other sheet being spaced at varying distances. Tests made with the second sheet grounded or isolated did not show much difference. The results of the tests indicate certain minimum spacings between sheets required to prevent hole burning in the second sheet, as shown in Table I for 110 coulombs flowing in the arc.

For extreme lightning conditions exceeding 110 coulombs, more liberal spacings would be required.

The chances of severe damage to a plane from exploding gasoline caused by a lightning stroke, however, are extremely small because:

1. The fuel tanks usually are mounted in the flat parts of the wings where lightning strokes are not expected to strike.

2. The gasoline tank must be partially empty and the vapor space above the liquid must be at a rather critically low temperature.

Glass Breaking Tests

While it is highly inconceivable that a lightning stroke would occur close to and across the cockpit windows, it was desirable to determine the effect of such strokes on glass. The probability of such an occurrence is very small because the windows are completely enclosed and generally relatively small—of the order of 12 inches high. It is known that corona streamers play on the surface of the glass due to the falling of charged rain drops on the glass. Such streamers may increase the chances of a close stroke across the cockpit window. Recently, a report was received of a windshield so badly shattered that the pilot could not see through the window. The plane, of course, landed safely.

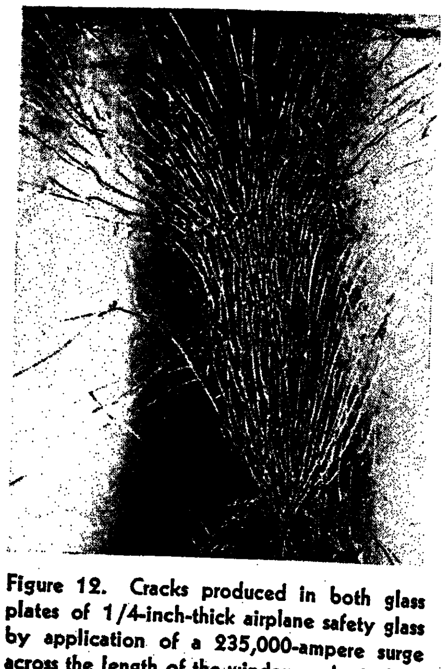


Figure 12. Cracks produced in both glass plates of 1/4-inch-thick airplane safety glass by application of a 235,000-ampere surge across the length of the window; plastic filler intact

It quickly was found that exceedingly high currents are needed to damage safety glass. The safety glass–automobile or airplane—used in these tests consisted of two panes of 1/8-inch thick glass, joined by a plastic filler, giving it a total thickness of 1/4 inch. A number of exploratory tests, therefore, were made with ordinary glass, because with the much lower currents required, the flexibility of the surge current generator was sufficiently good to apply waves of various shapes at different amplitudes.

Some of the principal requirements were:

1. The impulse spark had to be relatively long, covering practically the whole pane to produce cracking at low current, while with a short spark of one to two inches in length, currents of about 90,000 amperes were required; with long sparks in the proper position, only 50,000 amperes were required.

2. In order to produce the longer sparks, a fuse wire (0.002-inch manganin) was used to start the spark. The highest voltage of the surge current generator was only 150 kv and sufficient only to spark over a few inches. This wire was mounted at an angle of 30 degrees with respect to the glass to obtain optimum conditions. The top electrode, therefore, was mounted at a distance of less than one inch from the glass. This made it necessary to omit the grounded frame at the top and the sides, and for practically all tests, only the bottom end of the glass plate was laid in a grounded frame.

3. For most tests, the glass plates were mounted in front of a small wooden box to prevent rapid equalization of pressures on the two sides of the glass. It was found that the use of the box as compared to an open glass plate did not result in materially lower crest currents.

4. Although not many tests are available, it appears that current wave shape, particularly the wave tail, has a significant effect on the crest current required to break the glass. This is illustrated in Figure 11. The longer the wave tail of the surge current, the lower is the crest current required to break the glass.

The same criteria probably apply to

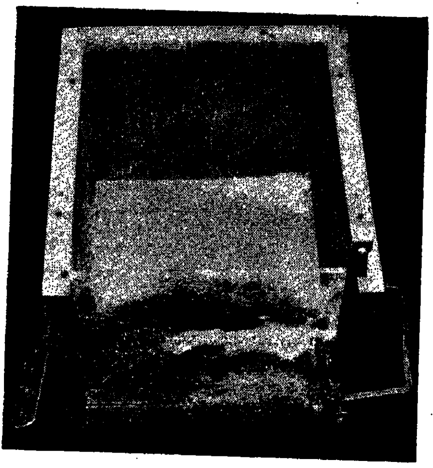


Figure 13. Test box with 1/4-inch-thick auto safety glass shattered by one 235,000-ampere surge; plastic filler intact

Note glass splinters in box and embedded in rear wood panel

Table I. Minimum Distance Between Stainless-Steel Sheets Required to Prevent Hole Burning in Second Sheet when 110-Coulomb Arc is Applied to First Sheet

Thickness of Sheets Mils	Spacing Between Sheets Inches
	Greater than:
16	1 1/4
20	3/4
30	3/8
40	1/4

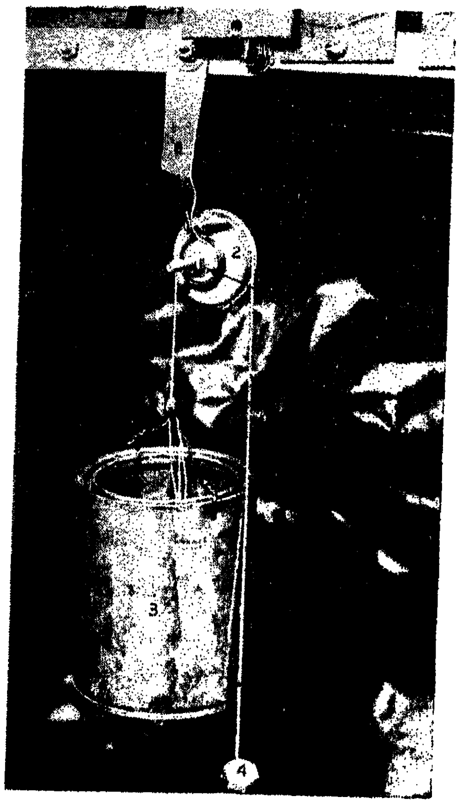


Figure 14. Test setup for ball bearings

1. Bearing
2. Pulley
3. Weight Pan
4. Counterweight

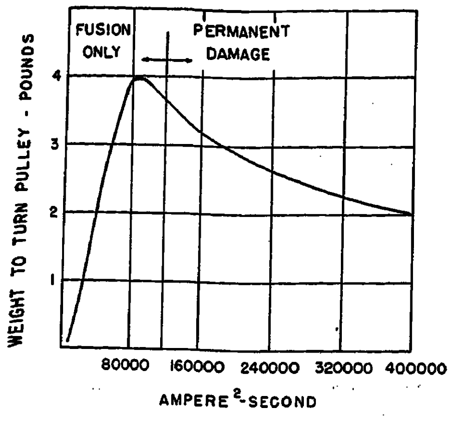


Figure 15. Relation between weight required to free ball bearing after having been subjected to high current discharges as function of amperes-squared seconds

safety glass. However, the strength of this glass with regard to exposure to high surge currents is so great that similar experiments could not be made because the available equipment was not sufficiently flexible, although the surge current generator was capable of producing a crest current of 265,000 amperes during oscillatory discharge.

For safety glass, either airplane or automobile—used because of ready availability—about 170,000 amperes were required to crack the glass on one side; 235,000 amperes to produce cracks on both sides (Figure 12) but the plastic filler remained intact. The mechanical strength of the airplane safety glass appeared superior to that of the automobile glass used.

It required several successive surges of 235,000 amperes to crack the safety glass completely, including the plastic filler. The glass and plastic filler could be broken if the surge current were made to penetrate the window to a grounded point on the side opposite from the origin of this spark.

Lightning currents of this amplitude, if they exist at all—maximum reported 220,000 amperes[5]—would be exceedingly rare. The possibility of repeated surges of this amplitude is believed nonexistent. The direct stroke through the window is inconceivable. Twelve-foot long sparks to a cockpit invariably contacted the metal rim around the window.

Factors, such as the wind stream, action of rain or hail as might be encountered in a plane in flight, were not reproduced, because the threshold current was found to be so high. One possible source of trouble is illustrated in Figure 13, which shows glass splinters embedded in the back part of the wooden box, although the glass itself was not completely ruptured. With modern planes—the automatic pilot and a co-pilot being provided—this should not contribute to destruction of a plane, unless during critical periods the co-pilot should not be available.

Effect of Current Flow Through Ball Bearings of the Control System

Rudders, ailerons, and other control surfaces are hinged to the body of the plane by means of ball bearings of rather small size. Previous tests had shown that fusion could result from d-c arcs, similar to continuous current discharges, on counterpoise gaps described elsewhere[6] (Figure 2b, reference 6). Tests on the bearings showed a complete immunity to continuing discharges, at least up to 300 coulombs, although fusing of the 1/16-inch gap (Figure 2b, reference 6) would occur with only 70 coulombs. On the other hand, high current discharges had effects on the bearings which definitely impeded their operational quality.

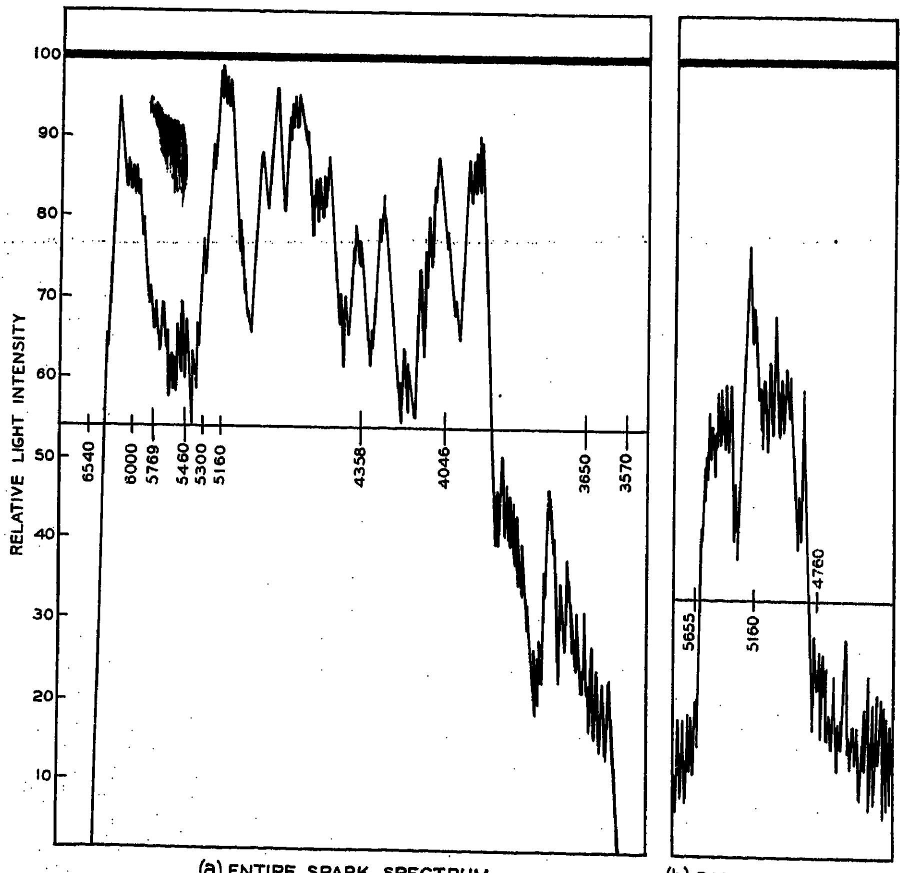
The test setup was not elaborate, as shown in Figure 14. The bearing was suspended, fixed at the outer race. A pulley was mounted on the shaft carrying the inner race and held in equilibrium by an empty can and a counterweight. After the test, small pellets were slowly poured into the can to determine the weight required to break the bearing loose, if the current had caused freezing of the balls to the races. The weight of the pellets is, of course, proportional to a steady force required to break the bearing free. The data obtained on 18 bearings can be presented in various forms, such as the effect of coulombs in the arc, which is unsatisfactory because low current discharges of high coulomb value did not harm the gap; or crest current in high current discharges, which results in rather meaningless variations of between 0.1 pound to 3.4 pounds to free the bearing after a 60,000-ampere discharge, depending on whether the surge was unidirectional or oscillatory. The relation which fitted all test results best is the weight required to free the bearing as function of the energy through the bearing expressed in amperes squared seconds, as shown in Figure 15. A maximum weight is required at 90,000 amperes-squared seconds. Further increase in energy increased the physical damage but did not require greater weights.

Current peaks of at least 40,000 am-

Figure 16. Microdensitometer analysis of impulse spark spectrum between brass electrodes

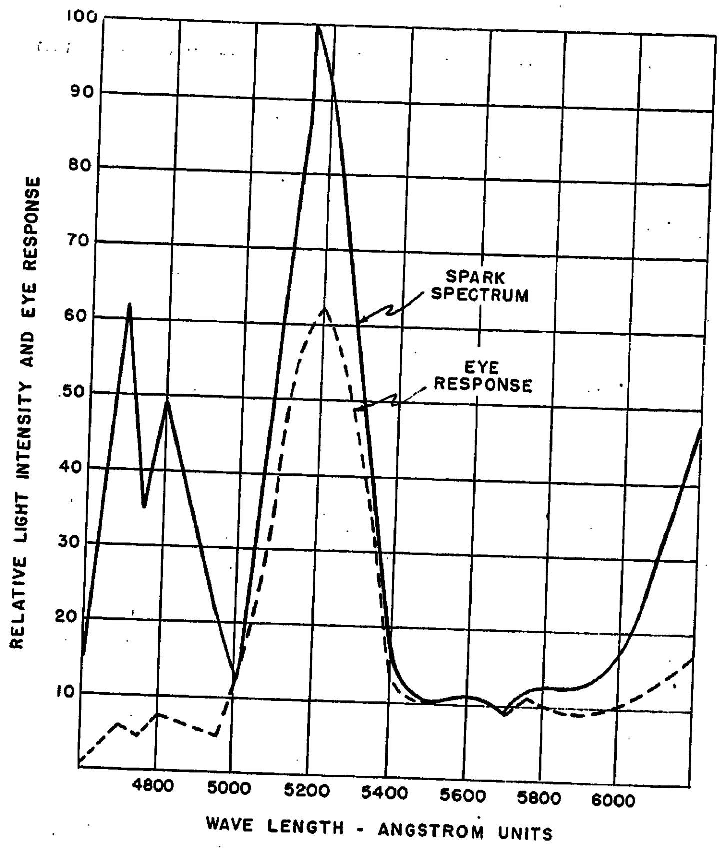
A. Without filter

B. With Burtweld filter



(a) ENTIRE SPARK SPECTRUM

(b) PORTION OF SPECTRUM TRANSMITTED THROUGH BURTWELD LENS



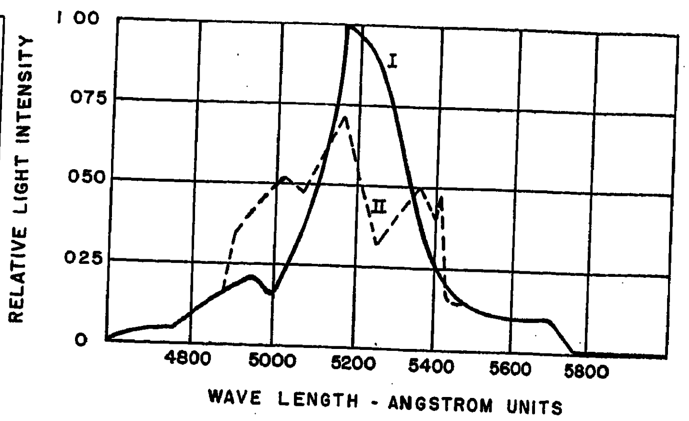


Figure 17 (left). Approximate relative light intensity produced by a 100,000 - ampere crest oscillatory surge current between electrodes at various wave lengths and eye response to spark

Figure 18. Approximate relative light intensity of same spark as Figure 17, but filtered through Burtweld lens

I. Calculated
II. Test

peres crest are required to prevent free spinning of the bearing. Considering that lever arms are provided, the force required to free frozen bearings would be insignificant judging by the small weights required to loosen the bearings. Some reduction in current flow through individual bearings results from the multiplicity of bearings provided in a plane. One set of bearings struck by lightning, from the same plane as the static mast of Figure 6, reacted similarly as the test bearings. No difficulty from this source was observed during the flight.

Effect of Lightning from a Lightning Stroke on the Vision of the Pilot

These tests were made on a qualitative basis to determine the effect of the light emitted by high current discharges, as well as the continuing type of discharge, on the vision of observers when their eyes were protected by various types of filters. For medical reasons the effects on unprotected eyes were not investigated.

Two types of tests were made, one to determine the physical effects on the vision and the other to determine the optimum filter specifications by means of spectral analysis and known eye response to the various wave lengths of light.

The spectrum analysis of a 100,000-ampere surge current discharge through a 3-inch long gap revealed prominent lines comparing well to Fox's[7] spectrum of an actual lightning stroke. Differences in relative intensity probably are due to: different color sensitivity of the film used; effect of intermediate air conditions between lightning stroke and camera, such as haze and rain; metal vapor from the electrodes drifting into the spark space—prominent copper lines at 5,105, 5,153, and 5,218 angstrom units might account for the high intensity lines of the spectrum obtained at 5,160 angstrom units.

Figure 16 (A) shows the relative light intensity of the spark spectrum as obtained with a microdensitometer. A Burtweld lens, developed principally for welding operations on aluminum, had a relative light intensity of transmitted light as shown in Figure 16 (B), indicating a great reduction in band width, as well as in intensity of the highest peaks.

Correcting for the color sensitivity of the film and using the average eye response to various wave lengths, the data are shown in Figure 17 for the unprotected eye and in Figure 18 for the eye protected by the Burtweld lens. The tremendous reduction in peak intensity, as well as in integrated light transmission by means of the lens, is evident from Table II. This table is of some value when considering the results of tests to determine the physical effects of blinding. In these tests the spark was observed through a filter from a distance of 5.5 and 12 feet. Results showed no fundamental difference in blinding effect for the two distances.

The blinding effect was determined by focusing the eye upon the gap. Immediately after the discharge, an attempt was made to read fine print. The image formed by the spark on the fovea of the eye (the most sensitive part of the eye, located at the center of the retina) generally prevented reading until the image had disappeared. The time required to enable the observer to read was considered a measure of exposure. With the Burtweld lens, a recovery time of between 29 and 47 seconds was required to restore the eye to normal visibility. Considering the great difference in eye response with and without the filter as shown in Table II, it is easily seen that an unprotected eye could be blinded for a considerably longer time. From actual flight experience it has been claimed that complete blindness existed for a period of ten minutes.

The effect of continuous discharges on the visibility was tested with a 1-inch gap consisting of one carbon and one aluminum electrode conducting an arc of 110 coulombs. The blind time with the Burtweld lens was approximately the same as for the 100,000-ampere discharge. This comparison strongly suggests that the in-

Table II. Comparison of Light Intensities

	Maximum	At Angstrom units	Integrated
Spark	100	5,160	100
Eye response	62	5,210	53
Transmitted through Burtweld lens	7.6	5,160	10
Eye response to spark through Burtweld lens	6.5	5,210	4

tegrated light intensity accounts for the blinding effect rather than he peak intensity.

The probability of such an occurrence is very small, because in order to result in momentary blindness, the eye must be focused on the discharge channel and the channel must be close to the pilot. The fact that a co-pilot and the automatic pilot are available reduces still further the possibility of accident due to the blinding effect of the channel. This fact and the greatly reduced visibility under normal conditions with a filter such as the Burtweld lens, made it appear impractical to recommend the use of such lenses.

Effect of Exhaust Gases on Guiding a Stroke to a Plane

Tests using actual exhaust gases were not made. Preliminary investigations on the effect of heat and possible ionization from a gasoline torch gave some interesting results. The test setup is shown in Figure 19, consisting of a line electrode and two identical grounded gasoline torches. Both torches had a 2-inch wire electrode projecting above the torch barrel. The left torch was lighted to produce a 5-inch flame. The large curvature of the impulse spark to the left electrode is evidence of the influence of the heated gases on the sparkover path. By substituting a 7-inch wire extension for the flame, a similar sparkover path was obtained. Temperature measurements above the flame, shown in Figure 20, indicate a temperature of 450 degrees centigrade at seven inches from the torch barrel. As far as the impulse spark is concerned, gas temperatures of 450 degrees centigrade and higher seem to be as effective in spark conduction as a metallic wire.

Further sparkover tests with 60-cycle test voltage indicated a reduction in sparkover voltage to approximately 70 per cent due to the influence of the flame. Most of this reduction can be explained by reduction in breakdown strength due to the temperature of the gas space above the flame on the basis of the influence of relative air density on the breakdown.

Thus, a definite effect of temperature was shown to exist in determining the breakdown path in a gap. The tests are inconclusive as far as planes are concerned because many of the factors are unknown; for instance, heat distribution of exhaust gases behind a plane. The fact that planes ordinarily do not get struck at the exhaust would indicate to some extent that the attractive effect of the exhaust gases is not as predominant as in the case of the tests described.

Nonmetallic Airplane Parts

The investigatory tests previously described have clearly shown that airplanes surrounded by metallic skins were practically immune against lightning strokes. However, wooden planes or planes built up of plastic materials were expected to be rather vulnerable because in these planes, metallic objects, such as engines and electrical and mechanical control wires, would attract the stroke, which then had to penetrate the insulating outer structural parts to reach the metal components.

As expected, the damage which can be caused by heavy current discharges through nonmetallic airplane parts is extensive, as illustrated in Figure 21 which shows the destruction of a plywood wing section due to a 20,000-ampere surge current discharge through the wing. The heavy damage caused is due to the fact that the destroyed section was not vented. Tests on a plastic-impregnated fabric flap with a 20,000-ampere current discharge produced only a few small holes. Most of the discharge in this case occurred over the outside of the flap, and sufficient ventilation was provided to prevent greater structural damage due to the gas expansion from the portion of current flowing internally. When the ventilation holes were plugged up, the fabric pulled away from its supports at the edge, but was not damaged to any greater extent than shown in Figure 22. However since the surge current amplitude was only 20,000 amperes, it is evident that with these and larger currents through sections of a nonmetallic plane, sufficient damage can be caused to make the plane inoperable.

The effect of shielding cables was investigated on a cardboard model 25 inches long, with a wingspread of 28

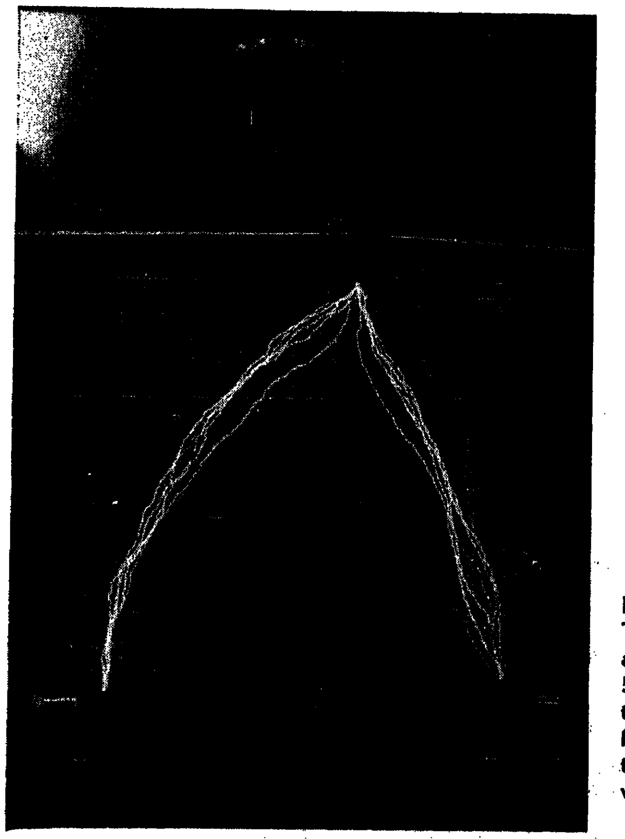


Figure 19 (left). Effect of 5-inch flame (left) on path of impulse sparkover over a 5-foot gap

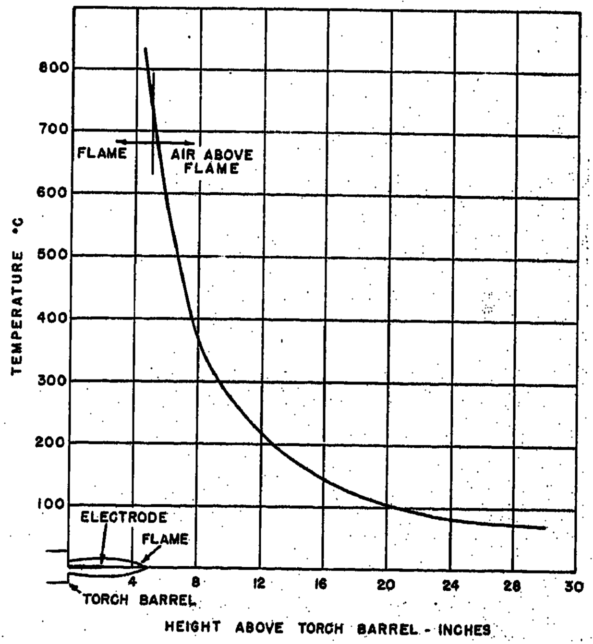


Figure 20 (right). Temperature in air space above 5-inch gasoline torch flame as function of distance. Flame in vertical position. See Figure 19

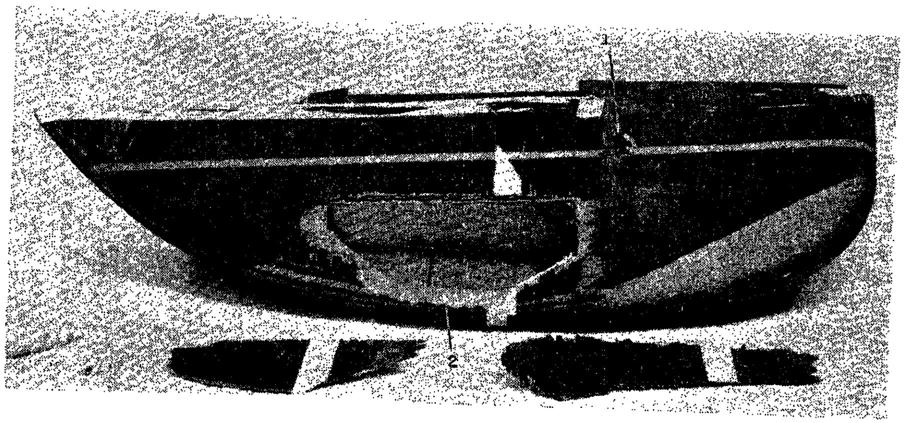


Figure 21. Shattering of plywood wing section by a 20,000-ampere surge between points 1 and 2

inches. Even with shielding wire following the contours of the model and a longitudinal wire through the center on the side facing the line electrode, puncture often occurred when the spark contacted the center of the model, as shown in Figure 23. When both sides of the plane model were wired, punctures did not occur, although the cardboard was scorched at the point of contact.

These tests show that rather elaborate protection must be provided in actual planes to prevent sparking inside the plane proper or sideflashes from the protective wiring to internal metallic parts.

Conclusions

1. This series of tests has shown that although the possibility exists that a lightning stroke may cripple an all-metal plane, the probability that all factors required occur simultaneously is so small that the lightning stroke can be disregarded as a serious potential hazard to a properly protected plane.

2. Evidence obtained from airplane parts struck in flight, when compared with laboratory tests on the same or similar parts, indicates that planes may be subjected to current peaks of strokes to ground of the order of 100,000 amperes and to continuing discharges or the order of 500 coulombs.

3. Nonmetallic planes, unless well shielded with a network of wires or other protective coating, are subject to damage from even minor lightning strokes of the order of 20,000 amperes.

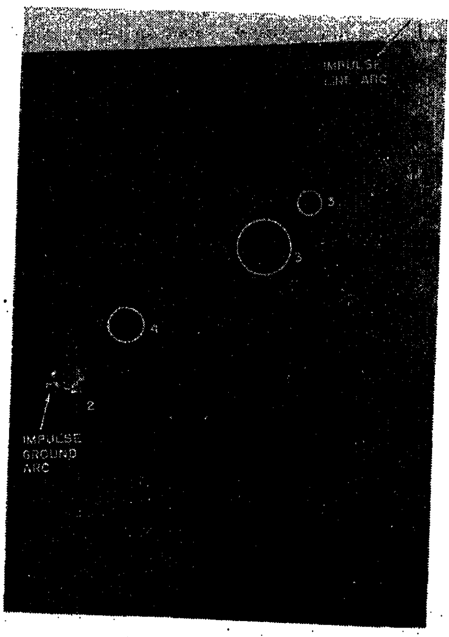


Figure 22. Small holes produced in ventilated plastic-impregnated fabric flap by a steep-fronted surge of 20,000-ampere crest

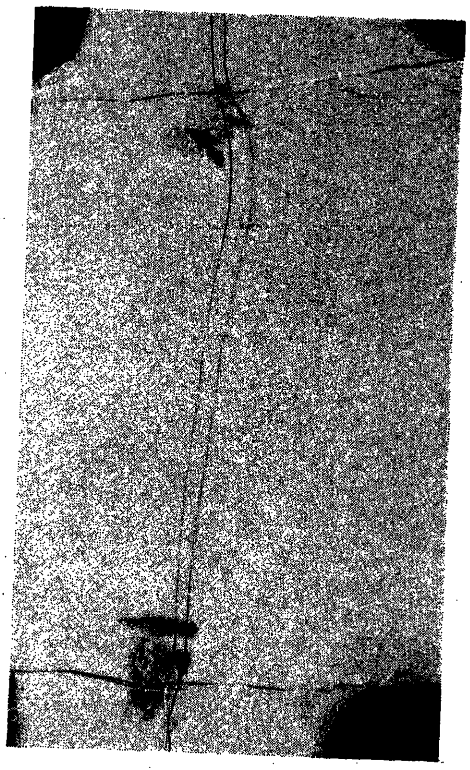


Figure 23. Holes produced in pressboard model plane by surge currents

Plane outline wired. Longitudinal and cross wires on one side only. Plane suspended in center of an 8-foot gap

4. Considering the other factors associated with lightning strokes, such as violent air drafts, rain, and icing, it would be considered safe policy to avoid areas of lightning activity.

5. Pilots should well understand the potential dangers as shown in this paper; to be prepared in case the unexpected, rare combination of several of these factors should occur.

References

1. Lightning to the Empire State Building, K. B. McEachron. *Electrical Engineering* (*AIEE Transactions*), volume 60, September 1941, pages 885–90.

2. Effect of Lightning on Thin Metal Surfaces, K. B. McEachron, J. H. Hagenguth. *Electrical Engineering* (*AIEE Transactions*), volume 61, August 1942, pages 559–64.

3. The Measurement of Lightning Currents in Direct Strokes, G. D. McCann. *AIEE Transactions*, volume 63, 1944, pages 1157–64.

4. Lightning Recording Instruments, J. H. Hagenguth. *General Electric Review* (Schenectady, N. Y.), volume 43, numbers 5 and 6, May and June, 1940, pages 195–201 and 248–55.

5. Lightning Investigation on Transmission Lines—VIII, W. W. Lewis, C. M. Foust. *Electrical Engineering* (*AIEE Transactions*), volume 64, March 1945, pages 107–15.

6. Symposium on Operation of the Boulder Dam Transmission Line—Insulation and Lightning Protection, Bradley Cozzens. *Electrical Engineering* (*AIEE Transactions*), volume 58, April 1939, pages 140–7.

7. Spectrum of Lightning, Philip Fox. *Astrophysical Journal* (Chicago, Ill.), volume 18, 1903, pages 294–5.

Discussion

L. P. Harrison (United States Weather Bureau, Washington, D. C.): Mr. Hagenguth is to be congratulated for his valuable paper summarizing results of tests made principally to determine damage to various aircraft components caused by simulated lightning strokes. These results have been most useful to the aviation industry. To a certain degree they have shown the extent of damage to be expected in cases of natural lightning strokes to aircraft, and they have indicated the nature and degree of protection necessary for aircraft to withstand the effects of such strokes without serious consequences. Conversely, by duplicating natural lightning damage to aircraft parts by employing artificial lightning strokes in the laboratory, the engineer has been enabled to calibrate the natural strokes. This has permitted him in some instances to determine approximately the peak current and the total charge in coulombs of the lightning. The author's findings permit us to give more credence than heretofore to the results of tests made with laboratory-produced lightning.

It is interesting to compare Mr. Hagenguth's data with those secured from the airlines and military services in regard to damage to aircraft caused by natural lightning. A report based on actual cases of lightning strokes to commercial and military aircraft has already been published for the Subcommittee on Lightning Hazards to Aircraft of the National Advisory Committee for Aeronautics.[1]

One of the theories which has been regarded as applying to a significant proportion of such cases is that discharges may be triggered by the intrusion of the aircraft into a region within or adjacent to certain cloud types. According to this theory, the field intensity of a region may be less than the critical breakdown potential gradient before the aircraft enters the region. However, after the aircraft makes its abrupt appearance under the given conditions of intense field, the phenomenon of electrical induction at the convex extremities (such as wing or propeller tips, antennas, and so forth) causes the local gradient to exceed the breakdown value, thus initiating a disruptive discharge.

Unpublished data in the writer's possession, together with those already published,[1] seem to indicate that triggering of disruptive discharges occurs most often when the aircraft is flying in a cloud with moderate or heavy snow or other frozen precipitation at a temperature near 32 degrees Fahrenheit, while presumably the cloud mass at lower levels consists largely of water droplets. These conditions are conducive to development of steep potential gradients.

Inasmuch as the theory of triggering is in harmony with known physical principles, and we have some pilots' reports giving factual information which apparently supports the theory, we can have a considerable degree of confidence in the reality of triggered strokes.

Now, one may wonder whether the characteristics of such strokes are invariably the same as those of natural cloud-to-cloud discharges, which Mr. Hagenguth states "probably are of the continuing-current type only." In a good proportion of the cases in which we believe triggering of a discharge by the aircraft was involved, pilots have reported the accompanying sound as "loud" (in one instance described as similar to a 155-millimeter howitzer going off in the cabin). Since loud thunder is a concomitant of lightning strokes characterized by a steep wave-front, we infer that at least some of the triggered discharges possess this characteristic, which apparently is not typical of natural cloud-to-cloud strokes. While it is possible that a triggered discharge may propagate to the ground and yield a steep-wave front return stroke, the high altitude and attendant meteorological conditions lead this reviewer to incline to the belief that development of such return strokes is not invariant in the cases considered.

In connection with the author's equations giving area of hole burned as a function of coulombs in arc and thickness of sheet, some reservations appear to be necessary to show their limitations. This is suggested because the equations are based on laboratory determinations and do not take into account heat losses under the conditions of low air density, low temperature, rapid relative air movement and often moisture or ice accretion prevalent in the case of an actual aircraft struck during flight through clouds or precipitation.

Mr. Hagenguth refers to the remarkable circumstance of a stroke terminal that remained fixed to one point on a plane even though the aircraft was traveling at the rate of 200 miles per hour. This is readily rationalized by regarding the airspace between the charge centers of the cloud as similar to that between the plates of a giant charged condenser. Under the conditions existing between extensive distributions of charges forming dissimilar charge centers, it is possible for the potential gradient to approach the critical breakdown value over a considerable horizontal extent. The intrusion of the metallic airplane effectively increases the potential gradient, causing the breakdown value to be exceeded progressively over a horizontal distance. Once a disruptive discharge occurs at the metallic surface of the airplane, the arc provides a copious supply of electrons and ions. The branching streamers that feed the main stroke channel in the cloud are distributed in space and vary their currents and paths to conform to the horizontal displacement of the channel as the aircraft moves through the field.

We consider especially noteworthy the author's finding of the marked effect of alloy-type on the relation of area of hole burned to charge in coulombs.

In regard to the tests on models of airplane fuel tanks, one may infer with confidence that double-walled tanks are in themselves much safer against explosive effects than tanks integral with the wing skin. Since the tanks can be partially empty and the temperature of their outer walls may lie in the range −20 to −40 degrees centigrade, following a dive, it is clear that integral tanks or single-walled, droppable, exposed tanks are not absolutely immune from such effects, as lightning occasionally does strike in relatively flat parts.[1]

The author states: "It is known that corona streamers play on the surface of the glass due to the falling of charged rain drops on the glass." We wish to point out that the drops need not be charged initially to produce corona, provided the intensity of the electric field is sufficiently great. Coronas are produced by fields that arise either from charges accumulated on the aircraft or from external distribution of charges, or both, as shown by Gunn, Hall, and Kinzer[2] and Edwards and Brock.[3]

The artificial lightning spectrum studied in the laboratory possessed certain prominent copper lines absent in the natural lightning spectrum, and lacked lines that may occur owing to passage of the discharge through a field containing numerous cloud or rain drops, which is often encountered by a pilot during flight. These differences between simulated and natural lightning spectra might be significant in exact work.

The author also states that the probability of a blinding effect owing to lightning is very small. Reports received from pilots indicate that temporary blinding may sometimes last about three minutes, and in two different reports pilots stated they were blinded for about eight minutes. From this it is evident that as a safety measure at least one of the crew in the cockpit should keep his eyes shielded during flights when lightning might strike.

With reference to the role of exhaust gases in the initiation or conduction of lightning strokes, it seems relevant to point out that the importance of this role is considerably lessened by the diffusion of these gases through the action of air currents induced by wings and propellers. These diffusive transports quickly tend to reduce the temperature and ionization of the gases, thus lessening the probability of breakdown or guidance of a discharge by the stream of ionized particles. However, the fact that the exhaust stack of aircraft is not ordinarily struck should not be used too strongly as an argument that the effect of the exhaust gases is not as predominant as the tests indicate (see paragraph F). It must be recalled that the direction of the potential gradient of the space surrounding the aircraft has a dominant influence on the direction taken by a disruptive discharge, and that the turbulent streak of exhaust gases may not be nearly parallel to the gradient, while other projecting members of the aircraft (such as the tail assembly) afford some protection to the exhaust stack.

With regard to tests on nonmetallic airplane parts, it would have been appropriate for the author to have referred to the more comprehensive investigations of this nature made at the High Voltage Laboratory of the National Bureau of Standards[4] under the general direction of Dr. F. B. Silsbee. One of the outstanding developments in these investigations was the demonstration of the value of protective coatings.

In closing, we may state that the author in his interesting paper has clearly demonstrated the protection against lightning provided by an all-metallic aircraft (as would be expected from the Faraday cage effect). His conclusions, however, do not appear to emphasize sufficiently the optical hazards to pilots and the frequent damage to radio equipment owing to lightning discharges.

References

1. Lightning Discharges to Aircraft and Associated Meteorological Conditions, L. P. Harrison. *Technical Note Number 1001*, National Advisory Committee for Aeronautics (Washington, D.C.) May 1946.

2. The Precipitation-Static Interference Problem and Methods for Its Investigation, Ross Gunn, Wayne C. Hall, Gilbert D. Kinzer. *Proceedings*, Institute of Radio Engineers (New York, N.Y.) volume 34, 1946, pages 156P-178P and 234-47.

3. Meteorological Aspects of Precipitation Static, R. C. Edwards, G. W. Brock. *Journal of Meteorology* (Lancaster, Pa.), volume 2, 1945, pages 205–213.

4. Protection of Nonmetallic Aircraft from Lightning, (four reports) High Voltage Laboratory, National Bureau of Standards (National Advisory Committee for Aeronautics) Washington, D.C., *AAR 3I10*, September 1943; *AAR 3J12*, October 1943; *AAR 4C20*, March 1944; *AAR 4I28*, March 1945.

A. E. Raymond (Douglas Aircraft Company, Inc., Santa Monica, Calif.): We enjoyed reading Mr. Hagenguth's excellent paper on the effects of lightning strike damage to aircraft. We agree that we do not know of any actual accidents or injuries to persons in aircraft due to the effects of lightning. However, the dangers from lightning strikes should not be minimized.

Furthermore, data from the Thunderstorm Project[1,2] Reports indicate that there were 21 lightning strikes in 1,363 thunderstorm missions which amounts to over 1.5 per cent. Another report from the Safety Bureau of the Civil Aeronautics Board[3] shows that there were over 1,800,000 scheduled airline departures in the United States in 1947. We have no way of knowing what percentage of these flights encounter thunderstorm activity enroute.

We were very interested to read the conclusions reached by Mr. Hagenguth concerning the use of ball-bearing bonding jumpers. His data and tests show that the bearing damage from lightning was negligible. Our own tests at the Douglas Aircraft Company have shown that even a properly installed jumper under the best conditions would carry no more than about one-third of the total current traversing the bearing. For both reasons we discontinued the use of jumpers several years ago.

We think there has been one notable omission from the paper with regard to lightning damage. This is the effect of lightning on radio and electronic equipments in aircraft. There have been many cases of lightning strikes interrupting the operation of such equipments due partly at least to blown tubes, fused contacts, burned out wires, and the like.

REFERENCES

1. Operation and Activity of the Thunderstorm Project, April 1–October 1, 1947. *Report Number 3, U. S. Weather Bureau* (Washington, D. C.), November 1947.

2. A Report on Thunderstorm Conditions Affecting Flight Operations. *Report Number 7, U. S. Weather Bureau* (Washington, D. C.), April 1948.

3. Frequency of Engine Failures in Scheduled Air Carrier Operation, James S. Rice. *Civil Aeronautics Board* (Washington, D. C.), June 3, 1948.

F. B. Silsbee (National Bureau of Standards, Washington, D. C.): This paper summarizes briefly the results of a very considerable effort given during the war to assist the work of the National Advisory Committee for Aeronautics Subcommittee on Lightning Hazards to Aircraft. It should be of great value both to the aeronautical industry which is directly concerned and which can make good use of the technical findings and to the traveling public which should feel much reassured by the results.

For a long time lightning has been used as a scapegoat to "explain" the cause of otherwise unexplainable accidents. The work of the National Advisory Committee for Aeronautics Subcommittee has gradually eliminated one possible explanation after another until very few remain. As stated in conclusion number 4 of the paper, other features of a thunderstorm such as violent gusts constitute much more serious hazards than does the lightning discharge itself.

Those interested in this field will find further information in the paper by L. P. Harrison[1] in which he summarizes observations on nearly 200 cases of lighting strokes to aircraft and discusses the meteorological conditions under which they occurred.

It is interesting to note the experimental data on the melting of holes in sheet material are so closely concordant with the results reported by P. L. Bellaschi[2] who performed similar tests on pointed conductors. The present results show that approximately 1.6 coulombs are required per cubic millimeter melted while Bellaschi's values ranged from 1.7 to 2.5. The slight difference doubtless results from the radically different shape of the specimens. Presumably the electrode drop is nearly independent of the current and is of the same magnitude in either case. The resultant heat developed is applied somewhat more effectively in the case of sheet material.

The fact that the results on ball bearings are best co-ordinated on the basis of the energy dissipated in a fixed resistance (that is, they are a function of $\int i^2dt$) is consistent with the results of tests made in the High Voltage Laboratory of the National Bureau of Standards on the ability of various thin foils and sprayed metal deposits to carry surge currents.[3]

In spite of statements to the contrary by other workers, the author's conclusion that it is inconceivable that the small currents produced by corona discharge alone could burn holes in the aluminum skin would seem to be well founded. However, such corona currents which develop when aircraft are flying near thunderstorms even though no lightning discharge occurs, do cause a serious interference with radio communication. This problem is one for which an entirely satisfactory solution has apparently not yet been developed. Still another hazard from corona discharge arises in the case of a glider towed with the usual nylon towline. Extrapolated estimates from laboratory experiments[4] indicate that such currents might well be sufficient to burn or melt the towline even at a distance of a kilometer or so from an active thunderhead.

The destruction shown in Figure 21 is a fine "horrible example" of what might be expected if an unprotected plywood aircraft, like the British Mosquito Bomber, were struck by lightning. Moreover, the control wires of such airplanes and gliders usually are so located that they would lead current directly to the pilot with fatal results. It appears that a moderate amount of conducting material, which would not be impossibly heavy, can be properly placed on such an aircraft to give reasonable protection.[5] However, even with such protection, and in the case of the usual all-metal airplane, there still remains one remote possibility of danger. If the bulk of the lightning current should be so concentrated as to flow in the metallic structure past one side only of a person his body might function as the secondary circuit of a transformer the primary of which is formed by the current path. If the current is changing at the frequently observed rate of 10^{10} amperes per second, the electromotive force induced in a loop the size of the human heart at a distance of 1 meter would be 80 volts! Of course the duration of such a surge is very short, the chance that it would occur during the sensitive phase of the heart cycle is probably not greater than 1 to 5 and the probability with an all-metal plane that the current path would be so closely concentrated is rather small. Experiments with animals at the National Bureau of Standards under conditions simulating this situation showed no fatal effects although very marked muscular reactions were produced in anaesthetized guinea pigs by such a transformer action.

REFERENCES

1. Lightning Discharge to Aircraft and Associated Meteorological Conditions, L. P. Harrison. *Technical Note Number 1001*, National Advisory Committee for Aeronautics (Washington, D.C.) May 1946.

2. Lightning Strokes in Field and Laboratory, III, P. L. Bellaschi. *AIEE Transactions*, volume 60, 1941, pages 1248–56.

3. Lightning Conductor Materials, *Wartime Report Number W-60*. National Advisory Committee for Aeronautics (Washington, D. C.).

4. Electrical Effects in Glider Towlines, *Wartime Report Number W-62*. National Advisory Committee for Aeronautics (Washington, D. C.).

5. Electrocution Hazards from Inductive Voltages, *Wartime Report Number W-85*. National Advisory Committee for Aeronautics (Washington, D.C.).

J. H. Hagenguth: The contributions of the discussors to the subject of the paper are gratefully acknowledged. The paper was strictly confined to a report on findings from tests made on various parts of the airplane

It was recognized that effects of corona discharges on radio reception, damage from strokes to the antenna and radio equipment, demagnetization of the magnetic compass, and other potential dangers exist. These effects were not introduced because they were not investigated. Omission did not imply that these factors are not of equal or greater importance than those covered in the paper. The bibliographies included in the discussions cover many of these factors and the investigations reported there are of considerable importance in the field of lightning hazards to airplanes.

The induced voltage effects on the hearts of pilots noted by Dr. Silsbee are an interesting speculation.

Mr. Harrison's comments on the trigger action of the plane probably are correct. It also is possible that charges on the plane may contribute to a steep-wave current; however, of relatively low magnitude. The stroke doing the damage shown in Figure 6 of the paper may have had one such component. It is doubtful that the noise produced would be very loud. I would suspect that loud noise results only when one or several stroke components reach ground. Discharges between clouds, generally speaking, would not be expected to produce return strokes based on experience at the Empire State Building in New York. When the leaders start at the building towards the cloud, no visible or measurable return stroke results.

This indicates that charges within the cloud are not sufficiently concentrated to produce return strokes.

Many factors may contribute to the hanging on of a stroke to a plane. The fact that the discharge does not move, of course, accounts for the large holes burned in the metal.

It was not intended to minimize the blinding effect. With a single pilot this might have disastrous consequences. In the case of air transport where two pilots are available, well acquainted with the possibilities of being blinded, a dangerous condition should not result.

Any conclusions drawn on current magnitude and charges in strokes to airplanes are necessarily approximate, if based on tests in the laboratory. The exact conditions existing at and near the plane are never known. However, such tests give an over-all story on the characteristics of lightning in the upper atmosphere which otherwise would be very difficult to obtain.

Mr. Raymond's statistical figures indicate a rather high incidence of lightning to aircraft. This should make it well worthwhile to educate flight personnel about the possible dangers involved. His remarks about badly damaged aircraft indicate that constant vigilance must be maintained.

Dielectric Strength of Oil with Variations of Pressure and Temperature

W. G. HOOVER
MEMBER AIEE

W. A. HIXSON
STUDENT MEMBER AIEE

THE DIELECTRIC properties of insulating oil under conditions other than those of atmospheric pressure and temperature are of interest in their effects on the performance of switchgear and other apparatus. The relatively small amount of information published on these effects prompted the study reported in this paper. The information to be presented is far from a complete coverage of the subject, but does result in some interesting conclusions and suggests the desirability of further research in this field.

Experimental Procedure and Equipment

The experimental procedure consisted principally of observing the breakdown strength of commercial quality insulating oil with variations of pressure up to 200 pounds per square inch gauge (above atmospheric) and down to −14.3 pounds per square inch gauge (below atmospheric). Samples of different quality, according to the standard 0.1-inch gap test, were tested with 60-cycle sine-wave voltages, long duration unidirectional impulse voltages (100 × 4,000-microsecond wave), and short duration unidirectional impulse voltages ($1^1/_2$ × 40-microsecond wave). Tests were made also on samples of partially degassed oil. Additional tests were carried out over the above pressure range with temperature variations of from 20 to 100 degrees centigrade.

The oil used for all reported tests was 100 per cent mineral oil, having a viscosity at 25 degrees centigrade of approximately 68 seconds, Saybolt Universal,[1] and a viscosity index of approximately 46.[2] The color of the oil in a small sample was just a shade off water-clear, and was estimated as having a color number of approximately two.[3]

All tests were made with breakdown taking place between 1-inch diameter brass spheres. The test tank was designed to permit an adjustment of the position of one of the spheres by a micrometer device operated through a control extending through the top of the tank. The micrometer setting could be observed through a window in the side of the tank. A small motor submerged in the oil in the tank drove a propeller to circulate the oil while the tests were being carried on. A vitreous enamelled resistor in the tank was used for heating the oil if desired. The power dissipated in the resistor was controlled by supply-voltage variation. The volume of oil contained in the test tank was approximately eight gallons. It was felt that with this relatively large volume and with the forced circulation of the oil, a given breakdown did not appreciably alter conditions under which a succeeding breakdown took place, due either to the presence of a small amount of decomposition products or to any possible clean-up action on impurities. This assumption seemed to be confirmed by the absence of any definite "hysteresis" effect upon reducing the pressure after a test run with increasing pressure.

Pressures above atmospheric were obtained by coupling the test tank through a short length of pipe to an Ashcroft gauge tester. The pressure could be raised by forcing oil into the tank by the piston on the gauge tester. The tank and tester were completely filled with oil, so that a small volume of oil displaced by the piston could vary the pressure over the entire range. The pressure could be adjusted accurately in steps of 5 pounds up to 200 pounds per square inch.

Pressures below atmospheric were obtained with a rotary vacuum pump. By means of a system of valves, the reduced pressure could be placed on the tank without changing any connections. An arrangement of bleeder valves was used to maintain the vacuum at the desired level. This condition was observed by a gauge placed in the line leading from the vacuum pump to the tank. The minimum possible pressure obtainable at the test gap was approximately 0.4 pound per square inch due to the head of oil above the gap.

For positive pressure, the results were influenced by the fact that the amount of air in the oil was limited to the amount contained in the oil when the tank was filled at atmospheric pressure. For negative pressures, air given up by the oil as the pressure was reduced could be drawn off by the vacuum pump until equilibrium was reached for the particular pressure involved.

The partially degassed oil was oil from the same source as tested in normal condition, but sprayed into a small storage tank from which the air was previously removed by a rotary vacuum pump. The process then was effectively repeated by allowing this oil to spray into the test tank under vacuum. However, the removal of all the air from the oil is a difficult process, and even with the above method a small amount of air remained in the oil.

The 60-cycle high voltage was obtained from a 110/65,000-volt test transformer. The primary voltage was controlled by a variable transformer ("Variac"). The Variac control shaft could be driven through a gear reduction train by a small motor to turn the shaft at a con-

---

Paper 49-216, recommended by the AIEE Basic Sciences, and Instruments and Measurements Committees and approved by the AIEE Technical Program Committee for presentation at the AIEE Pacific General Meeting, San Francisco, Calif., August 23–26, 1949. Manuscript submitted May 27, 1949; made available for printing July 18, 1949.

At the time this paper was written, W. G. Hoover and W. A. Hixson were both with Stanford University, Stanford, Calif. Since that time W. A. Hixson has joined the General Electric Company, Pittsfield, Mass.

stant speed, resulting in a uniform rate of voltage application. The rate of voltage rise was set at three kv per second. A 600,000-ohm resistor was placed in the high-voltage lead to limit the current flow at breakdown and to reduce pitting of the surfaces of the spheres.

The impulse voltages were obtained using two 100-kv 0.25-microfarad capacitors in a simple Marx circuit impulse generator. Both of the impulse wave forms previously mentioned were obtained by controlling the circuit resistances and load capacitances of the impulse generator. Wave forms of applied impulse voltages were observed with a cathode-ray oscillograph. Breakdown was noted either by change of wave shape of impulse voltage or by the sound produced by the breakdown in the tank.

Results of Tests

Experimental results are summarized in Figures 1 to 5. The curves were drawn through the averages of a number of breakdown values for each condition tested. Since it was found that some time was required for stabilization of breakdown strength after a change of pressure was made, values of breakdown voltage for the curves plotted as functions of pressure are values obtained after several minutes had been allowed for stabilization.

As is characteristic of breakdown tests of insulating oil, there was a considerable range of variation in individual breakdown voltages for a given test condition. Curves were drawn through what seemed to be the most probable value in each case. Individual points tended to be clustered with greater density near the curves as drawn, with points having larger deviation occurring less frequently. The spread of values was greatest in the 60-cycle tests, with most values lying within the range of ±25 per cent. For the impulse tests the range was more nearly ±15 per cent.

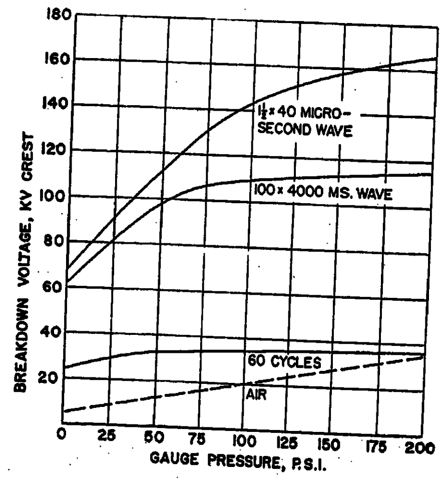


Figure 1. Breakdown of sample of good oil at positive gauge pressures, 0.04-inch gap between 1-inch diameter spheres

Breakdown of air at 60 cycles shown for comparison

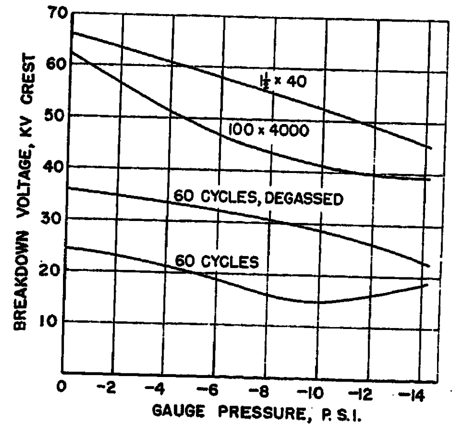


Figure 2. Breakdown of sample of good oil at negative gauge pressures, 0.04-inch gap

It will be observed that in all cases where the pressure was above atmospheric pressure, an increase in pressure resulted in an increase in dielectric strength. The beneficial effects of pressure appear to be greatest in the pressure regions just above atmospheric, with a tendency toward leveling off of the curves at the higher pressures. In all cases, the rate of increase of dielectric strength was considerably less than directly proportional to the total pressure, as is more nearly true for air (Figure 1).[4] The curves for pressures below atmospheric, Figure 2, show in general a continuation of the tendencies exhibited at higher pressures. The distinct minimum shown at less than minimum pressure for curve *A* of Figure 2 is characteristic of results obtained at 60 cycles with oil having the normal amount of air at atmospheric pressure.

The time requirement for stabilization of breakdown values after pressure changes from atmospheric to the limits available in these tests is illustrated in Figure 4. For this test the oil first was allowed to stand at atmospheric pressure to acquire its normal amount of dissolved air. Then the pressure was quickly increased to 200 pounds per square inch and readings taken of breakdown strength as time elapsed. A similar process was followed in obtaining the values corresponding to a rapid decrease of pressure. In this latter case the breakdown was at first reduced to nearly half its final value.

Effects of the duration of applied voltage are shown by the marked differences between curves in Figure 1. At 200 pounds per square inch, the 0.04-inch gap required an average breakdown gradient of about 4,000 volts per mil with the 1½ × 40-microsecond wave. This was more than four times the value required at 60 cycles.

In Figure 3 a comparison of results of tests at 60 cycles is shown. Curve *A* is the same as the curve for 60 cycles in Figure 1 and applies to a sample of good oil, which tested at about 30-kv rms in a standard test cup. Curves *B* and *C* are for another sample of oil, which tested at about 22 kv. Curve *C* is for a gap of 0.15 inch, nearly four times the spacing of 0.04 inch for the other curves. It shows, however, the same characteristic shape. Comparative tests of different samples of oil showed much smaller differences with impulse voltages than at 60 cycles.

Definite improvement in the performance of oil may be effected by degassing, as shown by curves in Figures 2 and 3. Advantage may be taken of this improvement where the oil is in sealed containers, such as in certain types of capacitors, transformers, and so forth.

Theoretical Considerations

The experimental results can be explained to a large degree by considering the apparent influences of gases and impurities in the oil. Oil as ordinarily used contains dissolved gas, generally air, and in addition may have gas in the form of a suspension or emulsion. A certain con-

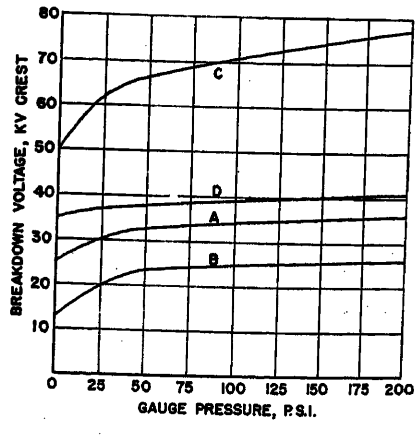


Figure 3. Breakdown for positive gauge pressures at 60 cycles

- A. Breakdown of good oil, 0.04-inch gap
- B. Breakdown of poor quality oil, 0.04-inch gap
- C. Breakdown of same oil as for A, 0.15-inch gap
- D. Breakdown of good partially degassed oil, 0.04-inch gap

centration of impurities, such as water and other foreign matter, is usually present.

The effect of gas in liquid dielectrics is known if the gas is present in suspension. Gas in this form makes up part of the volume of the insulating medium and lowers the dielectric strength. Most modern theories concerning the electrical breakdown of liquid dielectrics,[5,6,7] agree in stating that the ionization of gases in suspended form, where such gases can exist, is responsible for the initiation of the breakdown process. Stable gas suspensions do not exist in oil, but it has been shown that such can exist at a liquid–solid interface.[7] Furthermore, the application of an electric field may result in the liberation of gas from the dissolved state into the suspended form.[6,8] The breakdown gradient under these conditions should be a function of the electrical properties of the released gas.

In pure oil, this action of gas evolution no longer holds, and the process of breakdown is changed. All insulating materials have some conductivity[9] and therefore contain charged particles. It has been postulated[6] that the movements of such charged particles under the influence of the applied field results in a space charge which moves toward the electrode of opposite sign. As a portion of this space charge discharges itself, a film of neutral molecules is established, across which a difference of potential exists due to other portions of the space charge and to the applied field. The rupture of this film results in the profuse ionization that causes breakdown of the pure oil. In this case, however, the breakdown voltage is higher than that required if gas is present.

In oil containing gas in suspension, ionization of the gas is usually the dominant factor. It is to be expected that if a change of external conditions, such as pressure or temperature, should effect the availability of gas in the suspended form, the breakdown gradient would be altered. The physical nature of the suspended gas, such as bubble size, number of bubbles per unit volume of oil, density of gas within the bubbles, and so forth, should influence the contribution of the gas to breakdown. Time is required to produce a change in the ratio of dissolved to suspended gas. The breakdown gradient thus should be affected by the duration of the applied voltage and by the amount of time elapsed since a change of external conditions.

It is well known that the presence of water is detrimental to the electric strength of oil. As with gas, water may exist in either the molecular (dissolved) or emulsion phases. Emulsified water is apparently much more effective in reducing the electric strength than is dissolved water.[5,10] In the process of aging, emulsified water becomes more and more concentrated and may act to reduce greatly the breakdown value. The presence of sufficient water or other impurities may mask the effects of gas in the oil.

The solubility of air in oil is nearly directly proportional to pressure over a wide range. As a function of temperature the solubility at atmospheric pressure exhibits an upward trend through zero degrees centigrade to a maximum at about 70 degrees centigrade, beyond which the solubility falls off.

The experimental results show tendencies in agreement with the above theories. For positive pressures, the 60-cycle breakdown curves exhibit a definite rise at first, followed by a nearly horizontal section. The tested oil samples first were allowed to stand at atmospheric pressure until the oil was saturated with dissolved air. As pressure was applied in the sealed tank, the percentage of saturation decreased. Apparently during the rising part of the curve the electric field in the gap was able to extract gas from the dissolved state. The difficulty of liberation of gas and the density of the liberated gas increased with the pressure, causing the upward slope of the curves. Beyond 50 pounds per square inch pressure, the electric field necessary for extracting the gas apparently exceeded the breakdown value for the oil itself; little further increase with pressure occurred. This idea is further confirmed by the tests of partially degassed oil, in which the slope on the initial part of the curve is much less.

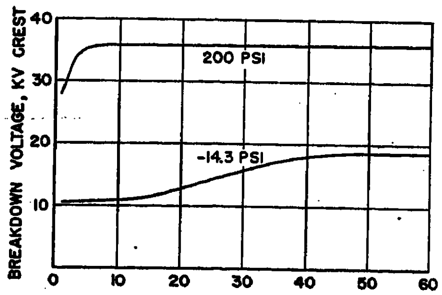


Figure 4. Breakdown voltages at 60 cycles following sudden changes from atmospheric to gauge pressures shown on curves

Initial breakdown at atmospheric pressure, 26 kv

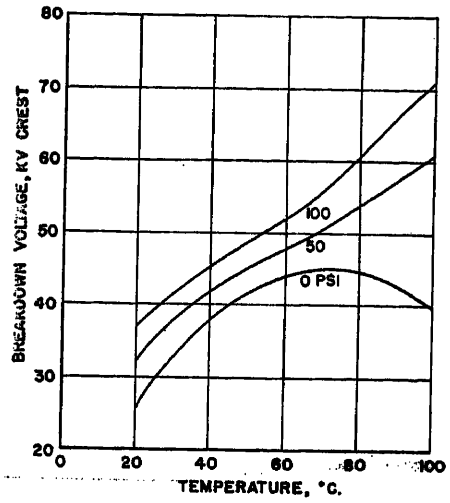


Figure 5. Breakdown of good oil at 60 cycles as functions of temperature

Numbers on curves show gauge pressure

Other contaminants in the oil, such as water, seem to affect the heights of the curves without significantly modifying the shape. This suggests that for the lower pressures, where gas is released, the space in the gap is in the nature of gas bubbles and oil layers in series, with the breakdown voltage for the combination a function of both air and oil.

For impulse voltages, the presence of free gas in the oil apparently plays an important part for the lower pressures. The breakdown values are much higher than for 60 cycles, due perhaps to the smaller sizes of bubbles produced, or to the possible time limitation, especially for the $1^1/_2 \times 40$-microsecond wave, of gas being available only at the electrode-liquid interfaces.[7] Less time is also available for the lining up of impurities in the electric field, so that these should play a relatively unimportant part. The extremely high breakdown gradients at the higher pressures indicate that field emission at the electrodes is probably an important factor in the initiation of breakdown at these pressures.

For negative gauge pressures the excess of air was drawn off by the vacuum pump. The further release of air with the application of voltage to the gap produced bubbles of lower density and consequent easier ionization, resulting in a decrease of breakdown voltage with decrease of pressure. For the curve for 60 cycles, Figure 2, the minimum could be explained as a point beyond which the total amount of air extractable from the oil was insufficient, in spite of its lower density, to provide as adequate a source of ionization as before. In this respect the picture is somewhat like the upward rise of the breakdown voltage for air as the pressure is reduced below the point for minimum breakdown at low pressure. For partially degassed oil and for impulse voltages, the above minimum did not occur within the pressure range covered probably due to either the insufficient amount of air dissolved or the time available for its extraction.

The effects of temperature observed correlate well with the solubility of air as functions of temperature and pressure. The improvement noted at higher temperature is opposite to the effects usually observed for solid insulations.

References

1. Method of Test for Viscosity by Means of the Saybolt Viscometer. *Standard D88-44*, American Society for Testing Materials (Philadelphia, Pa.), 1944.

2. Standard Method for Calculating Viscosity Index. *Standard D567-41*, American Society for Testing Materials (Philadelphia, Pa.), 1941.

3. Method of Test for Color of Lubricating Oil and Petrolatum by Means of A. S. T. M. Union Colorimeter. *Standard D155-45T*, American Society for Testing Materials (Philadelphia, Pa.), 1945.

4. The Electric Strength of Air at High Pressures—II, H. H. Skilling, W. C. Brenner. *Electrical Engineering (AIEE Transactions)*, volume 60, March 1941, pages 112–5.

5. Role of Dissolved Gases in Determining the Behavior of Mineral Insulating Oils, F. M. Clark. *Journal*, Franklin Institute (Philadelphia, Pa.), volume 215, 1933, pages 39–67.

6. Electrical Breakdown of Liquid Dielectrics, F. M. Clark. *Journal*, Franklin Institute (Philadelphia, Pa.), volume 216, 1933, pages 429–58.

7. Role of Gases in Breakdown of Liquid Dielectrics, A. Walther, O. Tscheljustkina. *Technical Physics in the U. S. S.R.*, (Leningrad, Russia) volume 3, 1936, pages 940–6.

8. Influence of Absorbed Air on Dielectric Strength of Liquids, F. Koppelmann. *Electrotechnische Zeitschrift* (Berlin, Germany), volume 52, 1931, pages 1413–6.

9. Space Charge in Liquid Dielectrics, J. B. Whitehead, E. E. Minor. *Physics*, (New York, N. Y.) volume 6, 1935, pages 380–8.

10. Hygroscopicity in Liquid Dielectrics, A. Lazereu. *Technical Physics of the U. S. S. R.*, (Leningrad, Russia) volume 1, 1934, pages 169–86.

No Discussion

An Electric Deicing System for Aircraft

E. W. GILOY
ASSOCIATE AIEE

Synopsis: This paper covers an electric ice removal system as applied to a recent experimental airplane. The system is described in detail from the power generators to the ice removal blankets and includes such items as extent of ice protection coverage, construction and installation of ice removal blankets, system electric power requirements, and system control. The paper does not treat the subject of ice protection in any of its theoretical aspects. The complete ice protection system installation is believed to be the first of its kind in this country.

THE FORMATION of ice on the flight surfaces of airplanes has been one of the major operational limitations of aircraft since World War I. Early research on the problem attempted a solution by the use of compounds such as soap or glycerin applied to an airfoil section. A successful practical solution by such means was thwarted by the large quantities of fluid and the need for its continual application. Later research along other lines led to the successful removal of ice by means of a rubber boot applied to the leading edge of the wing or airfoil. The rubber boot was periodically inflated by air, thereby cracking and breaking up any ice that was formed and allowing it to be blown away by the air stream. The present ice removal systems[1] used most successfully on today's modern airplanes, both commercial and military, apply heat (hot air) to the inner leading-edge surface of the metal wing skin. The large amounts of hot air required for such thermal ice removal systems usually are obtained from either engine exhaust-gas heat exchangers, combustion heaters, or air bled from the compressor on jet engines. Presently under investigation for flight surface protection is an electrical form of ice removal that is similar to one used for aircraft-propeller ice protection. Electric energy is converted to heat energy for ice removal by means of electrical resistance blankets applied to the inside or outside leading-edge skin of the wing, nacelle, and tail surfaces. It is this electrical method of ice removal that was applied to a recent experimental airplane.

Why Electrical Method

The wing construction of the experimental airplane is characterized by corrugations running spanwise and placed between an inner and outer leading edge skin. This type of wing construction, along with the limitation of a maximum allowable structure temperature of 200 degrees Fahrenheit, complicated the design of a hot-air ice removal system that would effect a heat transfer exceeding 1,500 Btu per hour per square foot through the inner wing skin and corrugation to the outer skin. The electrical method of ice removal offered a solution in that heat could be applied directly to the outer skin by the use of rubber blankets containing embedded resistance elements. The problem of providing sufficient electric power for the resistance blankets presented no undue difficulty since sufficient engine generator drives beyond the requirements of the airplane's 28-volt d-c primary-power system were available. Further, the airplane being an experimental model offered an opportunity to aid and to advance the art of ice removal by electrical methods.

Deicing Versus Anti-Icing

In designing for the removal of ice by electrical means it was felt, based on experience with propeller ice removal and National Advisory Committee for Aeronautics research, that a successful system could be provided by the deicing method in which heat is applied periodically in a repeating cycle to the various flight surfaces as necessary. Such a method materially reduces the continuous electric power requirements as compared to an anti-icing system in which sufficient heat must be applied continuously to prevent the formation of ice. In deicing, ice is allowed to form to a small extent and then is removed by the application of heat.

The ice that has formed acts as an insulator over the heated surface, with the result that only the bond between the airplane and the ice is broken upon application of heat, allowing the bulk of the ice to be removed by the airstream.

Extent of Deicing

The heating blankets are applied to approximately the forward ten per cent of chord, in line with the theory that only the forward portion of the chord of a wing need be heated to provide ice protection for the entire wing. The horizontal stabilizer and vertical fin of the airplane are protected in a similar manner. The blanket area for each surface is forward of the front spar and entirely within each removable leading edge. In the nacelle regions, protection is provided for the leading edge lip of jet-engine diffusers, jet-engine air intakes, and the by-pass tunnels located between engine nacelles. Deicing protection for the jet-engine diffusers covers an area one inch aft of the leading edge on the inner surface to seven inches aft on the outer surface. For the jet-engine air intakes and the by-pass tunnels, the coverage is one inch on the inner surface to approximately 16 inches on the outer surface. Figure 1 provides an overall view of the airplane's heated areas.

Paper 49-218, recommended by the AIEE Air Transportation Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Pacific General Meeting, San Francisco, Calif., August 23–26, 1949. Manuscript submitted July 30, 1948; made available for printing June 30, 1949.

E. W. GILOY is in the Electrical Design Engineering Section, The Glenn L. Martin Company, Baltimore, Md.

The author takes this opportunity to thank E. W. Colehower and other individuals of The Glenn L. Martin Company for their assistance in the preparation of this paper.

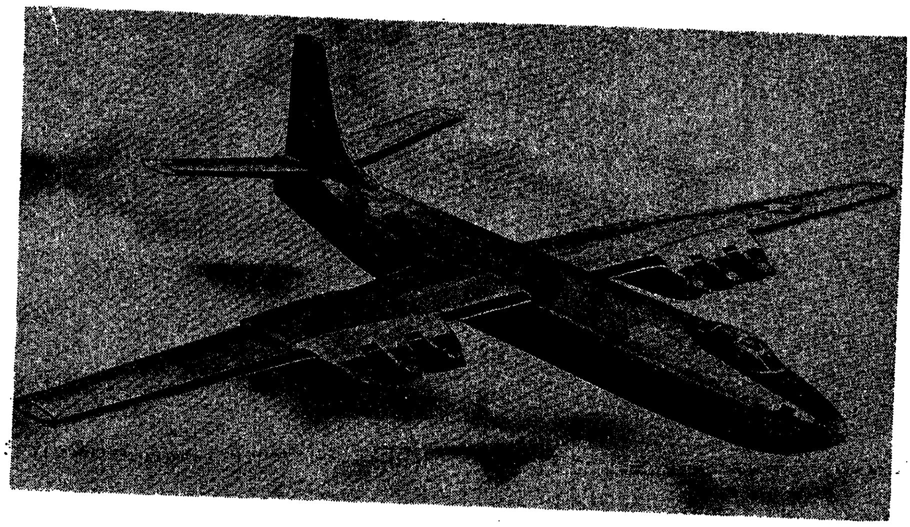


Figure 1. Artist's conception of airplane with deicing blankets

Means of Deicing

The heat necessary to effect ice removal for the areas outlined is obtained by means of the aforementioned electrical resistance blankets applied to the outside of the airplane skin. The blankets are of a laminated-vulcanized construction with electrical resistance wiring providing the heating element sandwiched between two thicknesses of black neoprene rubber. The finished product is 0.060 -inch thick with length and width as necessary. The wire elements of a blanket are spaced approximately 1/8 inch apart and run between braided internal bus bars, located at opposite edges of the blanket. For each blanket, heat is distributed evenly over its electrical area by the numerous parallel heating circuits contained therein. The electric connections to a blanket are made at each bus bar by an insulated conductor pigtail which is molded into the blanket normal to its surface. The directly heated or electric section is less than the total area of a blanket by the area occupied by the imbedded bus bars and a dead rubber area approximately 3/8-inch wide surrounding the electric section. This unheated area of each blanket varies the deicing protection slightly from the theoretical ten per cent of chord coverage for wing and tail surfaces.

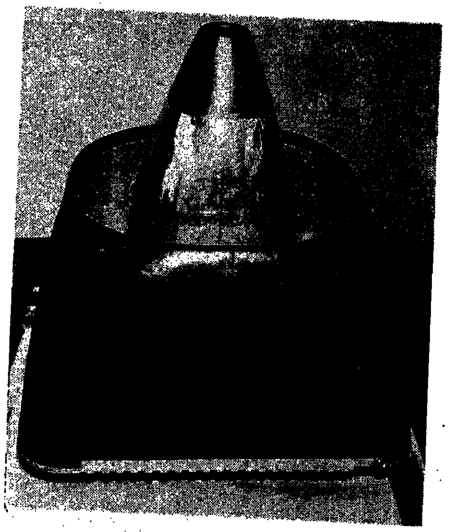


Figure 2. Deicing blankets installed on engine air intake and engine diffuser (center)

Installation of Blankets

The leading edge of wing, tail, and nacelle areas to receive icing protection are recessed 0.060 inch, the thickness of the deicing rubber blanket, from the desired final surface contour to provide a smooth contoured installation. For this particular installation the heating blankets are installed on the flight surfaces with the resistance wires contained therein running chordwise and the bus bars falling spanwise at the two aft edges of the blanket. The blankets as manufactured provide extra dead rubber in excess of the aforementioned 3/8 inch around the electrical section and are trimmed as necessary at installation to provide a butt joint between adjacent blankets and the airplane skin edge at the chordwise extremities of the recessed area. The installation is similar to that of fitting and laying inlaid linoleum to a table or floor provided with an edging strip. The rubber blankets are bonded to the metal surfaces of the airplane with a synthetic resin neoprene cement. Air that is advertently trapped under a blanket at various locations during installation is removed by piercing the rubber blanket with a hypodermic needle at each of the air pockets or blisters. Holes for the electric conductor pigtails are provided in the airplane skin at blanket locations and dimensionally match the conductor pigtails on each blanket. The molded-in conductor pigtails of the blankets pass through these holes and are connected to the airplane's wiring as the various circuits dictate. A terminal strip disconnect is provided in the circuit of each blanket and is located between each blanket and the airplane's wiring to facilitate the removal of a particular leading edge surface. Sufficient slack is provided in the wiring such that a leading edge section of wing, tail, and so forth, can be removed sufficiently to provide access to the terminal disconnects. For general ease of handling and installation, any individual deicing blanket is limited to a maximum length of about six feet, although it may contain more than one electric section as necessary to bring about a more satisfactory load division between blanket power sources.

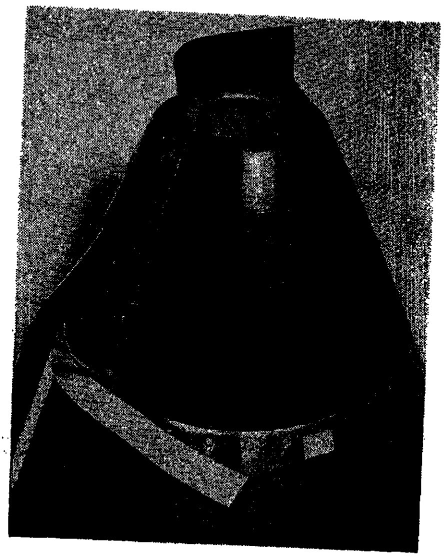


Figure 3. Partial installation of engine diffuser blankets

Note electric connection to blanket and indication of blanket heating wires

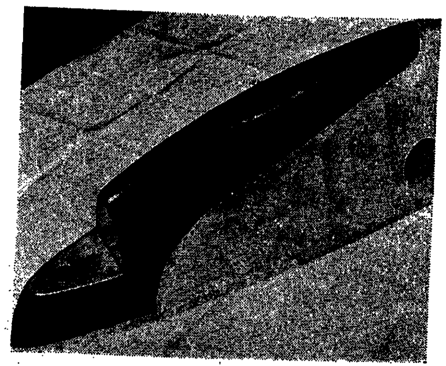


Figure 4. Installation of deicing blanket on airplane wing tip

Note cutout in blanket for wing tip light

It can be pointed out that with blankets installed side by side on a surface, there is a dead or unheated strip of rubber approximately 3/8 inch times two or 3/4-inch wide between the electric sections of adjacent blankets on which ice may tend to form and remain, but it is anticipated that sufficient heat will flow from the directly heated area of each blanket to this so-called dead area and effect a removal of ice under all but the more severe icing conditions. Figures 2, 3, and 4 show the installation of several of the deicing blankets.

Power Requirements

The rate of heat generation required by the deicing blankets for ice removal amounts to nearly 4,000 Btu per hour per square foot or an electric power consumption of eight watts per square inch. The total area covered by deicing blankets on the airplane amounts to approximately 400 square feet and is composed of 81 individual blankets with a total of 105 individual electric sections. The total connected deicing load of these blankets amounts to 452 kw with the average maximum, continuous load from the standpoint of generator capacity (due to deicing cycling, which will be covered later) amounting to 113 kw. In addition to this deicing load, variable-frequency a-c power is required for radar equipment, amounting to a maximum continuous demand of 7.8 kw plus *NESA** windshield anti-icing requirements as high as 2.5 kw.[2] An a-c system was elected to power the deicing loads based on the light weight per kilowatt and the individual capacities of recent a-c generators as well as the a-c needs of the radar and *NESA* loads. The deicing and *NESA* loads normally are supplied power by three engine-driven 208-volt 3-phase alternators with a normal rating each of 40 kva at 0.75 power factor. A fourth machine is provided as a deicing spare in event of an alternator failure and is normally used only as a primary power source for the radar load. These four alternators do not operate in parallel but are connected individually to their respective loads. Manual switching is provided to connect the fourth alternator to any one of the other three alternator deicing loads as may be necessary in event of failure of one of the three normally used machines. The load per alternator normally averages around 40 kw, as itemized in Table I.

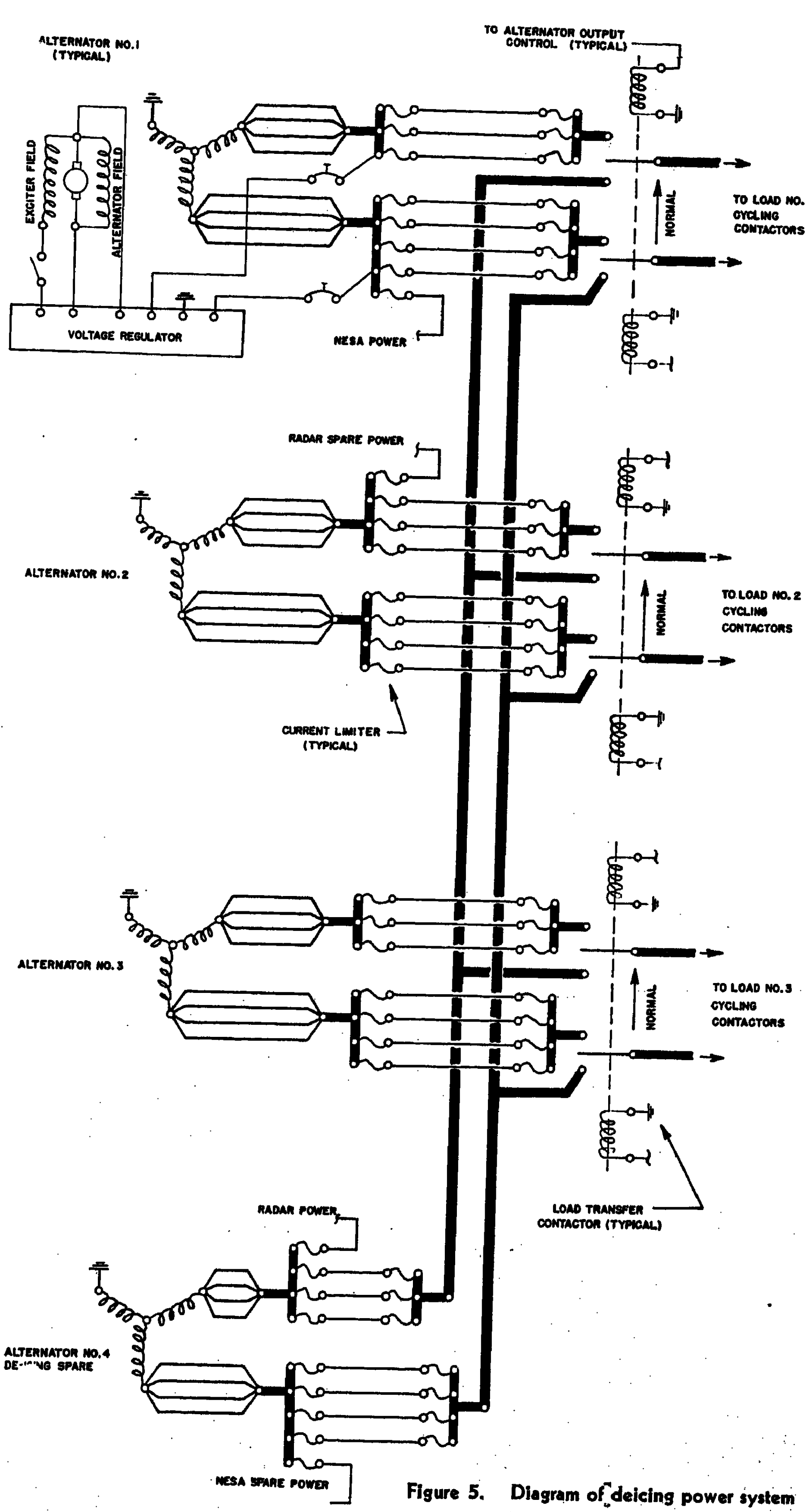

ALTERNATOR NO.1 (TYPICAL)
TO ALTERNATOR OUTPUT CONTROL (TYPICAL)
EXCITER FIELD
ALTERNATOR FIELD
VOLTAGE REGULATOR
NESA POWER
NORMAL
TO LOAD NO.1 CYCLING CONTACTORS
RADAR SPARE POWER
ALTERNATOR NO.2
NORMAL
TO LOAD NO.2 CYCLING CONTACTORS
CURRENT LIMITER (TYPICAL)
ALTERNATOR NO.3
NORMAL
TO LOAD NO.3 CYCLING CONTACTORS
LOAD TRANSFER CONTACTOR (TYPICAL)
RADAR POWER
ALTERNATOR NO.4 DE-ICING SPARE
NESA SPARE POWER

Figure 5. Diagram of deicing power system

Alternator Operation

The four alternators[3] used here for deicing power are a type developed for the Army Air Force and are normally intended for use as primary power sources for 120/208-volt 3-phase 400-cycle a-c systems. The alternators are Y-connected 8-pole machines, and in a normal paralleled a-c system are operated with grounded neutrals, thereby providing a line-to-line voltage of 208 volts and a line-to-ground voltage of 120 volts. However, for this deicing application the

---

* *NESA* denotes a conducting coating applied to windshields for electrical anti-icing.

Table I. Alternator Loading in Watts

	Load Group Maximum Total	Load Group Minimum Total	Load Group Average
Alternator number 1			
Phase *A*. Deicing	10,380	9,460	10,070
Phase *B*. Deicing	12,110	12,120	11,770
NESA	2,500	2,500	2,500
Phase *C*. Deicing	15,230	14,560	15,040
Total deicing load	37,720	36,140	36,880
Total load including *NESA*	40,200	38,640	39,380
Alternator number 2			
Phase *A*. Deicing	10,320	8,880	9,500
Radar *SP**	7,800	7,800	7,800
Phase *B*. Deicing	14,316	14,000	14,010
Phase *C*. Deicing	13,730	14,316	14,175
Total deicing load	38,366	37,196	37,685
Total load including radar	46,166	44,996	45,485
Alternator number 3			
Phase *A*. Deicing	10,160	10,520	9,850
Phase *B*. Deicing	14,170	14,100	14,365
Phase *C*. Deicing	14,500	13,390	14,190
Total load	38,830	38,010	38,405
Alternator number 4 (Deicing Spare)			
Phase *A*. Radar	7,800	7,800	7,800
Phase *B*. *NESA* *SP**	2,500	2,500	2,500
Plus deicing load from any other alternator as may be necessary			

* *SP* indicates spare or alternate power source for load shown.

alternators, as mentioned previously, do not operate in parallel. Nonparallel operation is feasible and warranted in that the deicing loads do not impose on the alternator power peaks or power surge demands that would make a paralleled system desirable, but instead present a steady and practically continuous load to the alternator. Further, nonparallel operation provides a lighter and simpler system by relieving the requirements for constant-speed operation of the alternators and the need for synchronizing equipment to parallel the machines.

The alternators also are operated with a grounded phase lead instead of a grounded neutral. This provides a 2-wire 3-phase 208-volt a-c system that is suitable for this special deicing application in which all the deicing blankets operate at 208 volts. This grounded phase lead operation of the alternators offers the following advantages as compared to grounded neutral operation: reduced wire weight (two wires versus three), and reduced quantity and thereby weight of circuit protective devices in the ratio of two to three. In operation, the alternators do not operate at their synchronous speed of 6,000 rpm, but rather at jet-engine speeds since they are directly driven by these engines through an extended shaft at the nose, without the bulk and weight of constant-speed drives.[4] The operating speed of the alternators in flight is between 7,700 and 6,700 rpm with a corresponding frequency variation of from 514 to 447 cycles. This frequency variation is tolerated since the loads, being primarily resistive, are not frequency sensitive. Figure 5 offers a simplified diagram of the deicing power system.

In addition to overspeed operation of the alternators, the installation provides blast cooling equivalent to ten inches of water pressure differential between air inlet and air outlet, whereas the normal pressure requirements are six inches of water. These factors, along with alternator operation at nearly unity power factor, result in a potential alternator output in excess of the normal rating of 40 kva at 0.75 power factor. In fact, recent tests of the machines under simulated operating conditions as will exist on the airplane, indicate the alternators are capable of delivering upwards of 52 kw at unity power factor continuously. Figure 6 shows a view of the deicing alternator.

Voltage Regulation

Alternator field excitation is provided by a small d-c generator that is integral with each alternator. The output of this d-c generator or exciter is directly connected to the alternator field. Voltage regulation of each alternator is provided by a conventional aircraft carbon-pile regulator controlling the d-c exciter. The solenoid-actuated stack of the regulator is in the shunt field circuit of the exciter and is controlled by the rectified 3-phase alternator output voltages. The regulator is capable of holding the average of the 3-phase alternator voltages to within ±2 per cent of the adjusted value. The point of voltage regulation for the deicing system is in the nacelle regions, approximately four feet from each alternator. The regulators are set to a nominal voltage of 208 volts and are installed in the flight area of the airplane, accessible for servicing in flight as may be necessary.

Cycling of Load

The electric load as represented by the deicing blankets is divided into three nearly equal divisions, with each division supplied power by one of the aforementioned 3-phase variable-frequency alternators. Each alternator's load is again divided into four basic 3-phase load groups, each group being composed of single-phase deicing blankets connected in delta. These load groups are supplied with electric power successively in a cyclic fashion, such that any one deicing blanket may receive power for a maximum of 1/4 of the total time in which the deicing system is in operation. Since the cycling time required for effective deicing is somewhat uncertain at the present stage of the art, a variable heating cycle control is provided. The "on" time for each blanket is continuously variable from a period of 26 seconds to 160 seconds, with the "off" time normally being three times the selected "on" time. Control also is provided for an increased "off" time greater than that mentioned. This additional "off" time, when used, is continuously variable from 40 to 240 seconds. For example, if the heating cycle control is set for 30 seconds and the additional time "off" control is set for 60 seconds, each alternator will supply power to its four basic load groups, one at a time, in succession for 30 seconds each and then will "rest" for 60 seconds before starting the cycle over again. If no additional time "off" is selected, the alternator "rest" period is eliminated, and the alternators will continuously cycle their four load groups one at a time in succession every 30 seconds. A diagram of a

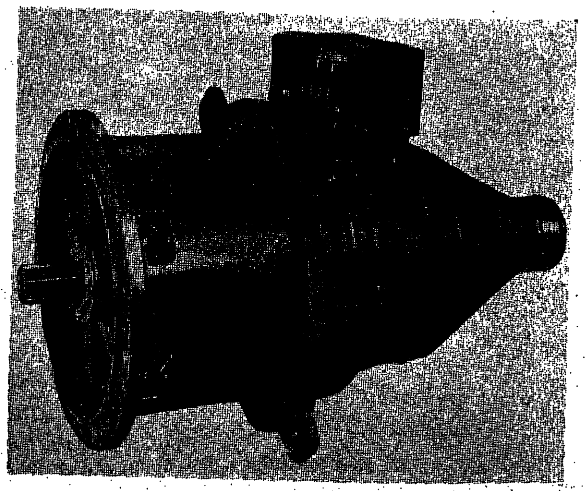


Figure 6. Deicing alternator. Alternator envelope is approximately 19 by 12 inches

typical deicing load on one alternator is provided in Figure 7.

Grouping of Blankets

In subdividing the deicing blankets into electric load units and connecting these units to the various alternators, care has been exercised to insure that corresponding blanket areas which are symmetrical about the center line of the airplane (namely areas on the wing and horizontal stabilizer) are connected in parallel so as to derive power through a common circuit protector from the same phase of a particular alternator. This arrangement insures that the ice which forms on the airplane between heating periods will be deposited in a symmetrical manner with the least disturbance to the airplane's balance and aerodynamic characteristics. Thus, under normal operation in icing conditions, ice will be forming on 3/4 of the ice-protected areas in a symmetrical pattern while ice removal is being effected on the remaining 1/4 of the area. Further, unforeseen power failures on various loads, as well as open-circuit protectors resulting from faulted wiring or blankets, still will provide a nearly balanced airplane as regards ice load. The subdivision of blankets applied to the nacelles is based on each deicing alternator being assigned the task of protecting its own particular jet-engine air intake system. The remaining jet-engine nacelle areas are divided among the three deicing alternators. See Figure 8.

System Control

Primary control of the deicing system is provided at the pilot's station by means of a single deicing control switch. The system may be turned off or on by this switch, provided the auxiliary controls at the copilot's station have been preset properly. The auxiliary controls of the system provided at the copilot's station are composed of alternator operation control, alternator output control, and blanket heating-cycle control. Alternator operation control is provided by alternator exciter-field switches. The exciter-field switches are normally in the "on" position, thereby providing alternator field excitation and alternator operation at regulated voltage. With the alternators in operation, deicing power is available to the load transfer contactors.[5] Control of the load transfer contactors is provided by three alternator output control rotary switches. Each main division of the deicing load is associated with one of these control switches and may be applied to either its normal alternator or the spare machine. Normally the main divisions of load 1, 2, and 3 will be connected to alternators 1, 2, and 3, respectively. The circuits of the control for the load transfer contactors are such that one and only one of the three main divisions of load can be electrically transferred to the spare alternator regardless of accidental mispositioning of the alternator, output control switches. With the output control switches correctly positioned, deicing power is available to the various load-cycling contactors. Control of the load-cycling contactors and thereby operation of the deicing system is provided by the aforementioned single deicing control switch located at the pilot's station. Positioning of the switch to the "on" position provides power to a load-cycling timing circuit operating and controlling the load-cycling contactors. Necessary control of the timing circuit to provide a variable heating-cycle control for the deicing blankets, as previously brought out, is the remaining control provided at the copilot's station. All control equipment is d-c operated from the airplane's 28-volt d-c primary power system.

Table II

	Weight, Pounds
Deicing System Plus *NESA* and Radar Power	
Four 40-kva alternators with exciter at 79 pounds	316
Four regulators at 12.5 pounds	50
Four alternator mounting provisions	60
Deicing power wiring and switches	35.3
Deicing blankets at 1/2 pound per square foot	200.4
Deicing contactors	59.7
Deicing wiring, fuses	160.2
Deicing control	18.9
Miscellaneous deicing provisions	28
Radar power transformer and power provisions	23.5
NESA power transformer and power provisions	8.7
Total	960.7
Estimated Weight for Separate *NESA* and Radar Power System	
Two 8-kva alternators at 48 pounds	96
Two exciter-regulators at 22 pounds	44
Two alternator mounting provisions at 15 pounds	30
Radar power provisions	15
NESA power transformer and power provisions	15.7
Total	200.7
Net weight increase for deicing system	760

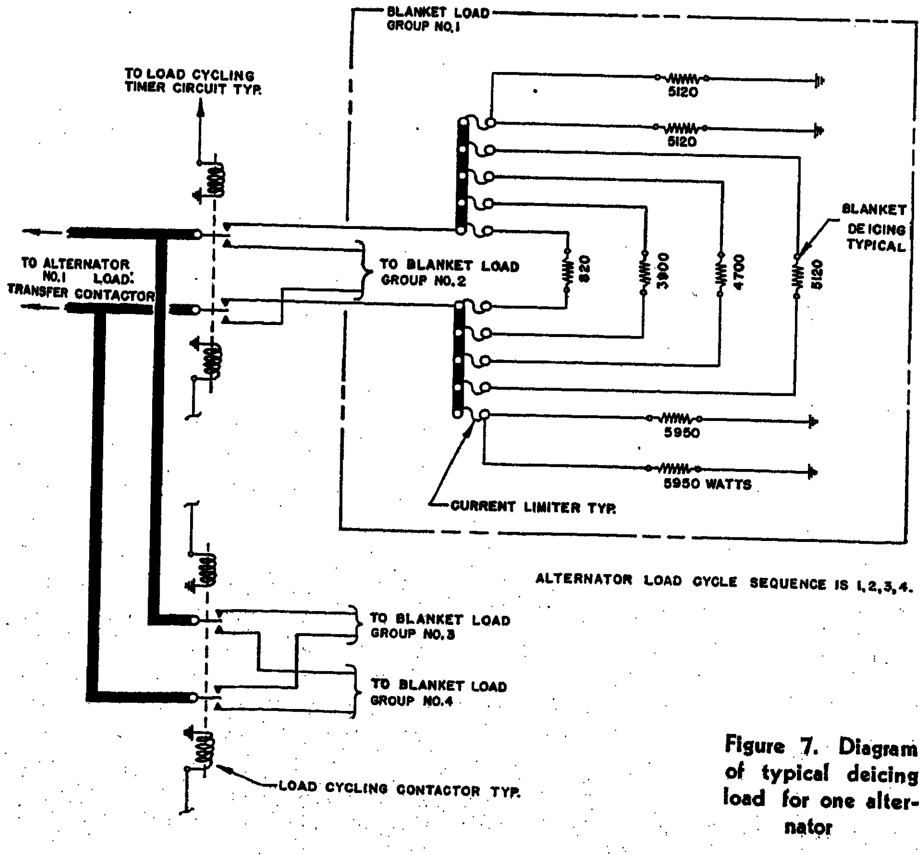

BLANKET LOAD GROUP NO.1
TO LOAD CYCLING TIMER CIRCUIT TYP.
5120
5120
BLANKET DEICING TYPICAL
TO ALTERNATOR NO.1 LOAD TRANSFER CONTACTOR
TO BLANKET LOAD GROUP NO.2
820
3900
4700
5120
5950
5950 WATTS
CURRENT LIMITER TYP.
ALTERNATOR LOAD CYCLE SEQUENCE IS 1,2,3,4.
TO BLANKET LOAD GROUP NO.3
TO BLANKET LOAD GROUP NO.4
LOAD CYCLING CONTACTOR TYP.

Figure 7. Diagram of typical deicing load for one alternator

Timing Circuit

The timing circuit controlling the load-cycling contactors consists of a motor-driven cam and a low travel limit switch controlling the coil of a stepping relay. The stepping relay advances with each operation of the limit switch, and completes the control circuits to the various load-cycling contactors of each alternator in sequence. Variations in the timing cycle are obtained through speed control of the motor-driven cam. The increased "off" or rest period control of the heating cycle is obtained by the stepping relay progressing through several "dead" or unused contact positions at a controlled rate before again picking up the various cycling contactor circuits. When the increased time "off" cycle is not desired, the stepping relay is stepped quickly through its unused contact positions such that a continuous and steady series of power intervals are supplied simultaneously to each alternator's load cycling contactors in a 1, 2, 3, 4,—1, 2, 3, 4 sequence.

Visual indication of the operation of the timing circuit and cycling contactors as well as alternator operation is provided in the form of four heating-cycle lights controlled by a load-check selector switch located at the copilot's station. Each light is connected to a pole of its respective cycling contactor and receives energy from the alternator supplying power to the particular load selected. With the load-check switch set for one of the three main divisions of load and with the system in operation, the four heating-cycle lights will operate in sequence and in step with their cycling contactor as each cycle of the load is energized. An "off" position is provided for the load-check selector switch so that the airplane crew need not contend with the load-cycling lights continuously during operation of the deicing system.

Circuit Protection

Protection of alternators, main power wiring, and blanket circuits is provided by single-phase aircraft limiters,[6] developed specifically for the 120/208-volt a-c system. The limiters are fusible metal links, similar to the common fuse, with an inverse-time–current characteristic particularly suitable for the protection of aircraft circuits. In the selection of limiter sizes the melting characteristics of the limiters were co-ordinated with the time–current smoking characteristics of the various gauge wires to provide protection against faulted or otherwise overloaded wiring.

The limiters for main power feeder protection are mounted in the nacelle regions near the alternators, while the bulk of the limiters associated with individual blanket circuit protection are installed at the power distribution center located in the fuselage in the bomb-bay area. Those limiters associated with individual tail surface blankets are installed in the aft section of the fuselage. None of the limiters are accessible in flight.

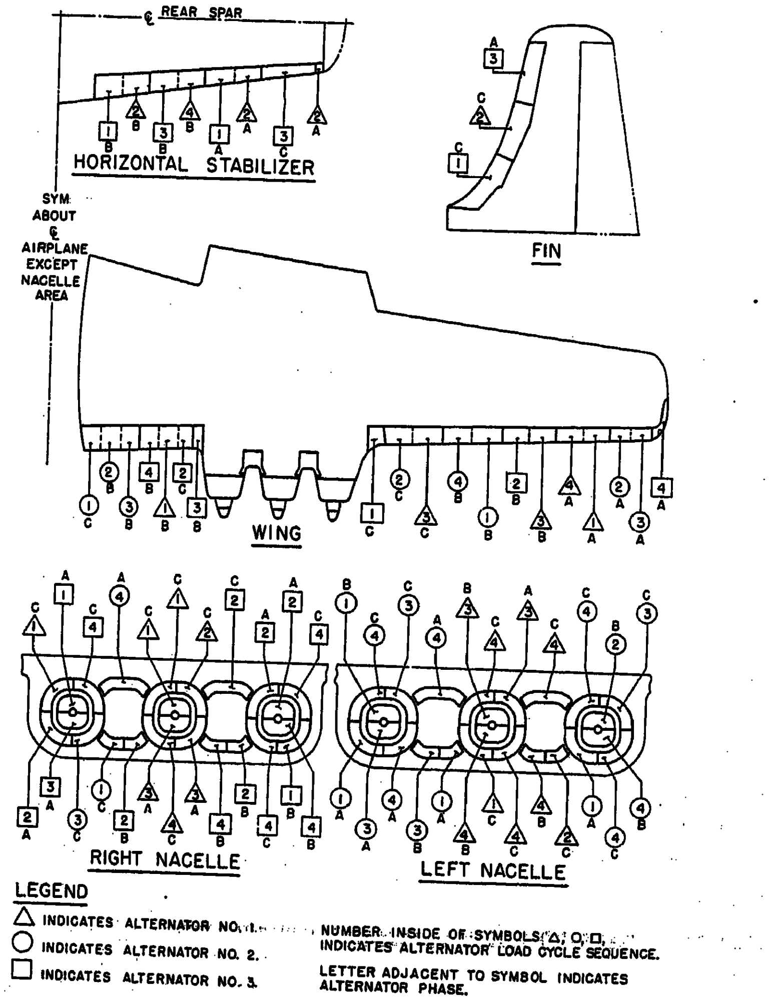

℄ REAR SPAR
HORIZONTAL STABILIZER
FIN
SYM ABOUT ℄ AIRPLANE EXCEPT NACELLE AREA
WING
RIGHT NACELLE
LEFT NACELLE
LEGEND
△ INDICATES ALTERNATOR NO. 1.
○ INDICATES ALTERNATOR NO. 2.
□ INDICATES ALTERNATOR NO. 3.
NUMBER INSIDE OF SYMBOLS (△, ○, □, INDICATES ALTERNATOR LOAD CYCLE SEQUENCE.
LETTER ADJACENT TO SYMBOL INDICATES ALTERNATOR PHASE.

Figure 8. Electrical breakdown of deicing blankets for alternator load

Weight

The net weight increase to the airplane for the electric deicing system amounts to 760 pounds, as indicated in Table II. This table in one part itemizes the total weight of the deicing system together with the power-system equipment necessary for the radar and *NESA* windshield loads. Also indicated is a weight total that would have been incurred for a radar and *NESA* power supply system alone if the airplane had not been electrically deiced. The difference between these two subtotals (760 pounds) is therefore the net weight incurred by the airplane for the electric deicing system. The deicing system weight falls within the generally accepted allowance of one per cent of airplane gross weight for ice protection systems.

Conclusions

Operational data and evaluation of the deicing system is not available at the present time. Regardless of the final findings, the installation will provide worthwhile information in the comparatively unexplored field of ice removal from wing and tail surfaces by the cyclic type of system. The electric deicing system should provide a desirable degree of operational reliability under adverse conditions, such as would occur with military aircraft in combat. The loss of one deicing alternator can be sustained with no impairment to the deicing system. Loss of two alternators may provide a flyable airplane in icing conditions, although with reduced deicing coverage. Damage to or loss of power to a deicing blanket will only locally affect the deicing ability of the system.

With individually protected multiple main power circuits, system failure due to loss of such circuits is remote.

Internal deicing blankets would appear to be more desirable than external blankets, although a selection may be largely dependent upon the airplane's construction as was the case with the present installation. External blankets are susceptible to damage by service personnel during airplane ground handling and by stones, and so forth, kicked up during take-off and landing. In flight the external blanket also is subject to wear from the impact of rain and sand.

On the other hand, internal blankets would be protected by the airplane metal skin with a resulting longer normal service life. Internal blankets would, however, require an increased power input and a longer cycle time due to the additional material between the blanket heating element and the ice.

The electrical method of ice protection may or may not provide the optimum in ice removal systems, but it does possess the highly desirable feature of flexibility and is certain to remain as a useful ice removal means in a capacity that will be determined by time alone.

References

1. N. A. C. A. Research Ends Ice Hazard, Robert McLarren. *Aviation Week*, (New York, N. Y.), December 22, 1947, page 24.

2. Electric Anti-Icing of Aircraft Windshields, J. D. McCrumm. *Electrical Engineering* (*AIEE Transactions*), volume 65, December 1946, pages 786–91.

3. A 40-Kva 400-Cycle Aircraft Alternator, H. E. Keneipp, C. G. Veinott. *Electrical Engineering* (*AIEE Transactions*), volume 63, November 1944, pages 816–21.

4. Constant-Speed Drives for Aircraft Alternators, C. J. Breitwieser. *Electrical Engineering* (*AIEE Transactions*), volume 64, November 1945, pages 763–7.

5. Alternating-Current Contactors for Aircraft, F. J. Russell, A. P. Charbonneau. *Electrical Engineering* (*AIEE Transactions*), volume 63, September 1944, pages 613–6.

6. A Current-Limiting Fuse for Aircraft Applications, A. H. Powell. *Electrical Engineering* (*AIEE Transactions*), volume 64, July 1945, pages 541–5.

No Discussion

Insulator Flashover Deionization Times as a Factor in Applying High-Speed Reclosing Circuit Breakers

A. C. BOISSEAU
ASSOCIATE AIEE

B. W. WYMAN
MEMBER AIEE

W. F. SKEATS
FELLOW AIEE

SINCE the initial tests and trial application of high-speed reclosing to high-voltage tie-line circuit breakers a little over 13 years ago by Philip Sporn and D. C. Prince,[1] high-speed reclosing of such circuit breakers has become standard practice on most modern American transmission systems. Today, when capital investment and operating costs are more important than ever before, high-speed reclosing is almost universally recognized by transmission engineers in this country as one of the most economical methods of:

1. Increasing the maximum power which can be transmitted over long high-voltage tie-lines without loss of synchronism following a short-circuit tripout. This is shown graphically for a typical system in Figure 1.
2. Reducing system disturbance and shock at the instant of reclosing by reconnecting the two parts of the system before they have swung too far apart. Figure 2 shows how the angular displacement between the two ends of a typical system increases with reclosing time.
3. Reducing line outage time and improving service to customers.

When Sporn and Prince made their original investigation of high-speed reclosing, the primary limitation was the operating speed of available circuit breakers which, in comparison with present-day circuit-breaker speeds, was relatively slow. The interrupting time from energizing of the circuit-breaker trip coil to interruption on standard high-voltage circuit breakers was eight cycles. The time required after interruption to again reclose contacts was 27 cycles. By definition, the reclosing time of a circuit breaker is the sum of these two values, or 35 cycles from the energizing of the circuit-breaker trip coil to the recontacting of the arcing contacts. This time was sufficiently long to permit loss of synchronism on many long, heavily loaded tie-lines. Stability studies showed that much heavier loads could be transmitted without loss of synchronism if the reclosing time could be reduced.

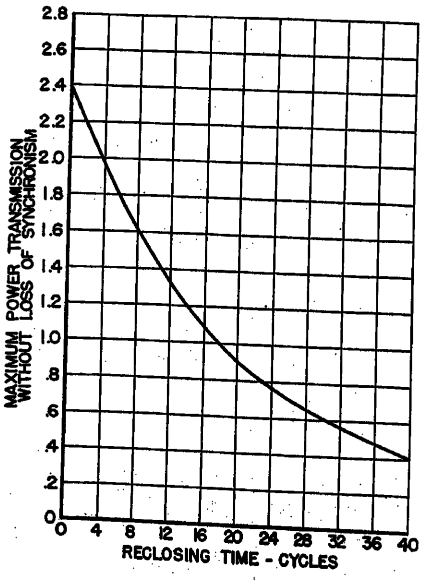


Figure 1. Maximum per unit power which may be transmitted without loss of synchronism during transient faults as a function of total circuit breaker reclosing time for a typical system

With a given circuit-breaker interrupting time, the minimum deenergized time for successful reclosure is the time required for the flashover arc path to deionize sufficiently to prevent arc reignition when the line is reenergized. Sporn and Prince first studied this time for 132-kv insulator flashovers, both in the high-power laboratory and on an operating transmission line. Although their studies showed considerable variation, they found that if the fault current lasted eight cycles, 12 cycles of deenergized time was generally adequate to prevent arc reignition upon reclosure. This gave 20 cycles as the total necessary reclosing time. Special high-speed mechanisms were then developed for 138-kv circuit breakers capable of 20-cycle reclosure and were installed on the 132-kv line between Marion, Ohio and Ft. Wayne, Ind. The results obtained on the initial installation were so promising that manufacturers immediately embarked on a program of developing standard high-voltage circuit breakers with pneumatic mechanisms which could be reclosed in 20 cycles or less. As the interrupting time of the circuit breaker

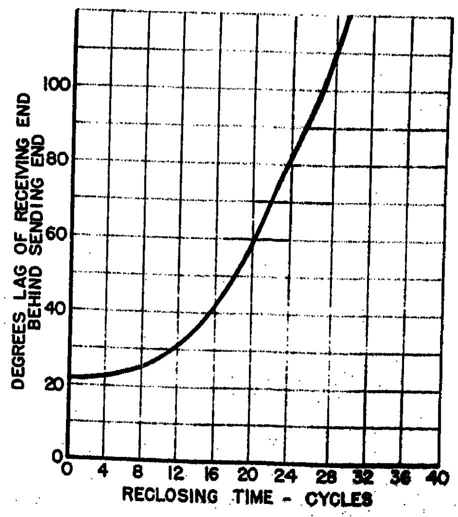
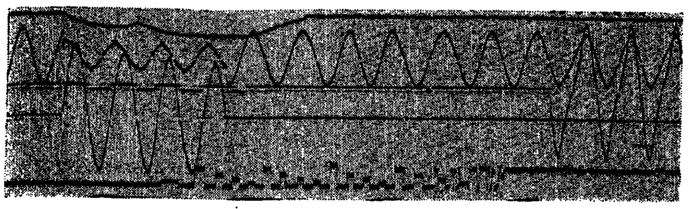


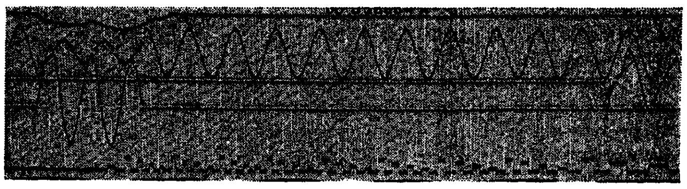
Figure 2. Angular displacement in degrees of system at receiving end of a tie-line behind system at sending end at instant of reclosing as a function of reclosing time

---

Paper 49-221, recommended by the AIEE Switchgear Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Pacific General Meeting, San Francisco, Calif., August 23-26, 1949. Manuscript submitted May 25, 1949; made available for printing July 9, 1949.

A. C. Boisseau, B. W. Wyman and W. F. Skeats dre all with the General Electric Company, Philaelphia, Pa.





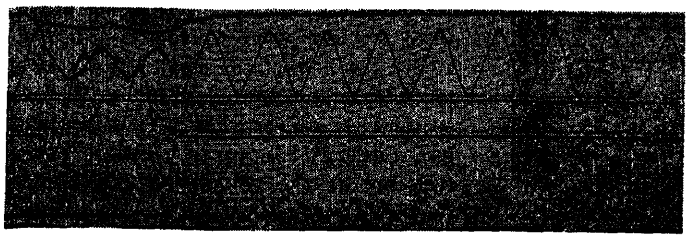


Figure 3(A) (upper left). Oscillogram of typical reclosing test with reclosing occurring at voltage zero showing absence of overvoltage oscillation at instant of reclosing

Figure 3(B) (upper right). Oscillogram of typical reclosing test on 230-kv suspension insulator with reclosing taking place at voltage crest showing double overvoltage oscillation at instant of reclosing

Figure 4(B) (lower left). Oscillogram of 8,000-ampere flashover shown in Figure 4(A) showing interruption of fault current in 2.3 cycles followed by reclosing operation eight cycles later at a voltage crest resulting in immediate reignition

Trace 1—trip coil current
Trace 2—generator voltage
Trace 3—arc voltage
Trace 4—short-circuit current
Trace 5—circuit breaker travel

also is important in increasing the maximum power which can be transmitted without loss of synchronism, the interrupting times of standard circuit breakers were subsequently reduced to five and three cycles. The results of nearly nine years operating experience with 20-cycle reclosing on the American Gas and Electric Company system have been outstanding and have shown that nearly 90 per cent of all faults of any type have been successfully reclosed at this speed.

Purpose of Present Investigation

Until recently, 15 to 20 cycles represented the minimum reclosing time which could be obtained with most high-voltage circuit breakers. However, recent developments in high-speed reclosing impulse circuit breakers effected such a drastic reduction in the time for the circuit breaker to reclose its contacts after interruption, that it became evident that the limitation on over-all reclosing time would most likely be the time required to deionize the fault arc, rather than the circuit breaker reclosing speed. Tests at Grand Coulee Dam last year on a modern high-speed impulse circuit breaker showed it capable of reclosing 8,700,000-kva faults in 8.6 cycles. The only remaining limitation, therefore, is the required deionization time of the flashover path around the insulator string. With circuit-breaker reclosing times adjusted down to the maximum flashover deionization time, maximum power transmission can be achieved without danger of loss of synchronism. There are a number of factors which will cause this to vary between systems. Several investigations have been made both here and abroad to study the effect of some of these factors at a particular voltage which was of interest to the investigator. Unfortunately, however, much of the information obtained has not been published, and that which has been published has not been sufficiently well co-ordinated to make a ready reference.

A general investigation was, therefore, undertaken to determine the minimum necessary deionization times for insulator flashovers on 69-kv to 230-kv circuits under various conditions which would be encountered in actual service.

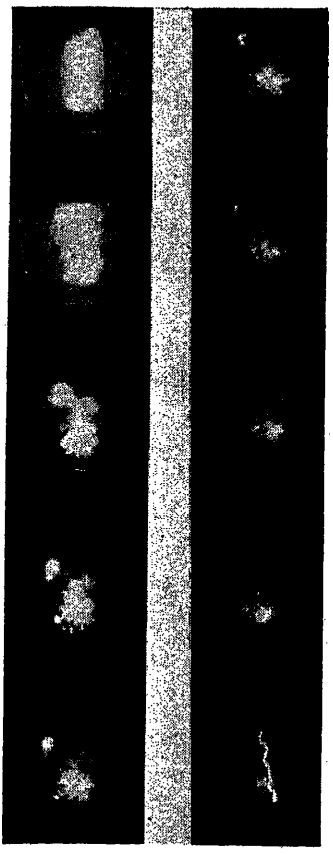


Figure 4(A). Pictures from 4,000-frame-per-second film of an 8,000-ampere flashover interrupted in 2.3 cycles and reclosed eight cycles later at a voltage crest with arc reignition taking place. (Only one picture per cycle shown)

Variable Factors in Determining Necessary Deionizing Time

Some of the more important factors in determining necessary deionization time for insulator flashovers are:

1. Magnitude of short-circuit current. The higher the fault current, the larger the amount of ionized gas which will be generated. This will tend to be offset somewhat by the greater amount of turbulence initiated in the surrounding air causing greater swirling and mixing with cool air. Also the magnetic blowout action increases with higher currents tending to loop the arc away from the insulator and lengthen it. Maximum short-circuit currents at high-voltage have increased several-fold since earlier investigations were made.

2. Duration of short-circuit current. The longer the fault current flows, the greater the total heat energy liberated. This will tend to be offset somewhat by greater turbulence. Also longer magnetic blowout action with the longer current duration may loop the arc out farther away from the insulator before the current is interrupted. When the initial 132-kv tests were made, standard circuit breaker interrupting times were eight cycles and tests were conducted with fault currents of this duration. Since that time standard circuit breaker interrupting times have been reduced to five and three cycles which besides reducing the total reclosing time by a corresponding amount, may have some effect on the necessary de-energized time.

3. Magnitude and duration of capacitive coupling follow-current which may flow due to induced voltage on de-energized conductor if adjacent conductors remain energized. This assumes particular importance when single-phase reclosing circuit breakers are used.

4. Magnitude and duration of resistance follow-current which may flow momentarily through shunting resistors with some types

of circuit breakers which are equipped with shunting resistors in parallel with the main interrupting contacts. This was not considered in the original investigation by Sporn and Prince since no high-voltage circuit breakers used in this country at that time were equipped with resistors.

5. Magnitude of re-energization voltage applied.

6. Point on voltage wave at which circuit is re-energized. This determines the magnitude of the surge. If re-energized at a zero point, no surge will be present. If energized at a crest voltage, a surge approaching twice crest voltage will occur. This was not considered in previous investigations and is believed to account for much of the randomness in the results obtained.

7. Configuration of the current path at the terminals of the arc. If the current path forms right angles at either end of the arc, magnetic forces will tend to loop the arc away from the insulator string.

8. Length of insulator string which determines the minimum length of flashover path.

9. Weather conditions. A strong wind will tend to lengthen the arc prior to interruption and also tend to carry the ionized gas column bodily away from the insulator string after interruption. The tendency of wind to lengthen the arc prior to interruption will be minimized with fast tripping circuit breakers.

10. Shape of grading rings and other parts at the end of the suspension insulator which determine the distribution of dielectric stress when voltage is reapplied.

The first six items were considered to be of the greatest importance in determining the necessary de-energization time for deionization. Tests were therefore made to determine the effect of each of these. These tests were made in the high-power testing laboratory rather than an actual power system for the following reasons:

1. A very large number of tests would be required to complete the investigation. These tests could be scheduled with greater ease in the laboratory than on an actual system.

2. A much wider range of conditions could be obtained in the laboratory.

3. Previous experience had shown that previous flashover tests made in the high-power laboratory had given results very comparable to those obtained on field tests.

Tests Made

In order to cover the range of fault currents normally encountered at the present time or anticipated in the near future, insulator flashover tests were made at approximately 1,000, 8,000, and 25,000 amperes with re-energization potentials equal to the phase voltages of 69, 138, and 230 kv. This range can be obtained readily in the high-power testing laboratory by supplying the fault current from one voltage source and the re-energization potential from a second voltage source. This combination of voltages and currents represents the following range of short circuit kilovolt-amperes which cover the range encountered in most systems of these voltage classes at the present time.

Kilovolts	Amperes	Kilovolt-Amperes
69	1,000	120,000
69	8,000	950,000
69	25,000	3,000,000
138	1,000	240,000
138	8,000	1,900,000
138	25,000	6,000,000
230	1,000	400,000
230	8.000	3,200,000
230	25,000	10,000,000

To be able to apply high-speed reclosing circuit breakers to all types of systems, flashover deionizing data were desired under conditions which would simulate the following types of circuits:

1. Single-circuit transmission line protected with triple-pole circuit breakers.

2. Double-circuit transmission line with both lines on same tower, each circuit being protected by triple-pole circuit breakers.

3. Single- or double-circuit transmission line protected by single-pole reclosing circuit breakers.

Test Procedure

All arcs were initiated by a fine fuse wire tied over the surface of the string. Standard Locke suspension insulators were used with the following number of units for each voltage:

Kilovolts	Units
69	5
138	10
230	15

A sufficient number of tests were made under each condition to establish the maximum probable deionizing time. It was felt that much of the randomness encountered by previous investigators was due to the magnitude of the surge which was determined by the point on the voltage wave at which re-energization took place, for while no voltage appreciably higher than normal crest occurs when re-energization takes place at or near a voltage zero, there is an overshoot almost to double when re-energization takes place at the crest. This is well illustrated in the two oscillograms of Figure 3. Both of these films represent successful reclosures after an 8,000-ampere flashover on a 230-kv insulator string with a reclosing voltage equal to the leg of 230 kv. In the first, reclosing took place near a zero point on the voltage wave with little oscillation present. In the second, reclosing took place at a voltage crest, oscillating to approximately double.

A synchronous closing switch capable of accurately selecting the closing angle was therefore used to re-establish the circuit throughout the tests. Test series made under the two conditions with this switch gave no restrikes after a de-energized interval above $6^{1}/_{4}$ cycles when reclosing at the voltage zero, although four tests were made with reclosing times between $6^{3}/_{4}$ and $7^{3}/_{4}$ cycles; when closing on the voltage peak on the other hand, restrikes were obtained at $7^{1}/_{2}$, 8, $8^{1}/_{2}$ and $9^{1}/_{2}$ cycles, with only one successful reclosure at an interval less than $9^{1}/_{2}$ cycles. There is thus a difference of about three cycles between the deionizing times for the two conditions while the

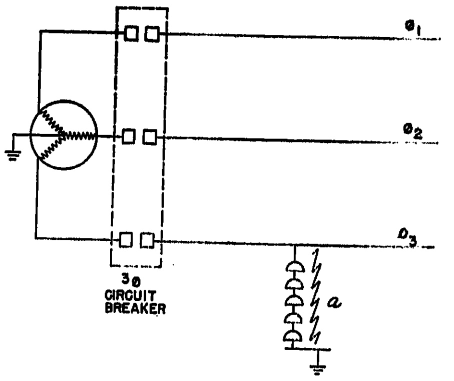


Figure 5(A). Single-circuit transmission line protected with triple-pole high-speed reclosing circuit breaker with single-line-to-ground flashover at point a on phase 3

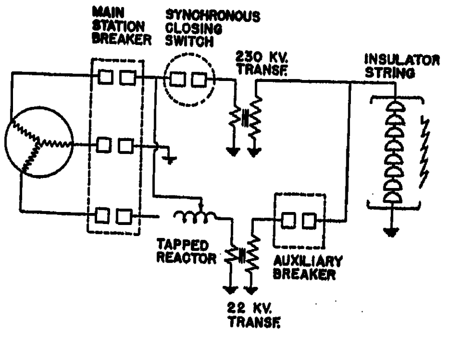


Figure 5(B). High-power laboratory circuit used to simulate flashover on single-circuit transmission line showing main station protective circuit breaker, tapped reactor for varying the magnitude of short-circuit current, 22-kv transformer for supplying fault current to the flashover, auxiliary circuit breaker for interrupting fault current, and synchronous switch for closing the high-voltage transformer for supplying the recovery voltage at the desired point on the wave

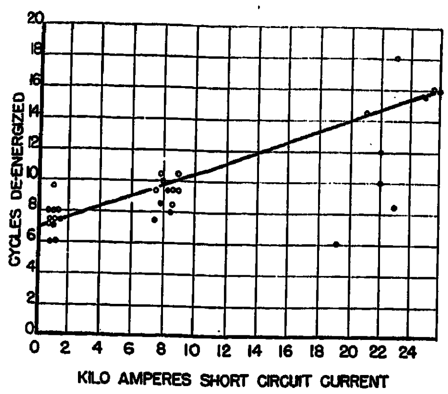


Figure 6. Results of high-speed reclosing tests after flashover on 230-kv suspension insulator with various values of short-circuit current

Black circles represent restrikes; open circles represent successful reclosure. Dividing line just above maximum restriking points represents absolute minimum de-energization time to prevent restrikes

data for each condition are quite consistent. It therefore appears certain that much of the randomness reported by previous investigators was due to lack of control of the closing point on the reclosing voltage wave. After proving the effect of this variable, all subsequent tests were made reclosing at a voltage crest to simulate the most severe conditions. This reduced considerably the number of tests necessary at each voltage and current condition to establish necessary deionizing times.

A 4,000-frame-per-second camera was used to photograph the tests to determine how and where arc re-establishment took place if encountered. A 60-cycle neon bulb exposure was printed on the side of the film to aid in correlating with oscillographic data. Figure 4 (A) shows every 66th picture (one picture from each cycle) of an 8,500-ampere flashover test on a 230-kv insulator string with a fault duration of 2.3 cycles followed by a de-energized time of 8.0 cycles with re-energization at crest voltage causing arc reignition. Figure 4(B) shows the oscillogram of this same test. Examination of each picture during the arcing period shows that the diameter and brilliance of the ionized gaseous arc column increased and decreased with the instantaneous current. This conforms with the well-known fact that the arc resistance varies inversely with current so that the voltage drop along the length of the arc is practically constant. The several thousand degree centigrade temperature core at the center of the arc sets up considerable turbulence and eddies in the surrounding air. Each small loop which is originated by turbulence then tends to be magnified by magnetic action. When the current flow is interrupted, the turbulence set up in the surrounding air aids in deionizing and cooling the column as can be seen from picture 3 to 9 in Figure 4 (A). When the insulator was re-energized from a 132-kv source at a voltage crest, an oscillation having a peak of 360 kv occurred which caused breakdown, as can be seen in the last picture.

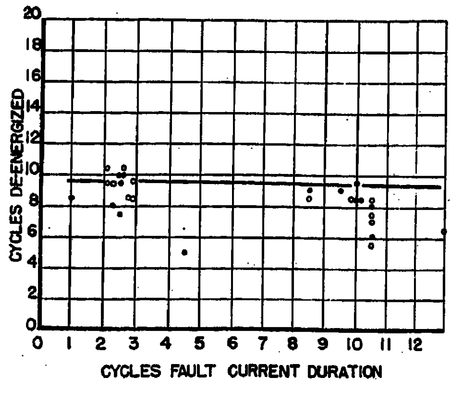


Figure 7. Results of tests varying the duration of fault current prior to interruption showing its effect upon necessary time to deionize arc

Tests made on 230-kv suspension insulator with recovery voltage equal to phase voltage of 230 kv. Fault current in all tests equals 8,000 amperes. Black circles represent restrikes, and open circles represent successful reclosures

Triple-Pole Reclosing—Single-Circuit Transmission Line

Figure 5 (A) represents a single circuit transmission line protected by a triple-pole high-speed reclosing circuit breaker without shunting resistors with a flashover from phase-3 to-ground over an insulator. A tie-line between two systems would have a similar high-speed reclosing circuit breaker at the other end also, but since both circuit breakers would normally be tripped and reclosed simultaneously, the other end may be disregarded in determining the deionizing time of the flashover path *A*. As phases 1 and 2 are opened with phase 3, they may be neglected when simulating this circuit in the high-power laboratory.

Figure 5 (B) shows the laboratory circuit used to determine the required deionization time of a flashover arc under this condition. The high-current short circuit was supplied from a 22-kv transformer connected to one phase of the high-power generator with the fault being interrupted by auxiliary circuit breaker. Re-energization potential equal to the phase voltage of 69-, 138-, and 230-kv circuits was supplied from a second transformer connected to the same phase.

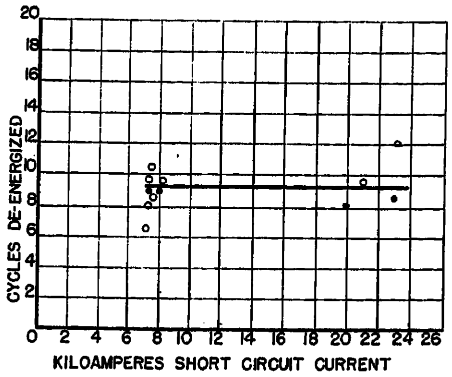


Figure 8. Results of high-speed reclosing tests after flashover on 138-kv suspension insulator with various values of short-circuit current

Black circles represent restrikes; open circles represent successful reclosures. Dividing line just above maximum restriking points represents absolute minimum de-energization time to prevent restrikes

Any desired number of cycles, as well as the point on the voltage wave, could be selected for re-energization by closing this circuit through the synchronous closing switch in the primary of the high-voltage transformer circuit.

Figure 6 shows a plot of results obtained following 1,000-, 8,000-, and 25,000-ampere flashovers on a 15-unit insulator string with 3-cycle fault duration and 132-kv re-energization voltage, which corresponds to the phase voltage of a 230-kv system. The open circles represent successful reclosures while the solid circles represent restrikes. By plotting the longest unsuccessful reclosing time as the necessary deionization time for that current, it will be noted that the necessary deionization time for 230-kv systems increases linearly with the short-circuit

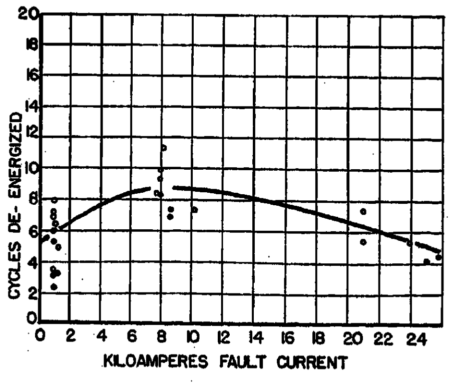


Figure 9. Results of high-speed reclosing tests after flashover on 69-kv suspension insulator with various values of short-circuit current

Black circles represent restrikes; open circles represent successful reclosures. Dividing line just above maximum restriking points represents absolute minimum de-energization time to prevent restrikes

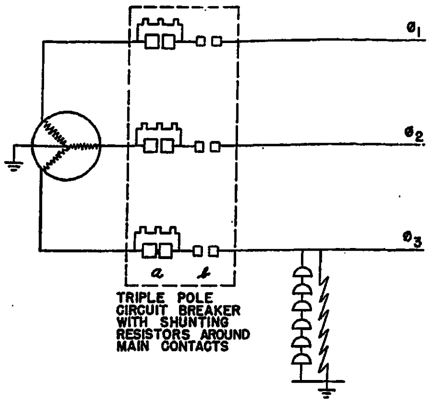

ø1
ø2
ø3
a
b
TRIPLE POLE CIRCUIT BREAKER WITH SHUNTING RESISTORS AROUND MAIN CONTACTS

Figure 10(A). Single-circuit transmission line equipped with triple-pole circuit breakers having shunting resistors with insulator flashover on phase 3

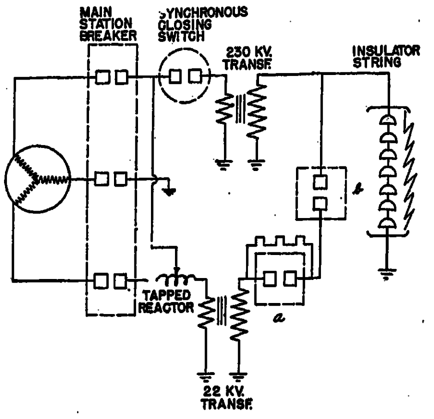


Figure 10(B). High-power laboratory circuit used to simulate reclosing with various deenergization times after interruption of fault current and 5-cycle resistor follow current

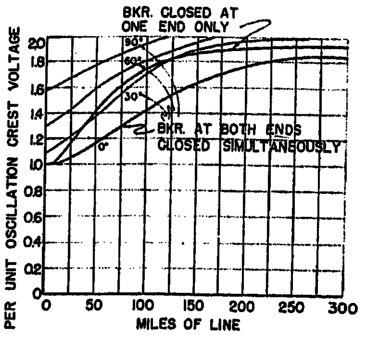


Figure 11. Maximum overvoltage oscillation on various lengths of 230-kv transmission line when reclosed at crest voltage by circuit breaker equipped with shunting resistors of 2,700 ohms per pole

current. By adding three cycles for arc interruption to each of these values for the circuit breaker to interrupt, the minimum total reclosing time for 3-cycle circuit breakers is obtained. For example, if high-speed reclosing 3-cycle circuit breakers were used on a 230-kv system where the maximum fault current was 9,000 amperes, a de-energized time of 10 cycles would be required. This added to the 3-cycle interrupting time results in a total minimum reclosing time of 13 cycles. If applied on a system with possible fault currents up to 25,000 amperes, the total reclosing time would have to be increased to 16 plus 3, or 19 cycles. If system studies on an actual transmission system having possible fault current of 25,000 amperes showed a considerable gain in transient stability by using a slightly shorter reclosing time than 19 cycles, the transmission engineer might decide to use the shorter time on the basis that most of the faults on the system would be at short-circuit currents appreciably below 25,000 amperes. Under these circumstances, the percentage of faults exceeding a predetermined lower value of fault current, with allowance for off-peak reclosing, would have to be balanced against the gain in transient stability which could be realized.

Many systems are equipped with circuit breakers having a longer interrupting time than three cycles. To determine the effect on deionization time of the greater amount of ionized gas generated, a series of tests were made with 10-cycle fault durations and approximately 8,000 amperes. Figure 7 shows the results of these tests as compared with 3-cycle fault durations. This figure shows that the maximum deionization times for 10-cycle faults are approximately equal to those of 3-cycle faults. This may indicate that the larger amount of ionized gas was offset by the greater amount of turbulence and looping due to magnetic blowout action, which does not agree completely with previous investigations.

Figure 8 is a plot of tests taken on a 10-unit insulator string with a re-energization potential of 80 kv, representing phase voltage of 132-kv systems. It will be noted that instead of increasing with higher currents as it did at 230 kv, the deionization time remains approximately constant up to 25,000 amperes.

Figure 9 is a similar plot of tests taken on a 5-unit insulator string re-energized with 44 kv, corresponding to phase voltage of a 69-kv system. It will be noted that for such short insulator strings, the extra magnetic blowout action more than offsets the extra amount of ionized gas generated at high currents. Although Figure 9 shows the results only up to approximately 25,000 amperes, successful reclosure was obtained in 2.3 cycles after 43,000 amperes which indicates this decreasing trend continues at even higher currents.

By adding these deionizing times to the circuit breaker interrupting time, the minimum possible total reclosing time will be obtained. How near these points circuit breakers should be adjusted will depend upon the stability margins of the particular system under consideration. If the stability margin is large, no advantage would be gained by adjusting for the absolute minimum reclosing time. Under this condition, an appreciable margin of safety would normally be added to these values to allow for the most unfavorable combination of circumstances. On the other hand, if the stability margin is small, the reclosing time at which it would be desirable to operate is as close to the minimum as possible.

Triple-Pole Reclosing by Circuit Breaker Equipped with Shunting Resistors—Single-Circuit Transmission Line

Many modern high-voltage circuit breakers are equipped with shunting resistors to eliminate overvoltages when interrupting the charging current of long unloaded transmission lines. When interrupting fault currents, the contacts of this type of circuit breaker interrupt the main fault current in either three or five cycles. The shunting resistor current in most such circuit breakers does not exceed 50 amperes and is interrupted by secondary contacts a few cycles later. Figure 10 (A) represents a single-circuit transmission line protected by triple-pole circuit breakers equipped with shunting resistors. Figure 10 (B) shows the laboratory circuit to simulate insulator flashovers under these conditions. Two separate circuit breakers were used to simulate the two sets of contacts. Circuit breaker *A*, which is shunted by the resistor, represents the main contacts, while circuit breaker *B* represents the secondary contacts. By varying the tripping time of circuit breaker *B*, the duration of the secondary resistor current could be varied at will. In most tests, the resistor current was interrupted approximately five cycles after the main fault current, which represents somewhat longer times than are normally encountered on modern circuit breakers. The value of resistance selected at each voltage limited the magnitude of the resistor current to 50 amperes.

Figure 12 (A) shows a series of pictures obtained on a test made on this circuit. In this figure, every 66th frame taken with the 4,000-frame-per-second camera (1 picture per cycle) is shown. In the test a 230-kv insulator string was flashed over by an 8,000-ampere arc, interrupted in 3.9 cycles, after which there was a

ure 12(A). Pictures from 4,000-frame-
-second film of 8,000-ampere flashover
rrupted in 3.9 cycles followed by 50-
pere resistance follow current of 4.7-cycles
ation and then de-energized for seven
les before being reclosed at a voltage crest
with restriking taking place

50-ampere resistance follow current for 4.7 cycles. The circuit was then de-energized for seven cycles and re-energized at a voltage crest with arc reignition taking place. Figure 12 (B) shows the oscillogram of the same test showing restriking after seven cycles. Figure 12 (C) is an oscillogram of the same current but with a de-energized time of 8.3 cycles after which reclosure was successful at a voltage crest.

In reclosing, a resistance shunted circuit breaker will first connect the circuit through a resistor and then short out the resistor. When reclosure on a transmission line takes place through a resistor of 2,700 ohms, which is a typical value, the oscillation is completely damped out for transmission line lengths of a mile or more, so that even though this reclosure may take place at a voltage crest, restriking of the fault arc would correspond to voltage zero conditions. Under this condition, tests at 8,000 amperes at a re-established voltage corresponding to 230 kv indicated that only $2^1/_2$ cycles of dead time are required.

When the resistance is finally shorted out, an oscillation takes place with an amplitude equal to the instantaneous voltage across the resistor just before it is shorted. The corresponding overvoltage increases with the length of the line as shown in the curves of Figure 11, which shows the overvoltage corresponding to shorting the resistor at the worst point on the voltage wave for the following conditions:

1. Circuit breaker at one end of the line fully closed before the circuit is closed through the resistor at the other end.

2. Both ends of line connected through resistors before either resistance is shorted out. Under this condition the amount of overshoot will vary with the phase angle between the two ends of the line at the time of reclosure. Curves are shown for angles of 0, 30, 60, and 90 degrees.

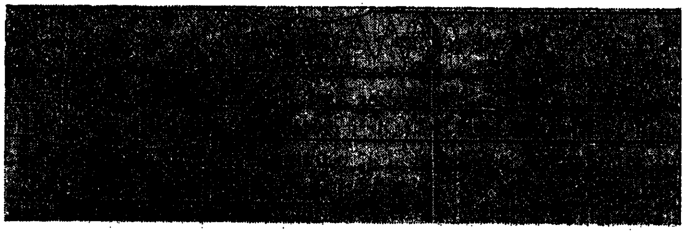
The overvoltage is greater for long than for short lines. For lines up to 100 miles in length, a 75 per cent overvoltage represents conditions approaching maximum practical severity.

In the test circuit, damping was eliminated by not reinserting the resistors during the reclosing operation. By varying the reclosing point on the voltage wave, any amount of instantaneous reclosing voltage oscillation from zero to twice crest could be obtained the same as in the tests without resistors. These data could then be directly compared with the previous tests made. Results similar to reclosing a short line with resistance-shunted circuit breakers could therefore be obtained by closing near a voltage zero where no overvoltage oscillation would occur. Results similar to reclosing a long line with resistance shunted circuit breakers could be obtained by closing at the crest of the voltage wave which gave twice crest oscillations. These tests showed that when a resistance-shunted circuit breaker which had a 5-cycle 50-ampere resistance follow current interrupted an 8,000-ampere flashover on a 230-kv insulator string, successful reclosure could always be obtained when reclosed at a voltage zero after a de-energized time of four cycles. Added to the five cycles of resistor current, this makes a total of nine cycles after the main interruption of the fault current for successful reclosure on a short transmission line. This agrees closely with the necessary de-energized time shown in Figure 6 without resistor follow current. When this test circuit was re-energized at crest voltage after an 8,000-ampere flashover to simulate long-line reclosing where overvoltage oscillation might be encountered, several cycles extra time were required. In some cases, the necessary de-energized time after resistor current was almost equal to the necessary de-energized time after fault current without resistors. For reclosing on very long lines, therefore, the duration of the resistor follow current should be added to the values given in Figure 6 to obtain

ure 12(B). Oscillogram of 8,000-ampere
hover test shown Figure 12(A) on 230-kv
ension insulator interrupted in 3.9 cycles
owed by 50-ampere resistor current for 4.7
les. Insulator then de-energized for seven
les before being reclosed at a voltage crest
causing arc reignition

Trace 1—trip coil current-circuit breaker *B*
Trace 2—generator voltage
Trace 3—arc voltage
Trace 4—short-circuit current
Trace 5—trip coil current-circuit breaker *A*
Trace 6—resistor current
Trace 7—travel-circuit breaker *B*

Figure 12(C). Oscillogram of 8,000-ampere flashover test on 230-kv suspension insulator interrupted in 3.5 cycles followed by 50-ampere resistor current for five cycles. Insulator then de-energized for 8.3 cycles before being successfully reclosed at a voltage crest



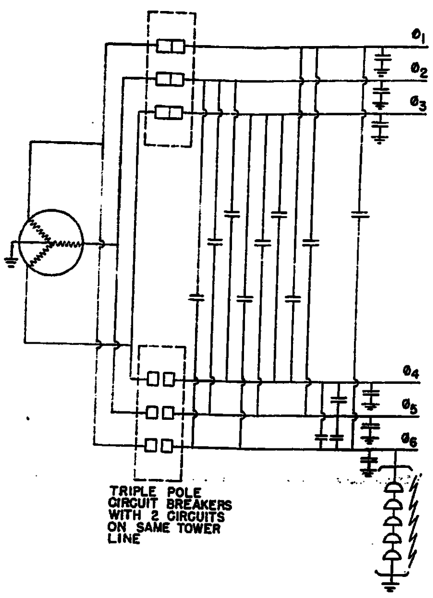

ø1
ø2
ø3
ø4
ø5
ø6
TRIPLE POLE CIRCUIT BREAKERS WITH 2 CIRCUITS ON SAME TOWER LINE

Figure 13(A). Double-circuit transmission line with both circuits mounted on the same towers and with each circuit protected by its own triple-pole high-speed reclosing circuit breaker with insulator flashover on phase 6

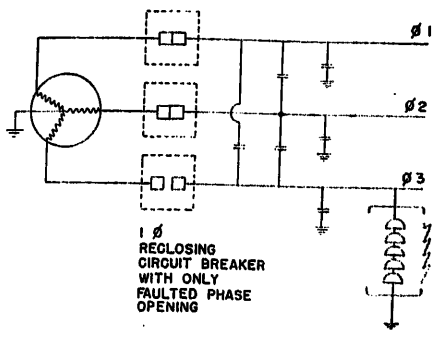
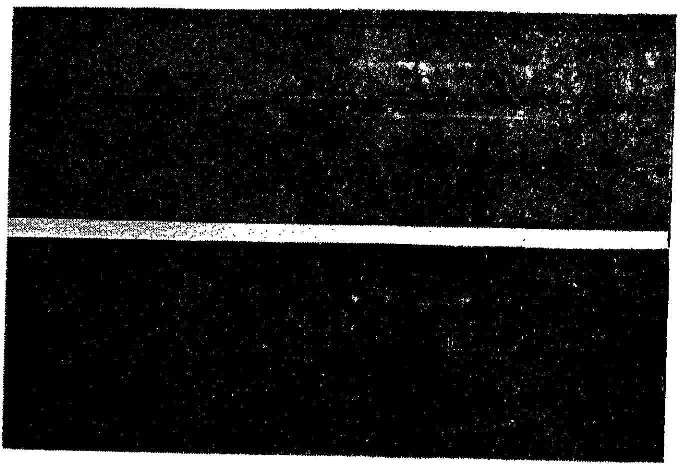

ø1
ø2
ø3
1 ø RECLOSING CIRCUIT BREAKER WITH ONLY FAULTED PHASE OPENING

Figure 14(right). Three-phase transmission line protected by high-speed single-pole reclosing circuit breakers with insulator flashover on phase 3

Figure 15 (lower right). Oscillogram of 7,100-ampere flashover on 230-kv suspension insulator interrupted in 3 cycles with capacitive coupling voltage of 22 kv. Capacitance current from 4.5 microfarads coupling representing 300 miles of line is interrupted only after 22 cycles



Trace 1—trip coil current
Trace 2—generator voltage
Trace 3—capacitor current
Trace 4—arc voltage
Trace 5—short circuit current
Trace 6—circuit breaker travel

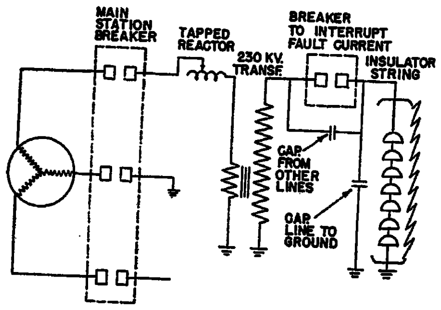


Figure 13(B). High-power laboratory circuit used to determine the length of time required for interruption of capacitive follow current with various values of capacitive coupling voltage tending to sustain arc

the minimum possible reclosing time.

Most systems will fall somewhere between the short-line and long-line case. For these systems, an intermediate value of resistor current duration based upon the relative overvoltage obtainable in Figure 11 may be added to determine the minimum possible reclosing time.

Triple-Pole Reclosing—Double-Circuit Transmission Line

Figure 13 (A) represents a double-circuit transmission line with both circuits mounted on the same towers and with each circuit protected by its own reclosing circuit breaker. When a fault occurs on one circuit, only the circuit breaker on that circuit will open. The capacitive coupling between the isolated line and the energized line causes the isolated conductors to float at some intermediate voltage. This voltage, if high enough, will feed current through the flashover path as long as it is still partially ionized. If the voltage and current are small enough, this capacitive follow arc will interrupt itself as the ionized flashover path cools. On the other hand, if this capacitive coupling arc is supplied by a high enough voltage and current, it will represent a new stable arcing condition and would arc indefinitely so that the circuit can never be successfully reclosed.

Figure 13 (B) shows the test circuit used to simulate the capacitive coupling conditions to determine the length of time required for final interruption of this capacitive arc. The voltage at which the unenergized conductors tend to float is determined by the ratio of the circuit-to-circuit capacitance to the circuit-to-ground capacitance. The current which will flow is determined by the values of the capacitances. These tests showed that capacitive currents were interrupted successfully in two cycles or less as long as the coupling voltage did not exceed 10 kv with short initial short-circuit durations. Considerable randomness was encountered in the length of time required for interruption, but this is to be expected in such a nonstable arc. The magnitude of the coupling voltage appeared to be much more important than the current in the arc, for 5-ampere arcs required as long to interrupt as 50-ampere arcs.

Fortunately capacitive coupling voltages at which one circuit of a double-circuit transmission line tends to float with normal transposition are appreciably below 10 kv. As shown in Appendix I, about 5 kv may be expected on a 220-kv circuit without transposition, and less than 3 kv with reverse transposition. With the normal values of capacitance coupling encountered from such operation, therefore, the follow current will not generally continue for more than one or two cycles. Triple-pole reclosing may, therefore, be used on 230-kv double-circuit transmission lines by allowing one or two cycles extra time in the reclosing cycle for interruption of capacitive follow current. This agrees with previous experience and with operating data on 132-kv systems.

Single-Pole Reclosing—Single- or Double-Circuit Transmission Line

Figure 14 represents a 3-phase transmission line protected with single-pole reclosing circuit breakers. When a flashover occurs on such a system and the circuit breaker in the faulted phase opens, capacitive coupling current will flow as in the previous case. The magnitude of the capacitance coupling voltage sustaining this current flow will be much higher in this case, however. This increase is due to (a) the field from the two phases not being as well balanced as the transposed 3-phase field from an adjacent circuit in the double-circuit triple-pole reclosing case, and (b) the closer spacing normally employed between the three conductors of one circuit as compared with the spacing between two circuits. The appendix shows calculations for determining the values of these voltages.

The test circuit for this condition is similar to the circuit used for the previous series, except for the magnitudes of the capacitances and the transformer voltages. Tests were made with capacitive coupling voltages ranging from 10 kv to 44 kv. Capacitances were varied to give the equivalent of 100 and 300 miles of 230-kv transmission lines. All tests were carried out on a standard 15-unit insulator string. The data from these tests are given in Table I. It will be seen from this table that when capacitive coupling voltages equal or exceed 22 kv, the necessary deionizing time becomes very long and in many of the tests interruption did not occur at all in the half-second or more after fault current interruption. Whether interruption would have subsequently occurred is extremely doubtful. As shown in the appendix, however, capacitive coupling voltages will be frequently encountered in this range with single-pole reclosing on 230-kv lines. Despite the gains that may be shown in transient stability under some conditions on 230-kv systems with single-pole reclosing circuit breakers, these data do not indicate that such operation will always be feasible. Similar data were not taken in this investigation using shorter insulator strings to simulate lower voltages.

Table I. Tests to Determine Duration of Capacitive Follow Current after Interruption of Main Fault Current on 230-kv Suspension Insulator

Capacitive Recovery Voltage	Voltage Supplying Main Fault Current	Fault, Amperes	Fault Duration, Cycles	Ratio C_1/C_2 Microfarads	Equivalent Miles 230-kv Line	Capacitance Follow Current,* Amperes	Duration Capacitive Follow Current Cycles
10,000	22,000	7,700	5.0	0.68/0.82	100	2.3	0.7
10,000	22,000	930	2.0	0.68/0.82	100	2.0	2.2
10,000	22,000	900	2.0	0.68/0.82	100	2.0	0.6
10,000	22,000	870	9.5	0.68/0.82	100	2.0	0.6
10,000	22,000	870	9.0	0.68/0.82	100	2.0	5.5
10,000	22,000	6,800	10.5	0.68/0.82	100	3.0	6.5
10,000	22,000	6,600	10.5	0.68/0.82	100	3.0	0.1
10,000	22,000	6,800	11	0.68/0.82	100	2.0	0.5
10,000	22,000	6,600	10.5	0.68/0.82	100	2.8	1.6
10,000	22,000	6,800	11	0.68/0.82	100	3.3	0.5
10,000	22,000	6,800	8.5	2.04/2.46	300	25	1.2
10,000	22,000	6,800	9.0	2.04/2.46	300	22	2.1
10,000	22,000	6,800	9.5	2.04/2.46	300	20	1.7
12,000	22,000	8,100	3.7	0.82/0.68	100	2.6	0.6
12,000	22,000	8,000	2.9	0.82/0.68	100	2.1	0.8
12,000	22,000	880	2.4	0.82/0.68	100	2.0	3.6
12,000	22,000	890	1.9	0.82/0.68	100	2.0	4.3
12,000	22,000	6,800	10.5	0.82/0.68	100	2.0	2.5
12,000	14,500	16,200	2.8	3.72/0.78	300	17.5	0.2
12,000	14,500	18,100	2.8	3.72/0.78	300	17.5	0.7
14,500	14,500	17,700	3.8	4.5/open	300		0.3
15,000	22,000	7,600	3.0	1.02/0.48	100	2.5	0.7
15,000	22,000	7,600	3.5	1.02/0.48	100	5.1	7.0
15,000	22,000	8,000	2.4	1.02/0.48	100	5.5	1.8
15,000	22,000	6,900	8.5	3.07/1.43	300	39	3.7
15,000	22,000	6,900	9.0	3.07/1.43	300	16	0.9
15,000	22,000	7,300	4.4	3.07/1.43	300	40	3.1
15,000	22,000	7,700	2.0	3.07/1.43	300	22	0.6
15,000	22,000	7,600	3.0	1.02/0.48	100	11	2.6
15,000	22,000	7,600	3.5	1.02/0.48	100	37	2.2
22,000	22,000	7,100	3.0	4.5/open	300	100	22
22,000	22,000	7,200	3.0	4.5/open	300	120	26
22,000	22,000	7,500	3.1	1.5/open	100	35	32†
22,000	22,000	7,200	3.0	1.5/open	100	35	18
33,000	44,000	4,200	1.5	1.13/0.376	100	50	10†
33,000	44,000	4,100	2.0	1.13/0.376	100	35	23
33,000	44,000	3,600	4.0	1.13/0.376	100	35	28†
33,000	44,000	3,700	3.5	1.13/0.376	100	105	24
33,000	44,000	3,800	3.0	3.37/1.12	300	105	31†
44,000	44,000	3,900	2.9	1.5/0	100	44	31†
44,000	44,000	3,800	4.0	1.5/0	100	44	31†

*Current magnitudes in the column may be subject to error from difficulties in reading films due to poor wave shapes and short current durations.

†Did not clear.

Conclusions

The following conclusions may be drawn from this investigation:

1. When capacitive coupling is not present, the de-energization time after insulator flashover necessary to prevent arc reignition upon reclosure of the line increases linearly with the fault current on 230-kv systems is practically constant for 132-kv systems, and decreases at high current on 69-kv systems. By adding the circuit breaker interrupting time to the data obtainable from these figures, the minimum reclosing times permissible on various systems may be obtained.

2. On systems with small stability margins, gains in the maximum power transmission without loss of synchronism during transient faults may be obtained by using circuit breakers with reclosing time much less than the present standard of 20 cycles.

3. Triple-pole reclosing on double-circuit transmission lines may be accomplished by adding one or two cycles extra de-energized time. This will still fall well under the present standard of 20 cycles in most cases.

4. Shunting resistors do not tend to increase the minimum total reclosing time on circuit breakers applied on short lines. On long lines they may increase it up to the duration of the resistor follow current.

5. The capacitive coupling associated with single-pole reclosing appears to make single-pole reclosing infeasible under many conditions on 230-kv systems unless excessive de-energization times are allowed.

Appendix I. Calculation of Voltages Electrostatically Induced on Unconnected Transmission Line Conductors

The voltage induced on one of two long parallel conductors by a charge on the second when their spacing is large in comparison with their diameter and with the effect of the ground plane taken into account is given very closely by the equation

$$e = 25{,}740\, q \log \frac{a'}{a} \qquad (1)$$

where

e = voltage induced on first conductor, in kilovolts

q = charge on second conductor, coulombs per mile

a = distance between the two conductors

a' = distance from one conductor to the reflection of the other in the ground plane

The voltage induced on a conductor by its own charge is given by the same equation with a set equal to the conductor radius.

By setting up a system of equations like equation 1 and solving simultaneously, it is possible to determine the charge on each of a system of conductors connected either to ground or to a voltage source of moderately low impedance. Then the voltage on nearby unconnected conductors may be determined as the sum of the effects of all charges on the connected conductors.

Thus for a system of two ground wires, a and b, and 3-phase conductors, 1, 2, and 3, the following system of equations would be used to determine the charges, the quantities in the fractions representing the distances between the indicated conductors or reflections

$$e_a=25{,}740\left(q_a\log\frac{aa'}{aa}+q_b\log\frac{ab^1}{ab}+\right.$$
$$\left.q_1\log\frac{a1'}{a1}+q_2\log\frac{a2'}{a2}+q_3\log\frac{a3'}{a3}\right)=0$$
$$e_b=25{,}740\left(q_a\log\frac{ba'}{ba}+q_b\log\frac{bb'}{bb}+\right.$$
$$\left.q_1\log\frac{b1'}{b1}+q_2\log\frac{b2'}{b2}+q_3\log\frac{b3'}{b3}\right)=0$$
$$e_1=25{,}740\left(q_a\log\frac{1a'}{1a}+q_b\log\frac{1b'}{1b}+\ldots\right)=E$$
$$e_2=25{,}740\left(q_a\log\frac{2a'}{2a}+\ldots\ldots\right.=E\underline{/120^\circ}$$
$$e_3=25{,}740\left(q_a\log\frac{3a'}{3a}+\ldots\ldots\right)=E\overline{\diagdown 120^\circ}\qquad(2)$$

One of the 220-kv transmission lines in this country has two circuits carried on the same towers with flat vertical spacing and the following dimensions:

Distance between conductors of the same circuit, feet	16.5
Average height of lowest conductor above ground, feet	100
Distance from center line of tower to conductors, feet	14.5
Average vertical distance from conductors to ground wires, feet	20.7
Distance from center line of towers to ground wires, feet	8.67
Effective conductor radius, inches	0.52
Effective ground wire radius, inches	0.19

Calculating interconductor distances from these values, substituting in equations 2, and solving for the charges when one circuit only is energized, the other being disconnected, gives the following values

$$\left.\begin{aligned}q_a&=-1.2-0.4j\\q_b&=-0.7-0.3j\\q_1&=14.4-1.2j\\q_2&=-7.6+13.2j\\q_3&=-6.2-13.1j\end{aligned}\right\}\times10^{-6}E\qquad(3)$$

Table II

	Induced Voltages		
	Per Unit	Kv at 220 Kv	Amperes Per Mile at 220 Kv
Top conductor	$0.035+0.022j=\lvert 0.041\rvert$	5.2	0.024
Center conductor	$-0.006+0.011j=\lvert 0.013\rvert$	1.6	0.007
Bottom conductor	$-0.037-0.019j=\lvert 0.042\rvert$	5.3	0.024
Similarly transposed	$0.028+0j=\lvert 0.028\rvert$	3.6	0.016
Reverse transposed	$0.010+0.018j=\lvert 0.021\rvert$	2.7	0.012

Table III

	Induced Voltages		
	Per Unit	Kv at 220 Kv	Amperes Per Mile at 220 Kv
Top conductor	$-0.15+0.06j=0.16$	20	0.097
Center conductor	$-0.20j+0.01j=0.20$	25	0.120
Bottom conductor	$-0.15-0.09j=0.17$	22	0.102
Transposed	$0.17+0.02j=0.17$	22	0.102

The voltages of the unconnected conductors are then as shown in Table II. The effective capacitance of one conductor under these conditions is 0.012 microfarad per mile and currents that would flow in a connection to ground also are shown.

These lines are transposed on the same towers, but in opposite rotation. Because of asymmetry with respect to the ground circuits, this would not completely balance out the induced voltage even if the transposition were otherwise perfect. The residual voltage is somewhat less than if the lines were similarly transposed.

Current measurements were made on the three phases, grounded individually, of a 58.5-mile length of one circuit of this line, the other circuit being energized, and the two circuits being transposed in sections of 18.88, 22.84, and 16.80 miles. These measurements correspond with calculated values as follows:

	Amperes		
Calculated values	0.70	0.74	0.58
Measured values	0.60	0.63	0.52

These voltages and currents tend to increase with greater conductor diameter and greater spacing between conductors in the same circuit; they tend to decrease with greater height above ground, greater separation from the ground wires and between circuits. Complete elimination of the ground wires, however, increases the voltages of the two upper phases by 70 and 60 per cent, but decreases the voltage of the bottom phase by 25 per cent. Transposed values are increased about 20 per cent.

Single-Phase Switching

For single-phase switching, the voltage induced on one unconnected phase of a single circuit when the other two phases are energized is of interest. These values have been calculated by the method indicated above, and are shown in Table III. The values are for one circuit of the line described with the other circuit de-energized, and for phase rotation 1, 2, 3. With the other circuit energized, the values will be modified somewhat by induction from the other circuit.

These values tend to increase with greater conductor diameter, more height above ground, and greater separation from the ground wire. They tend to decrease with greater phase spacing.

Reference

1. ULTRAHIGH-SPEED RECLOSING OF HIGH-VOLTAGE TRANSMISSION LINES, Philip Sporn, D. C. Prince. *Electrical Engineering (AIEE Transactions)* volume 56, January 1937, pages 81–90.

Discussion

W. M. Leeds (Westinghouse Electric Corporation, East Pittsburgh, Pa.): These test results on the deionization times of fault arcs flashing over insulator strings will undoubtedly be of assistance in establishing minimum reclosing intervals for the rapid reclosing of high-voltage circuit breakers. The conclusions drawn are of considerable interest even though some points may require more data for complete confirmation.

For instance, a superposition of the curves of Figures 6, 8, and 9 would indicate that the minimum de-energization time is approximately independent of voltage over the range from 69 kv to 230 kv for fault currents up to 8,000 amperes. While the curves obviously diverge for higher currents, the data hardly substantiates the statement that the minimum times at 132 kv are constant over a wide range of current, particularly below 8,000 amperes.

In the section Triple-Pole Reclosing by a Circuit Breaker Equipped with Shunting Resistors, it is stated in the third paragraph that reclosing tests at 8,000 amperes at a re-established voltage corresponding to 230 kv indicated only $2^1/_2$ cycles of dead time to be required. Adding 5 cycles of resistor current flow after fault interruption gives a total of $7^1/_2$ cycles which, astonishingly enough, is appreciably shorter than the 9 cycles de-energized time indicated by Figure 6 for a circuit breaker without any resistor at all.

The fifth conclusion suggests that single pole reclosing at 230 kv may not be feasible, in many instances, because of the small current maintained in the fault path due to capacitive coupling from the unfaulted phases. Since 3-pole reclosing will be required in the majority of high-voltage applications where rapid reclosing is to be applied, it would appear advantageous to design the circuit breakers for operation of all three poles by a single mechanism whenever possible to avoid the expense of maintaining three operating mechanisms instead of one.

In recent years considerable design and development work has been applied to the mechanical operation of high-speed reclosing circuit breakers for 115 kv and above. The pneumatic type of operating mechanism has proved to be more flexible than the solenoid for this purpose, and has been equipped with quick-acting air inlet valves and trip-free links for maximum operating speed. As an indication of the progress that has been made, it should be mentioned that high power fault interruptions were made at Grand Coulee Dam in March 1947 at 230 kv with successful reclosure in 12 cycles. These results were reported by Killgore and Clagett.[1]

Still more recently, by utilizing a number of design improvements including antifriction roller bearings at the most heavily loaded pivots in the pole unit linkage, all three poles of the largest 230-kv oil circuit breaker rated 10,000,000 kva have been reclosed in less than 9 cycles with a single pneumatic mechanism. Still shorter reclosing times are possible with separate mechanisms to operate each pole, although flashover deionization times appear to set a lower limit for practical application at 12 to 15 cycles.

As indicated by the test results reported in this paper, circuit breakers which are capable of functioning satisfactorily without low value resistors have an advantage for reclosing duty since the effect of the residual resistor current in delaying the flashover deionization is thus avoided. The necessity for such resistors can be eliminated by incorporating into the interrupting units of conventional circuit breaker designs spring-driven oil pistons of moderate size which will provide substantially restrike-free performance during the interruption of transmission line charging current.

There are thus available to the industry simple steel-tank high-voltage circuit breakers of the dependable downward break type with an isolating oil gap below highly effective interrupting units which provide high speed arc clearing in less than 3 cycles, actually approaching 2 cycles under normal system voltage recovery rates, over the entire range of line-charging, magnetizing or fault currents up to 10,000,000 kva or even higher. Furthermore, mechanically trip-free pneumatic operating mechanisms which require no air or external assistance during the tripping function can provide high-speed reclosing within time intervals shorter than any practical service requirement.

REFERENCE

1. FIELD TESTS FOR DEVELOPMENT OF 10,000,000-KVA 230-KV OIL CIRCUIT BREAKERS FOR GRAND COULEE POWER PLANT, C. L. Killgore, W. H. Clagett. *AIEE Transactions*, volume 67, part I, 1948, pages 271–87.

Philip Sporn (American Gas and Electric Service Corporation, New York, N. Y.): The work on ultra-rapid reclosing of high-voltage lines, initially carried out by D. C. Prince and myself more than 13 years ago, foreshadowed a good many of the results indicated here. It is most gratifying, however, not only to have all of that definitely confirmed, but to have available the results of the complete job the authors have done. Of much greater importance is that we now have the benefit of the work carried out by the authors as a further stimulus to the development of the idea of ultra-rapid reclosure as a major tool in the exploration of the maximum economy at which high tension transmission of electric power and energy can be carried out.

It is particularly gratifying to have available the technical basis for speeding up ultra-rapid reclosure to a considerably lower elapsed time interval than 20 cycles; to have a clarification of the fundamentals of, and particularly the difficulties and limitations inherent in, the use of single-pole reclosing; and to know the effect of shunting resistors on successful reclosure. With this information application of ultra-rapid reclosure to lines of 220 kv and higher voltage should not only be possible to the same degree that has been successfully applied at 132,000 volts, but the degree of successful functioning should be further increased at 132,000 volts and the higher voltages beyond present experience.

The authors are to be congratulated on this outstanding contribution toward the development of a major tool in the art of transmission of power and energy at high voltages.

Harry P. St. Clair (American Gas and Electric Service Corporation, New York, N. Y.): This paper is of particular value in throwing light on two important questions, first, the effect of resistors on circuit breaker deionization time and second, the feasibility of single-pole reclosing on long high-voltage lines.

Resistors appear to be necessary on many types of circuit breakers to enable them to handle the charging current of long lines without excessive restriking and resulting overvoltage transients. One of the important results of the authors' test is that these resistors are no help in cutting down deionizing time and reducing the reclosing cycle, since resistor current time must be added to the reclosing time. This would affect both 3-pole and single-pole reclosing.

As to relative merits of single-pole and 3-pole reclosing and the theoretical improvement in stability offered by single-pole reclosing, we are particularly indebted to the authors for a realistic analysis and tests. It seems quite conclusive that for higher voltage lines, even where distances are moderate, such as 100 to 200 miles, the mutual coupling effect may not only offset the theoretical gain from single-pole reclosing, but as the authors state, make it entirely impossible.

Finally, it also may be concluded that any proposed combination of single-pole reclosing with resistor-equipped circuit breakers would encounter especially hard going and it may well be that at least for higher voltage lines, neither can be used.

The work done by the authors as reported in this paper is a job that has been badly needed and constitutes another important milestone in the transmission art. In discussion with the authors, I was told that time did not permit carrying out the test program to the extent that would be desirable. We sincerely hope that such tests will be continued and further results made available. Also, the differences shown between 138-kv and 230-kv results indicate the desirability of exploring high-speed reclosing at voltages even higher than 230 kv.

A. C. Boisseau, B. W. Wyman and W. F. Skeats: In an investigation of this nature, the interest and encouragement for continuation from such pioneers in the high-speed reclosing field as Mr. Sporn and Mr. St. Clair are sincerely appreciated.

In this investigation of the fundamental considerations involved in high-speed triple-pole and single-pole reclosing, the authors did not confine their data to any particular type of circuit breaker. The data presented are applicable to either impulse-type low oil content circuit breakers or to tank-type circuit breakers either with or without resistors, as most of these types have been developed to the point where they will either exceed or approach the minimum practical reclosing times which can be applied on high-voltage systems. Mr. Leeds suggests, in addition, a tank-type circuit breaker equipped with auxiliary spring-driven pistons to provide "substantially" restrike-free performance during the interruption of transmission line charging current. The blast of oil furnished by such a piston is in principle similar to that used for many years in the impulse-type circuit breaker. In the experience of the authors, such a piston, if properly designed, should not only "substantially" eliminate but should completely eliminate restrikes, as no restrikes have ever been obtained with the impulse type circuit breaker. The authors feel that by putting the piston-type interrupter inside a tank-type circuit breaker, as Mr. Leeds suggests, the biggest advantage of the piston is being lost, namely, the elimination of the large tank and thousands of gallons of oil which the impulse-type circuit breaker accomplishes automatically.

The authors agree with Mr. Leeds that deionization may be expected to be substantially independent of voltage over the range from 69 to 230 kv for fault currents up to 8,000 amperes.

As stated in the paper, when shunting resistors are used, reclosure takes place in two steps: first, through the resistor, and finally, by shunting out the resistor. So far as the first step is concerned, the overshoot associated with reclosure is completely damped out with a resistance of the order of 3,000 ohms and transmission line lengths of a mile or more. For this condition, the tests indicate, as Mr. Leeds points out, that at 230 kv and 8,000 amperes, the shortening of the necessary dead time caused by elimination of the overshoot is greater than the increase caused by the flow of resistor current. Thus, considering the first step only, there is a net gain from the use of resistors. However, the second step must be considered also, and here, as indicated in Figure 11, the overshoot is dependent to a considerable extent on switching conditions and the necessary dead time may be expected to vary accordingly over a range from $2^1/_2$ cycles with favorable conditions on very short lines to nine cycles with unfavorable conditions on long lines.

Development and Application of Electric Heating to Deicing of Aircraft Propellers

JACK H. SHEETS
NONMEMBER AIEE

EDWARD J. SAND
NONMEMBER AIEE

THE use of heat for protecting aircraft propellers against ice formation is considered as one of the most positive approaches to the problem. Electric energy is a very convenient means for obtaining necessary heating on the propeller blades under adverse installation conditions. This paper discusses the development of the electric heating system and design practices being used on current commercial and military aircraft. In view of design problems with respect to power supply and application probable future trends also are discussed.

Propeller Icing Problem

Ice formation on the propeller is a potential hazard to flight safety because of the serious effects it can have on the aerodynamic, balance and structural characteristics of the propeller and the airplane. Available data are very meager with respect to a quantitative evaluation of these factors. Difficulties in applying widely varying meteorological conditions to propeller icing have been a major stumbling block to adequate definition of the problem. Flight test data taken in the past under natural icing conditions have not been reliable because of difficulties in separating performance losses caused by ice accretion on other parts of the airplane structure from the aerodynamic characteristics of the propeller under icing conditions.

The most reliable data on the reduction of propeller efficiency caused by icing are believed to be those presented by the National Advisory Committee for Aeronautics at a recent conference on aircraft ice prevention[1] where it was indicated that in a total of 42 ice encounters, approximately 37 per cent of the encounters showed a loss in propeller efficiency of 0 to 5 per cent; 50 per cent of the encounters showed a loss of 5 to 10 per cent; 5 per cent of the encounters showed a loss of 10 to 15 per cent; and 8 per cent of the encounters showed a loss of 15 to 20 per cent. These data were taken in actual flight on an airplane with very complete ice protection equipment and instrumentation in order to isolate as closely as possible the aerodynamic effects of ice accretion on the propeller and other parts of the airplane.

Nonuniform build-up or shedding of propeller ice will cause propeller unbalance which can result in objectionable propeller and engine vibration. It is doubtful that propeller unbalance would be sufficient to cause structural damage even under severe icing conditions on installations using dynamic engine mounting. On installations with rigid engine mounting, continued operation under vibratory conditions resulting from nonuniform ice build-up might cause structural damage. In either case the resulting unbalance could be sufficiently objectionable to require feathering the propeller.

The amount of ice that will build-up on the propeller blade depends upon many factors and will vary widely with the type and severity of icing conditions encountered. Pieces of ice will be thrown from the rotating propeller even when no ice prevention methods are employed. Experience has shown that these pieces of ice thrown from the rotating propeller can be large enough to cause structural damage to the fuselage, windows, lizer, and other components of th plane. Likewise the noise from th pact of ice thrown from the propel the fuselage is objectionable for mercial operations because of pass discomfort and annoyance even th the ice particles may not be of suff size to be harmful structurally.

While there is still much discussio the quantitative effects of prop icing which will not be resolved further studies are made, it is gene conceded that the best possible prop icing protection should be provided both commercial and military operat This is particularly true since impr ments in icing protection being m available for other airplane compon will make it practical to schedule op tions through more severe icing condit with adequate flight safety.

Early Methods of Ice Protection

Early methods for protecting the p peller in icing conditions included the of fluids which acted to lower the freez point at the blade surface sufficiently provide a liquid interface between blade surface and any ice layer wh forms. The adhesion of the ice to blade is lowered sufficiently under t condition to allow pieces of ice to thrown off under normal centrifu forces. Chemical paints and pastes a have been applied to the blade surface accomplish the same purpose.

With the fluid system a slinger ring mounted on the propeller hub. Fluid pumped from a supply tank in the a plane into an annular channel in t slinger ring through a stationary feed tube mounted on the engine nose. Flu is discharged along the leading edge of th blade at the shank by the centrifug forces on the fluid through suitable di

---

Paper 49-222, recommended by the AIEE A Transportation Committee and approved by th AIEE Technical Program Committee for presentat tion at the AIEE Pacific General Meeting, Sa Francisco, Calif., August 23–26, 1949. Manuscrip submitted August 15, 1947; made available fo printing June 30, 1949.

JACK H. SHEETS and EDWARD J. SAND are both wit the Curtiss-Wright Corporation, Caldwell, N. J.

charge tubes connected to the slinger ring. A series of grooves molded in a thin strip of synthetic rubber cemented to the leading edge of the blade is used usually to assist in fluid distribution over the entire length of the blade. The fluid normally used is isopropyl alcohol.

While the fluid system has given reasonably satisfactory results under light icing conditions, the trend toward larger diameter propellers with lower rotational speeds, together with general increases in operating air speeds, has made it much more difficult to obtain satisfactory fluid distribution over the blade. Increased blade angle ranges being used on controllable pitch propellers providing feathering and reverse pitch also complicate the problem of providing a satisfactory fluid discharge on the blade leading edge. On long-range multiengine aircraft, the weight of fluid which must be carried is also significant. It is very doubtful that fluid anti-icing performance would be considered acceptable under the more severe icing conditions for which icing protection is expected to be required.

The chemical agents of the anti-icing paints and pastes are used up under icing conditions to such an extent that they maintain their effectiveness for only a limited time. Operation in rain has the same effect on using up their effective period even though icing conditions are not encountered. Because of these characteristics it has been considered that the various chemical pastes and paints are not suitable for long-range operation. Satisfactory protection for shorter range operation can be maintained only by reapplication after operation in rain or icing conditions. Reapplication is quite time-consuming because of the care and drying time usually required to maintain satisfactory coverage under air and centrifugal forces. While these anti-icing pastes and paints have been shown to give reasonably satisfactory icing protection for short periods, application time requirements make their use undesirable for even short range flights on scheduled operation.

Electric Heating Development

The National Research Council of Canada had started development work on an electric anti-icing system, using a continuous power supply, as early as 1938. From 1938 through 1942, their work dealt primarily with theoretical calculations for power requirements for satisfactorily de-icing the blades, scale model tests on brass-sheathed insulated nichrome heating elements, and ground tests on an airplane on both manganin and nichrome wire elements covered with sprayed aluminum and rubber respectively. By this time, the National Research Council had done sufficient background testing to recommend flight testing as a means of checking the heat intensities they had calculated and to determine the extent of heating element coverage required. Under conditions of the tests, they had concluded that a heat intensity of 1.25 watts per square inch provided satisfactory de-icing performance.

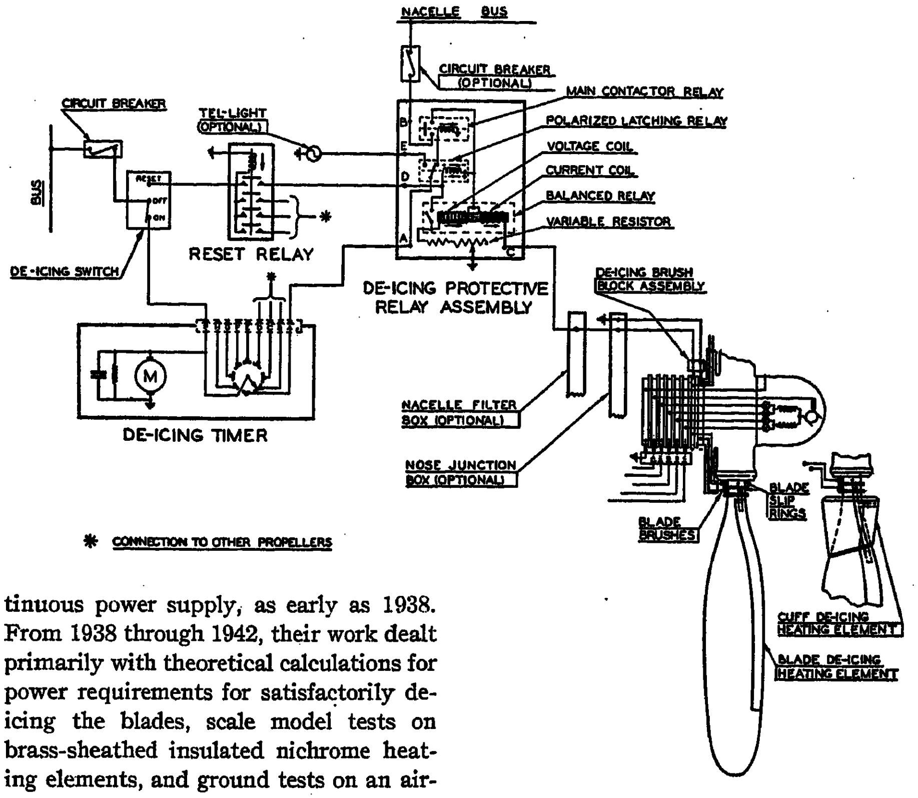

NACELLE BUS
CIRCUIT BREAKER (OPTIONAL)
MAIN CONTACTOR RELAY
POLARIZED LATCHING RELAY
VOLTAGE COIL
CURRENT COIL
BALANCED RELAY
VARIABLE RESISTOR
CIRCUIT BREAKER
TEL-LIGHT (OPTIONAL)
BUS
RESET
OFF
ON
RESET RELAY
DE-ICING SWITCH
B
E
D
A
C
DE-ICING PROTECTIVE RELAY ASSEMBLY
DE-ICING BRUSH BLOCK ASSEMBLY
M
DE-ICING TIMER
NACELLE FILTER BOX (OPTIONAL)
NOSE JUNCTION BOX (OPTIONAL)
BLADE SLIP RINGS
BLADE BRUSHES
* CONNECTION TO OTHER PROPELLERS
CUFF DE-ICING HEATING ELEMENT
BLADE DE-ICING HEATING ELEMENT

Figure 2. Propeller deicing wiring diagram

A continuous heating system using a generator mounted on the rear of the propeller hub for the power source, together with suitable blade heating elements was designed in 1940 for use by the Army Air Forces. The generator was rated at 750 watts and the design heat intensity on the elements was 2.5 watts per square inch. Flight tests conducted on these units under natural and simulated icing conditions indicated the necessity for higher heat intensities with an indication that 4 watts per square inch along the leading edge and 2.5 watts per square inch over the remainder of the heated area would be required. These tests indicated the significance of providing an adequate thermal insulating layer between the heating element and the blade to reduce conducted heat losses.

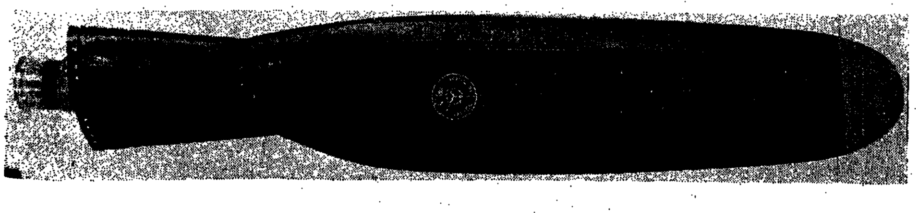


Figure 1. Heating element installed on blade and cuff

Changes in design requirements requiring use of larger diameter, lower rpm propellers resulted in hub generator designs which were not too practical even for the power requirements then being considered because of increases in size and weight necessary to obtain the required output. A cyclic-heating system was investigated by the Army Air Forces where heat is applied on the propeller blades for a relatively short period. It was found that an energization time of approximately 12 seconds would be sufficient to reduce the surface adhesion of the ice by establishing a liquid interface so that the normal centrifugal force would throw off the ice. With this cyclic system it was shown that the over-all power requirements could be reduced by successive energization of propellers or groups of blades on multiengine aircraft. This reduction in over-all power requirements made it practical to consider the possibility of using the airplane power supply for propeller de-icing even though it was found that somewhat higher heat intensities would be required than those necessary to give equivalent results with the continuous heating system. It also was

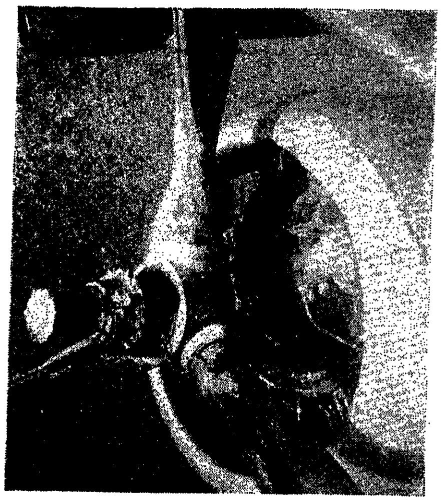


Figure 3. Simulated propeller icing—no deicing

found that the cyclic system helped to prevent run back and refreezing which was a problem with the continuous heating system.

It was concluded by the Army Air Forces in a comprehensive report published in 1944[2] that the cyclic-heating system should be adopted because of improvements in propeller ice protection, together with the advantages in weight and power saving over the continuous heating systems considered previously.

Present Design Practice

Electric deicing systems on current production propellers utilize heating elements cemented to the leading edge of the propeller blades which are connected to slip rings rotating with the propeller. Brushes mounted on the engine nose are used to transfer current to the rotating propeller through these slip rings. The normal power supply system which is the conventional 28-volt d-c system on installations being made at this time is used for propeller deicing. A timing mechanism operates remotely controlled contactors to give successive energization to the several propeller deicing circuits. A propeller deicing power connection is generally made at the nacelle bus in order to reduce wiring weight although a separate deicing bus could be used from a main load center. A typical installation and wiring diagram are shown in Figures 1 and 2.

Army Air Forces Specification 29542, which was the original basis for design of present propeller electric deicing systems, requires 5.72 watts per square inch at the leading edge and 2.86 watts per square inch over the remaining heated area for a cyclic system. Heating element coverage of 75 per cent of the exposed length with a chordwise coverage of 17 to 25 per cent is required. These values for heat intensity and coverage have been used satisfactorily under light to moderate icing conditions at temperatures close to the freezing point, but are considered to be too low for adequate protection under more severe icing conditions or icing at lower temperatures in the range from zero to 10 degrees Fahrenheit which are occasionally encountered. An average intensity of 8 or 9 watts per square inch over the heated area is being considered for new designs. Heating elements being used on propellers for *DC-6* airplanes are designed for approximately 1,100 watts per blade and those for the Boeing Stratocruiser propellers will require approximately 1,600 watts per blade. Heated length has been reduced on some installations to the 60 per cent propeller radius to permit increases in the heat intensity without having line currents exceed the capacity of available power supply and propeller current transfer systems. This reduction in heated length is expected to improve the service life of the element under abrasion conditions with a negligible reduction, if any, in deicing effectiveness at the tip.

The total cycle time between successive energization periods of a given element is limited by the amount of ice which can be allowed to build up on the blade. The amount of ice build up must be kept below that which could cause structural damage when thrown off. This condition is considered to be more severe than the aerodynamic effects of propeller icing. Another requirement for airline operation, which is in turn more limiting than that of no structural damage, is that ice thrown from the propeller blades should not cause noise inside the cabin from strikes against the fuselage which would be objectionable from the passenger standpoint. Some investigations have indicated that total cycle times between successive energization can be 200 seconds or more, but it is felt that total cycle times in the order of 100 to 150 seconds will be the highest which will be generally acceptable under cabin noise limitations. Available data indicate that an energization time of 12 to 15 seconds can be used allowing a ratio of 8 to 1 between total cycle time and energization time of a single element. The 8 to 1 energization time ratio permits use of up to eight heating elements circuits without having the line current exceed that required for a single circuit. This is an important factor in determining power requirements. The cyclic energization of elements also is significant in reducing the temperature rise in wiring and current transfer brushes. Energization times between 20 and 30 seconds with a 4 to 1 energization ratio are being used on some installations, but it is felt that a total cycle time of 120 seconds with an 8 to 1 ratio giving 15 seconds energization to individual elements will be found entirely adequate.

Two types of heating element constructions are in general use. Both of these types are molded from synthetic rubber compounds, usually neoprene. A laminated construction is used with an external layer of material approximately 0.020-inch thick for abrasion protection and a layer of material approximately 0.030-inch thick on the side of the element that is bonded to the blade for thermal and electrical insulation. One of these types has a grid of resistance wires embedded between the cover layers, and the other an intermediate layer of conductive rubber. With either construction suitable lead terminations and internal connections are incorporated as a part of the molding process. Conductive rubber elements were used in early hub generator systems operating at approximately 50 volts. The resistance wire elements appeared to be more suitable for use with 28-volt systems, although earlier difficulties with conductive rubber elements having resistance values suitable for this system voltage have been resolved, and both types are currently being furnished in production. It is not felt that sufficient service experience has been accumulated to date to indicate a superiority for either type of element.

Current transfer to the rotating propeller imposes definite design problems because of space limitations and high vibration levels in addition to brush difficulties at altitude which require special measures to obtain satisfactory results. Experience gained in the design of brushes

Figure 4. Typical electric deicing under simulated icing conditions

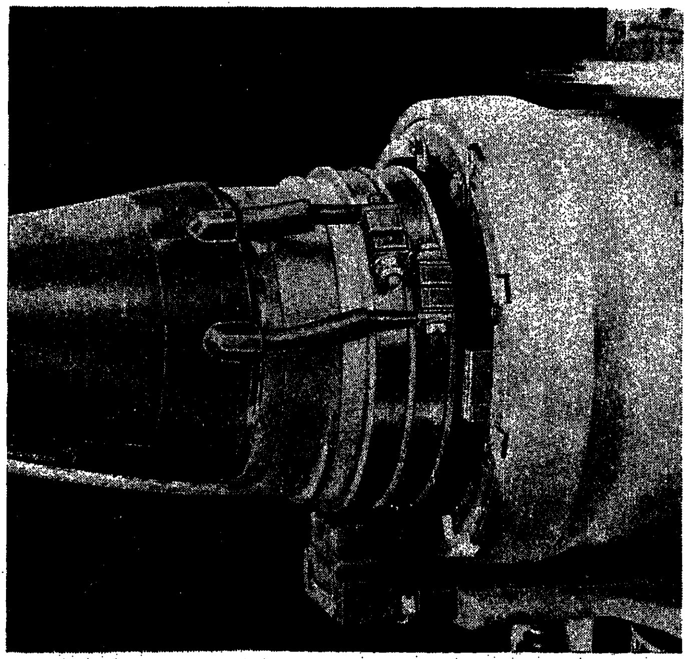
and slip rings used in the pitch changing circuits of conventional propellers using an electric motor for pitch changing power has been helpful in solving this problem, although the higher current required for electric deicing accentuates brush voltage drop and heating problems. High altitude treated carbon brushes with high copper content are currently being operated with current densities of 500 amperes per square inch with spring pressures of approximately 8 pounds per square inch on bronze slip rings at a surface speed of 2,000 feet per minute. It is believed that a current density of 750 amperes per square inch will be a limiting value for these conditions. It must be noted that these values are for cyclic energization and probably would not be satisfactory for continuous duty because of excessive heating. Relative motion between the blades and the hub structure also complicates current transfer, but this is not so great a design problem because the movement is limited to approximately 120 degrees at a relatively slow rate. An additional set of brushes and slip rings has been considered desirable at this point, although propellers are being operated with a pig-tail connection between the hub and the blade element with careful attention to the design details to eliminate service difficulties resulting from centrifugal forces, vibration, and handling damage. Figures 5 and 6 show current transfer brushes and slip rings being used on production propellers.

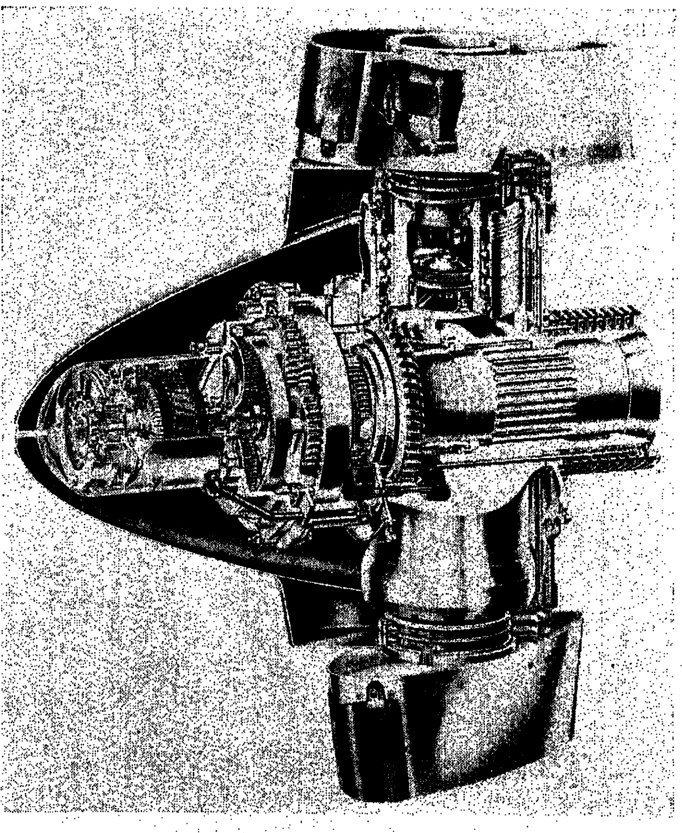
Electric deicing circuits require under-current protection in addition to the over-current protection normally used in aircraft circuits to prevent wiring and equipment damage and to isolate faults from the bus. Under-current conditions can result from open circuits or internal short circuits within the system. This condition is undesirable when it is such that low current is drawn by one blade element because of the propeller unbalance which could result. It is also desirable to receive an indication of marginal deicing protection through actuation of the under-current protective device should blade elements or other parts of the circuit become damaged prior to normal inspection periods. It has been found possible to incorporate this under-current and over-current protection as a part of the nacelle contactor so that the contactor circuit will remain open in the event of line currents above or below the normal operating range. Since a device with the proper sensitivity for this application is difficult to build with sufficient capacity to handle high fault currents which could occur with a solid short circuit to ground, it is customary to provide circuit breakers or limitors for this type of protection.

It has been difficult to establish design specifications for power requirements of blade heating elements because of the problem in defining adequately the limiting meteorological conditions under which operation is expected with propeller deicing equipment. Airplane operation under more severe icing conditions has been made possible by improvements in anti-icing protection for other parts of the airplane. Available data have indicated that earlier estimates as to power requirements for propeller electric deicing systems are lower than desirable to provide complete propeller deicing under some of the more extreme conditions. Since some propeller deicing systems currently in use have been designed with definite restrictions on the amount of power available, it is considered essential that definite attention be paid to the performance of these existing systems in actual operation in order that the extent of compromise, if any, with theoretical performance requirements can be evaluated. It is believed possible that this service experience will indicate that total propeller deicing power requirements in the order of 3,500 watts will be adequate since it is felt that performance with this power will insure safe operation under the most severe icing conditions for which protection is provided on other parts of the airplane even though theoretical performance may not be satisfied in a small percentage of the cases. If this is found possible, a practical compromise will have been achieved without requiring the power increases to approximately 4,500 watts indicated by some recent data. It is recommended, however, that the value of 4,500 watts for propeller deicing power be used as a basis for new designs with three blade propellers up to 13 feet basic diameter and four blade propellers up to 15

Figure 5. (below) Blade deicing brushes and slip rings—protective cover removed

Figure 6. (right) Propeller section showing hub slip rings





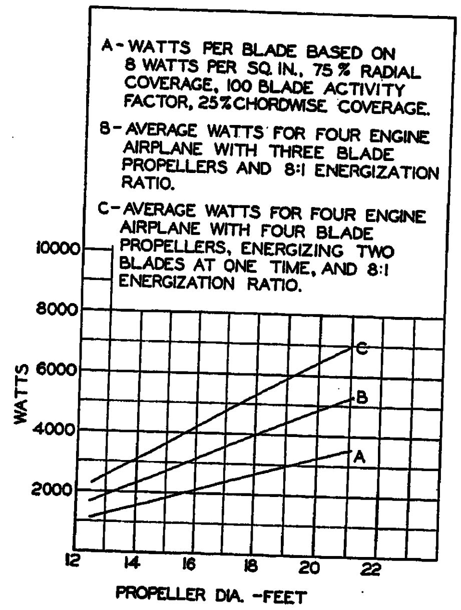


Figure 7. Recommended maximum power requirements for propellers with external heating elements

feet basic diameter until actual operating requirements are better established.

Probable Future Trends

Heating elements mounted on the blade leading edges are susceptible to damage from stones picked up on runways as well as wear from water and sand abrasion. Changes in materials and methods of fabrication for external elements will probably be desirable to improve service life. On propeller blades having a hollow construction, the use of heating elements on the inside of the blade has been considered. Internal heating elements will eliminate maintenance requirements resulting from this type of damage on external elements. An internal heating element has the disadvantage of requiring additional power input and an increase in the cyclic energization time as a result of the thermal lag of the increased material thickness between the heating element and the layer of ice. In addition to the power increase required per blade, this will have the effect of doubling airplane power requirements on installations where energization of two deicing circuits is required simultaneously in view of limits on the allowable icing build-up between successive energizations. Power requirements for internal elements are not as well established as those for external elements and more work will be required to permit a better definition of these requirements.

Available data indicate that an average of 8 watts per square inch should be used on new designs until the service experience discussed previously has been obtained. This will require 2,300 watts per blade on a 16 foot 6-inch diameter blade having heating element coverage to 75 per cent radius and a cordwise coverage of 25 per cent. On a 4-engine airplane with four blade propellers this will require 4,600 watts from the airplane power system with two blades being energized at a single time at an 8 to 1 energization ratio. A more general indication of these power requirements is given in Figure 7.

It can be seen that line currents in the order of 200 amperes are required with the normal 28-volt d-c power system to provide propeller deicing if heating intensities and coverage of the type described are used. A line current of 250 amperes has been considered as a practical upper limit to obtain acceptable service with brushes and slip rings of a reasonable size. The desirability of using a higher voltage supply for the propeller deicing system is readily apparent. Supply voltages in the order of 110 volts alternating or direct current would be very desirable for this application. These higher voltages would permit an appreciable reduction in wire weight and would make the system less sensitive to variations in element resistance through reduction in the percentage of the system voltage lost through wiring and brush-slip ring drops.

The electric deicing system for propellers has found favor because of its flexibility and the ease with which it could be adapted to existing propeller designs. Another system for deicing propellers using blades of hollow construction by means of heated air passed through the inside of the blade is being developed. This system in its present form requires an appreciably greater power input to the propeller than with the electric system, although the use of exhaust heat exchangers or relatively simple combustion heaters make this power much more readily available than that required from the airplane electric system. It is believed that the heated air propeller anti-icing system will be improved through further development to a point where the deicing performance will be as good as or better than that provided by the electric system with lower installation weight and reduced maintenance requirements. Until this development is completed electric deicing systems for propellers will continue to play an important part in insuring the safety of all-weather operation of aircraft.

References

1. Effect of Ice Formations on Airplane Performance, C. Merritt Preston, W. V. Gough, Jr. National Advisory Committee for Aeronautics Conference on Aircraft Ice Prevention, June 26–27, 1947 (To be published).

2. Propeller Electrical Anti-Icing Systems. Army Air Forces (Washington, D. C.) *Memorandum Report Number TSELA-3C-581-144-6*, September 25, 1944.

No Discussion

Lightweight Aircraft Transformers

D. S. STEPHENS
ASSOCIATE AIEE

Synopsis: The purpose of this paper is to discuss characteristics and construction of lightweight aircraft transformers. It is intended primarily to be of interest to those members of industry who design and service electric systems of aircraft. Topics covered are construction, materials, standard ratings, window heating transformers, operating characteristics, and estimated life.

THE higher voltages of the a-c system are required to handle economically the ever-expanding electrical loads of large commercial and military airplanes. Modern aircraft require approximately 1 kw of electric power for each ton of gross weight and with such electrical loading increasing applications are found for transformers. Transformers are required for applications such as lighting, instruments, control, rectification, and window deicing. The development of a laminated glass which can be heated by the passage of current through a conductive layer[1] has increased the demand for larger aircraft transformers capable of supplying loads of 1,000 to 8,000 watts. Transformers are relatively heavy electric apparatus and the weight problem requires consideration from both the aircraft and transformer industry. Weight reduction is a permanent contribution to the increased range or pay load of the aircraft. As the lower nominal frequency of aircraft generators is fixed at 400 cycles per second, transformer size and weight are fundamentally determined by the quality of the magnetic circuit and temperature limits of the insulation. It is assumed that copper is the most practical current conductor and that established limits on regulation and efficiency must be met. Therefore, increased core and copper losses are the prices to be paid for size reduction. This requires magnetic materials of high flux carrying capacity, heat stable insulation, and abnormally high operating temperatures in proper combination to result in minimum size transformers.

General Features

United States Air Force Specification *32450* provides a general pattern of performance and construction although only 12 ratings are covered in the tabulated section of the specification. The construction of a medium size auto-connected transformer is shown in Figure 1. The design is proportioned to take advantage of the 200 degree centigrade rise permitted on aircraft transformers. The construction features an exposed coil and shell-type magnetic circuit. The terminal studs are located near the top of the unit and close to the coil to give a minimum over-all size. Locating terminals close to the coil is, in itself, a manufacturing problem as there is always a minimum length required for a terminal connection. In ratings below 350 volt-amperes the space saving of the 2-loop core design becomes unappreciable and the single core-single coil arrangement is used. Over a wide variation in the ratio of copper loss to iron loss the inside of the winding has the highest temperature rise of any part of the transformer. The operating temperatures are gradually lower for the coil surface, the core surface, and the frame surface in the order named. It is unlikely that there are appreciable temperature gradients within the core as it is comprised of good thermally conductive material and each lamination reaches two core surfaces. The coil has a small surface exposed and dissipates heat loss by radiation and convection and by conduction to the core.

Core Frames

The frame which holds the type *C* Hipersil cores is typified by the vertical side pieces and base being an integral piece of sheet steel for rigidity. The cores are cradled in horizontal and vertical channels welded to the inside of the inside of the U-shaped steel part. Refer to Figure 1 for this type of construction. Variation of this construction is found on small single-phase transformers and 3-phase transformers. The arrangement gives a low center of gravity with respect to the mounting plane. The cores on the larger transformers, those weighing approximately three pounds and over, are positively held by means of a screw through the center of the top and bottom channel. The screws are tightened to bring pressure to bear on the core. On small units the cores are held by packing tightly into a rigid top and bottom channel.

Fishpaper channels are placed between the edge of the core and the metal channels to prevent short circuiting of core laminations. It would appear that fishpaper, being a class *A* insulating material, is unsatisfactory for such an application. However, the temperature of the core is much lower than that of the coil upon which allowable temperature is based, and fishpaper stands up well under this service. The paper receives a coating of silicone varnish at transformer assembly which imparts heat resistant qualities. It has been found that mica tends to disintegrate when placed between two metal parts that undergo varying compressive forces. Such would be the condition in an aircraft transformer in actual service.

After fabrication the frame parts receive a cadmium plate for protection against corrosion. The frames are given

---

Paper 49-223, recommended by the AIEE Air Transportation Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Pacific General Meeting, San Francisco, Calif., August 23–26, 1949. Manuscript submitted May 23, 1949; made available for printing July 1, 1949.

D. S. STEPHENS is Design Engineer in the Specialty Transformer Engineering Department, Westinghouse Electric Corporation, Sharon, Pa.

a heat resistant black enamel finish which improves the appearance of the completed unit and increases the radiation constant. The high temperature enamel has a silicone base and was developed originally for finish of space heaters and similar equipment.

Cores—Magnetic Circuit

The cores are of type *C* construction which consists of a continuous strip of lamination, wound to size on a mandrel, annealed, bonded solidly, and cut at the center.[2] The resulting air gap is of the order of 0.002 inch total, and in combination with the high quality steel gives an unusually small exciting current in a 400-cycle transformer. There is no overlap of laminations in a type *C* core which results in saving of space and weight, again an important contribution to light-weight aircraft transformers.

The core material is 0.005-inch thick Hipersil.[2] Hipersil is silicon steel of high permeability wherein the grain orientation is controlled during processing. The elementary pattern of crystals in the steel is arranged so that their axes of easiest magnetization are nearly parallel and aligned in the direction of rolling. As the 400-cycle flux travels in the direction of rolling, very high densities are possible. A common core density for single-phase cores is 15,000 gauss which still permits an over-voltage of 10 per cent without exciting current difficulties. The apparent watts loss (product of exciting current and applied voltage) per pound of core is 10.0 at 15,000 gauss while *TW* (true watts iron loss) is 8.0 at 15,000 gauss. Thus it can be recognized that 5-mil Hipersil is an important contribution to lightweight aircraft transformers.

The top and bottom *C*-shaped sections of the core are held together by steel bands. The steel bands are in definite tension and the ends of the bands are locked together in a crimped seal. A soldered seal is used to hold the bands on small cores when the temperature rise is insufficient to loosen high temperature solder. A film of thermosetting joint compound is placed on the core faces at core assembly and immediately baked. The joint compound is effective in reducing magnetostriction noise.

Coil Materials

The coil insulating materials are chosen to withstand an average temperature rise of 200 degrees centigrade at 25 degrees centigrade ambient. A flexible mica wrapper lines the inside of the coil. Glass-insulated copper wire or strap is used as a current conductor. Silicone impregnated fiber glass serves as layer insulation, end turn sleeving, and coil side wrapper.[3] Glass tape is used where applicable. As the maximum voltage of most designs is below 500 volts, relatively little insulation is required between core and coil. Common test voltages to ground are 1,500 and 2,500 volts. With a minimum of insulation a very solid coil results. A solid coil has two advantages; first it can be wedged solidly on the core and secondly good heat conduction from within the interior results. An ordinary solder joint cannot be used for attaching leads in the coil because of the operating temperature. Leads are incandescent brazed to wire of coil. If the coil wire is very small brazing is difficult to perform without embrittling the copper and in those cases, a high temperature solder joint is made. Stranded copper leads are used to prevent lead breakage arising from vibration. The leads are insulated with a silicone impregnated glass sleeving.

Designs have been made wherein vertical coil ventilating ducts were formed between layers of the coil during winding, as shown in Figure 2. The ducts are effective in permitting high current densities in the copper but lose their advantage with low air densities. Disregarding high altitude performance, a transformer with coil ducts can be made lighter than a transformer without ducts but the decrease in weight tends to be offset by the increase in the long dimension of the coil. Some objection is made to ventilated coils from an appearance and sturdiness viewpoint. A coil with ducts is quite difficult to wind and has multiple openings for penetration of moisture and salt laden air. Experience indicates that the solid coil is more practical.

Core—Coil Assembly

Stiff mica plate is used to wedge the coil on the core and to space the coil sides centrally in the core window. The coil is wedged securely in order to withstand vibration. The core-coil assembly is vacuum impregnated with silicone varnish. A feature of this varnish is that it neither becomes fluid nor brittle over a wide range of temperature. It is "tacky" and penetrates the coil crevices and aids in holding the insulating materials together. Also the conduction of heat from within the coil is improved. Thus, because of their insulating qualities and ability to withstand higher temperatures, mica, glass, and the silicones definitely contribute to lightweight aircraft transformers.

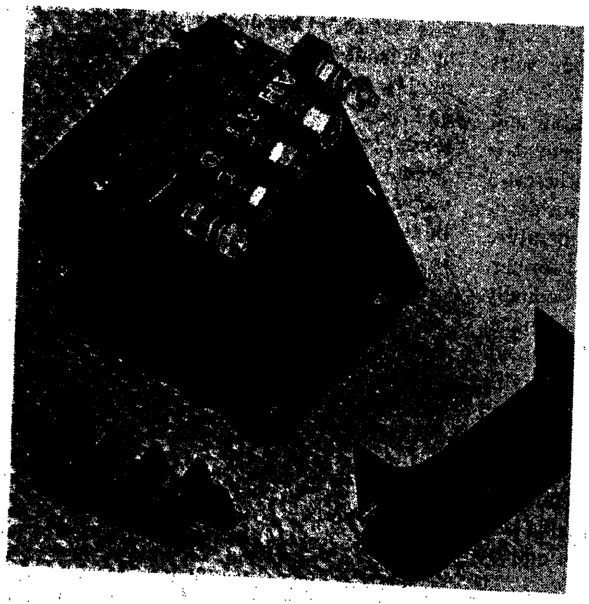


Figure 1 (left). Aircraft transformer of 0.75 kva, voltage ratio 115 to 28, autoconnected

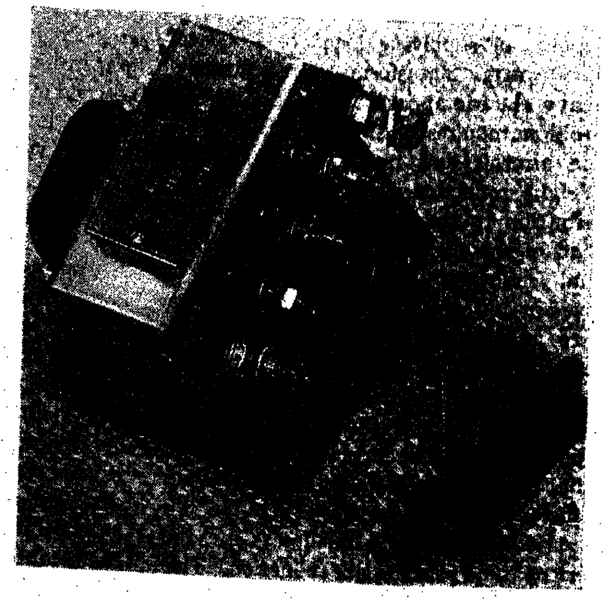


Figure 2 (right). 1.3-kva deicing transformer with ventilating ducts in coil

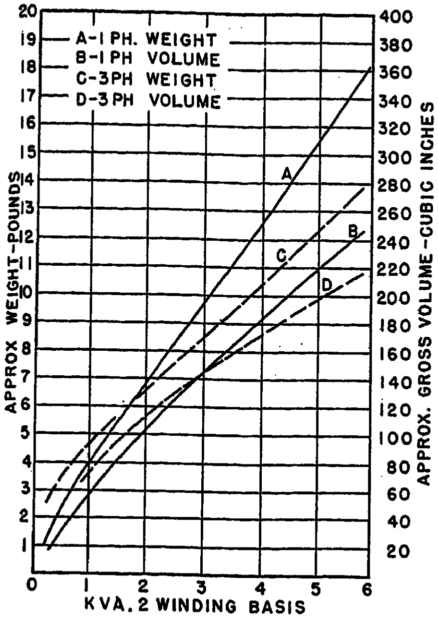


Figure 3. Weight and volume of aircraft transformers, 3-phase and single-phase

Standard Aircraft Transformers

The standard single-phase ratings in United States Air Force Specification *32450* cover a range from 0.005 to 5.0 kva and are auto-connected for an open circuit ratio of 115 to 28 volts. Table I contains the electrical characteristics of standard designs. Figure 3 illustrates weight and volume of high temperature aircraft transformers. In referring auto-connected transformers to this curve the relation shown in the following equations must be considered.

$$\text{Equivalent 2-winding kva} = \frac{E_1 - E_2}{E_1} \times \text{load kva}$$

where

E_1 = high-voltage line voltage
E_2 = low-voltage line voltage

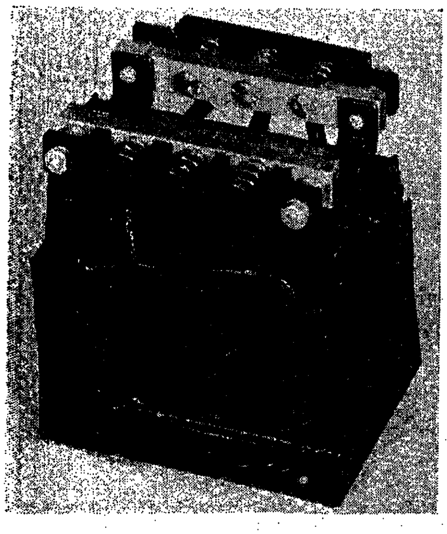


Figure 4. Three-phase aircraft transformer, 0.25 kva, 208 volts wye to 120 volts delta

The volume is maximum space occupied that is, the product of the maximum dimensions along the three axes. Spot checks have shown that the net volume is approximately 45 per cent of the gross volume. A line of standard ratings is very useful in estimating the size of special or new designs. In using Figure 3 consideration must be given to factors other than equivalent kilovolt-amperes which affect the size of a transformer. These factors are ratio, number and position of terminals, current capacity of terminals, terminal protective covers, test voltages, coil guards, and mounting arrangement.

Three-Phase Transformers

Weights and volumes are contained in Figure 3 and typical construction is illustrated by Figure 4. Three-phase 2-winding designs are used for supplying 3-phase loads and approximately balanced single-phase loads from 208-volt generators. A few applications exist for 3-phase deicing transformers which are either auto-connected or extended delta connected. Three-phase transformers use the same materials and construction as single-phase transformers. Three-phase type *C* cores are formed by placing two core loops side by side on a face plate and winding around them a build-up of lamination equal to the build-up of the individual core loops, as shown by Figure 5. This method permits available single-phase core mandrels to be used and results in a wide choice of size. Three-phase cores have higher core loss and exciting current than single-phase cores operated at the same induction because a portion of the flux must travel across rather than along the laminations.

Window Heating Transformers

Standard load and voltage ratings are impractical on deicing transformers. The heat requirements to prevent formation of ice on windows is based on a certain watts per square inch of window surface.[1] The current and voltage requirements can be found by Ohms Law wherein the resistance is a function of unit area resistivity of conductive film and proportions. The window shape and number of windows vary with each type of airplane and a special transformer is necessary. Ratings of single-phase deicing transformers range from 1,000 to 8,000 watts output. It is apparent that such ratings will be quite heavy even at 400 cycles and high temperature limits, and the auto-connection is used for minimum weight. Figure 6 shows a high production window deicing transformer. Load voltages vary from 250 to 500 volts and dual simultaneous loads are common. Plus or minus taps are usually required on the nominal load voltage to adjust for variations in glass resistance. Primary taps are used to reduce the load voltage during a warm-up period to prevent cracking of glass due to heat shock.

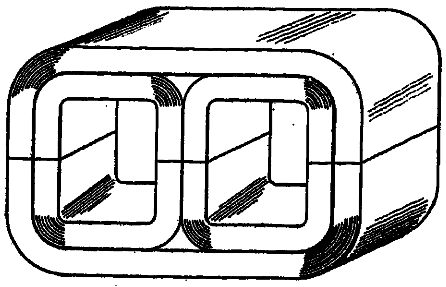


Figure 5. Sketch of 3-phase type *C* core construction

The aircraft industry specifies 1 per cent to 2 per cent tolerances on load voltages. By comparison, 2 per cent tolerance is customary on filament transformers for radio application. This tolerance becomes a difficult design problem whenever 4- to 16-load voltages are specified. The volts per turn on large deicing transformers will be approximately 3 to 4 volts and adjusting one load voltage by a change of turns may place another load

Table I. Electrical Characteristics of Standard Aircraft Transformers

Army Air Force Specification *32450* Reference Item Number	Load, Kva	No-Load Current as Per Cent of Full-Load Current	Voltage Regulation, Per Cent	Efficiency, Per Cent
1*	0.30	45	5.0	90
2	0.005	60	7.0	80
3	0.05	25	6.0	90
4	0.100	20	6.0	88
5	0.200	10	7.0	90
6	0.50	10	5.0	94
7	0.75	15	6.5	92
8	1.0	13	6.5	92
9	1.5	10	6.0	92
10	2.0	8	5.5	95
11	3.0	6	4.5	95
12	5.0	3.5	5.3	96

* 3-Phase.

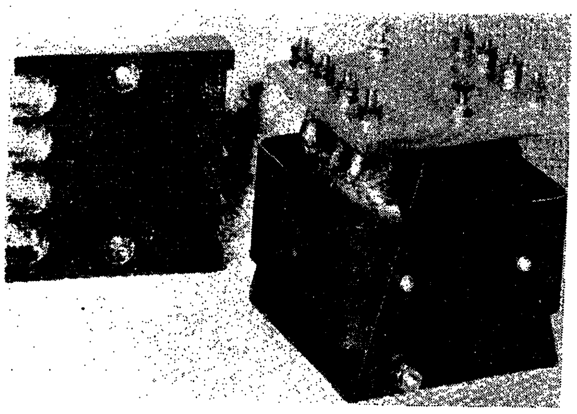


Figure 6 (left). Single-phase de-icing transformer of 3.5-kva load. Voltage ratio 115 to 282 maximum

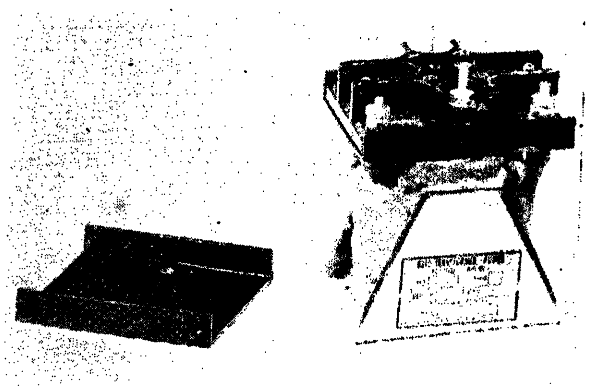


Figure 7 (right). Aircraft transformer with Fosterite treatment

voltage out of tolerance. In general, the more voltages required and the larger the transformer, the more difficult becomes the fixing of the turns ratio. It would be helpful if the aircraft industry would permit 3 to 4 per cent tolerances where more than 4-load voltages are specified.

Insulation of Small Aircraft Transformers

The problem arises as to the best method of insulating and processing the small aircraft transformers which have operating temperatures not requiring high temperature insulation. In order to reduce size it is desirable to eliminate a case and use an open-type core-coil assembly. A resin developed during the last war, is ideal for impregnating and coating open-type core-coil assemblies which have an ultimate temperature rise below 120 degrees centigrade. The resin (named Fosterite after the inventor, Newton C. Foster) changes by heat polymerization from a liquid to a solid state with only a slight change in volume. It has very low viscosity, in the liquid state, requires no solvent, and with care, can be made to produce complete filling of the coil. A resin of higher viscosity can be prepared to form a protective coating over the parts. Fosterite serves a 2-fold purpose by protecting the windings against moisture penetration and by improving the transfer of heat from within the coil. As the units utilizing this resin would normally have small wire sizes it is all the more important that they be protected against moisture penetration and fungus attack. The following example illustrates how Fosterite improves thermal conductivity. A 450 volt-ampere transformer had a 156 degree centigrade rise with high temperature insulation and silicone varnish impregnation. The same design when Fosterite impregnated (see Figure 7) had a temperature rise of 117 degrees centigrade, a 25 per cent decrease. This illustrates that use of Fosterite on certain designs will permit a change to less expensive insulation and give better moisture resistance.

Frequency and Regulation

The present aircraft a-c nominal frequency is 400 cycles ± 10 per cent. The minimum frequency is 380 cycles which determines the size because maximum induction in the core and maximum core losses occur at minimum frequency for constant applied voltage. Operating frequencies higher than 440 cycles are limited by the effect of transformer reactance on voltage regulation as reactance is proportional to frequency. The ratio of per cent reactance to per cent resistance increases with size for similar transformers. Designs 2 kva and above have acceptable regulation up to 800 cycles while smaller ratings have acceptable regulation to 1,600 cycles.

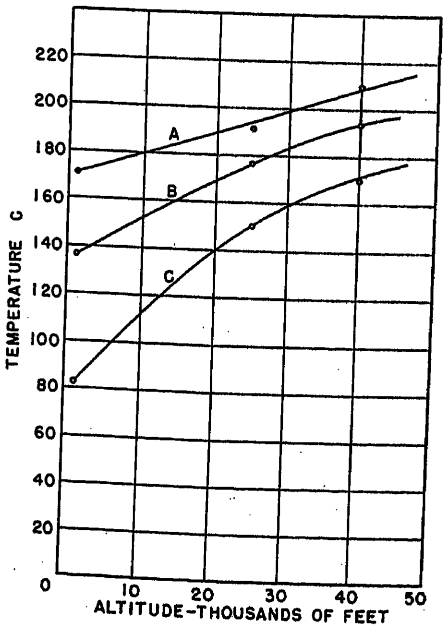
Military specifications require the voltage regulation to be not more than 8.0 per cent for transformers of less than 1-kva rating, and not more than 6 per cent for transformers of 1 kva or larger at 0.8 power factor at normal operating temperatures in a 25 degree centigrade ambient. The regulation of 200 volt-ampere transformers and smaller is predominantly caused by the resistive drop in the windings and is proportional to copper loss. Hence, their size is fixed by the permissible regulation and not necessarily by temperature rise. For example it is possible for the temperature rise of a small transformer to be only 50 degrees centigrade and just meet the regulation limit. For such a transformer class *B* insulation would be unnecessary. By contrast the size of a 1.5-kva transformer and larger ratings is dependent on temperature rise while their regulation is low and efficiency high in comparison to normally specified values. The size of ratings between 200 volt-amperes and 1.0 kva is approximately equally dependent on regulation and temperature rise.

Altitude Performance

It should be noted how the heat loss in a transformer is affected by altitude. The heat transmitted by conduction is a function of the mounting surface contact area, temperature difference between frame and surface, and the thermal conductivity of adjoining materials. Radiation loss is proportional to the fourth power of the absolute temperature and the surface area of the unit. Cooling by convection is a function of the air density and hence decreases rapidly with altitude. As convection decreases with altitude the temperature rise is such as to offset the decrease in cooling by convection with increased conduction and radiation.

Figure 8. Variation of transformer temperature with altitude

Curve A—coil temperature by resistance
Curve B—coil surface
Curve C—core surface



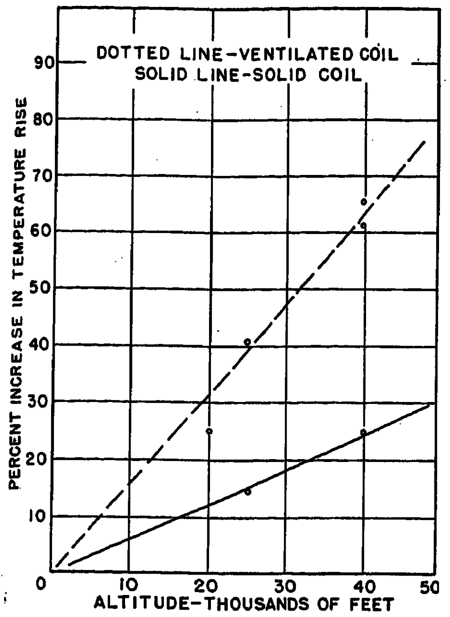


Figure 9. Comparison of solid coil and ventilated coil temperature increase with increased altitude

Load tests have been made at simulated altitudes. Figure 8 indicates the temperature rise on a 3.5-kva autotransformer. The average coil temperature rise and coil surface rise have approximately straight line variation with altitude. However the core surface temperature rise flattens out with higher elevation. As the frame surface closely encloses the core and is large compared to the coil surface it can radiate more heat than the coil. As the temperature of the transformer increases, due to loss of convection cooling, more watts are dissipated by radiation and the core tends to have less rise than the coil.

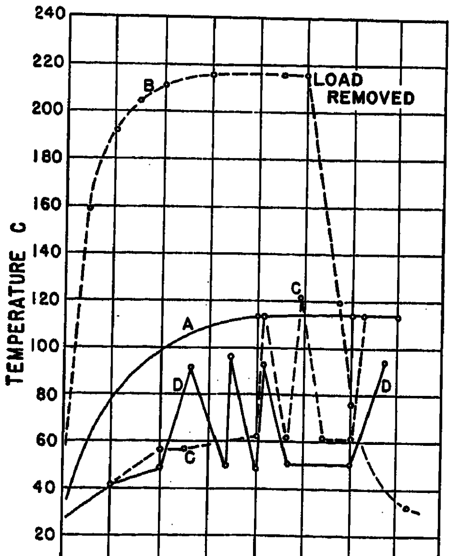


Figure 10. Transformer coil temperature (by thermocouple) at normal load and overloads

Curve A—normal load
Curve B—150 per cent load continuous
Curve C—150 per cent load, 5 minutes on—15 minutes off
Curve D—250 per cent load, 1 minute on—19 minutes off

Figure 9 compares the temperature rise of a transformer of ventilated coil construction with one of solid-coil construction. The ventilated coil design had a 65 per cent increase in temperature rise at 40,000 feet altitude over the sea level temperature rise. The solid coil design has only a 25 per cent increase in temperature rise at 40,000 feet. Ventilated coils can dissipate more watts, at near sea level conditions, than a solid coil of the same cross section but become ineffective with altitude.

United States Air Force Specification *32450* recognizes the effect of altitude by permitting a temperature rise of 225 degrees centigrade at zero degree ambient at 50,000 feet pressure altitude conditions. The specification also stipulates that the temperature rise shall not exceed 200 degrees centigrade at sea level conditions. Hence, Specification *32450* assumes that the transformer has a 112.5 per cent increase in rise in going from sea level to 50,000 feet altitude. Tests, as stated in the preceding paragraph, have shown the increase to be more of the order of 130 per cent. The altitude condition then governs the design by fixing the sea level rise at 225 degrees centigrade divided by 1.3 which equals 170 degrees centigrade. Thus the transformer must be designed for a 170 degree centigrade rise at near sea level conditions and not 200 degrees centigrade as at first might be assumed.

Tests shown in Figures 8 and 9 were made with the transformer mounted on wood in accord with United States Air Force Specification *32450*. Such mounting prevents heat transfer by conduction. In actual use the transformer would be bolted to metal which would promote good heat transfer. If the transformer was bolted to a metal surface that became very cold with altitude, a lower transformer temperature would result. However it is unwise to assume that the temperature of ambient air and adjoining parts will decrease with altitude and offset the effect of decreased air density.

Overload Ratings

The heat storage capacity of a transformer permits intermittent overloads.[4] It must be recognized that the increase in voltage regulation will be proportional to the amount of overload. An overload of 50 per cent can be delivered for five minutes if followed by an off period of 15 minutes in a cyclical manner for a long period of time. Similarly a 250 per cent load can be delivered on a 1-minute-on and 19-minute-off cycle. If the overload temperature exceeds normal temperatures only for a short period of time no measurable damage to the insulation results. Figure 10 shows coil temperatures at various loadings. At 150 per cent load the highest recorded temperatures exceeds the normal load temperature by less than 10 degrees centigrade. The 250 per cent load cycle results in still lower maximum temperature. The transformer of Figure 10 weighed $1^1/_2$ pounds. A heavier unit would have lower temperature peaks during overload because it would have more capacity to store heat and the temperature would rise more slowly. The overload capacity of aircraft transformers increases their dependability and permits the load of one transformer to be shifted to another loaded unit under emergency conditions.

Estimated Life

Temperature is a more important factor in the life of a transformer than in many other types of electric apparatus. Since a transformer is a static device, not subject to wear or mechanical failure, its life depends on the life of its insulation.

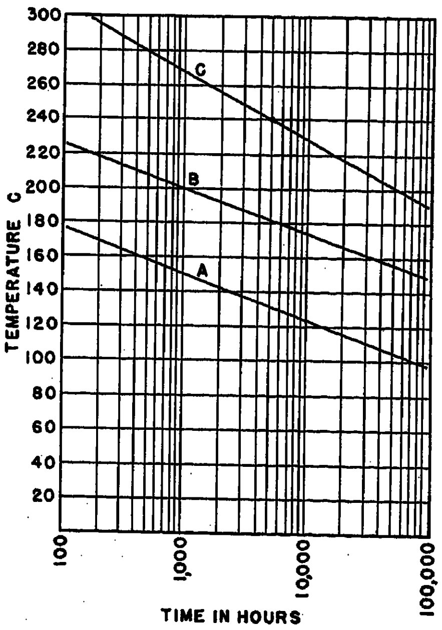


Figure 11. Estimated life

Curve A—class *A* insulation
Curve B—class *B* insulation, varnish treatment, 10 per cent duty cycle
Curve C—silicone insulation and silicone impregnation

As insulation deterioration is a function of temperature, the importance of using operating temperature to predict transformer life becomes evident. Very little data is available which is based on actual life tests on aircraft transformers and a comparison method must be used to estimate life. The transformer industry has accumulated volumes of data on life of class *A* insulation (organic material such as paper and cloth) which can be accepted as valid because it has stood the test of time. The life of class *A* insulation is shown by curve A of Figure 11. It should be noted that life is halved by an 8 degree centigrade increase in temperature. Curve B illustrates the life of class *B* insulation (inorganic materials such as mica and glass) with common varnish impregnation. Curve B is based on a 10 per cent duty cycle but meager data exist for its proof. Curve C* of Figure 11 gives the estimated life of silicone insulation (includes mica) impregnated with silicone varnish. Curve C is based on tests made on electric motors[5] operating continuously, and is believed to be sufficiently accurate in predicting the life of aircraft transformer insulation because of the similarity of motor and transformer insulation. This curve has a slope such that a 12 degree centigrade increase in operating temperature halves the life. After giving due consideration to variations in manufacture it is reasonable to expect a continuous duty life of 2,000 to 5,000 hours for a silicone insulated aircraft transformer operating at 225 degrees centigrade.

Conclusion

Thin gauge Hipersil, type *C* cores, class *B* insulation, silicone materials, and high operating temperature reduce the volume and weight of aircraft transformers to a minimum value. The same materials and methods also can be used effectively to reduce the size of electronic transformers, phase-changers, instrument transformers, and inductive reactors. Further weight reduction must result from improvements in permeability of electrical steel, better heat resistant insulation, and a correspondingly higher operating temperature.

---

* Curve C courtesy of *Westinghouse Engineer*.

References

1. Control and Power Systems for Aircraft Window Deicing, C. L. Mershon. *AIEE Transactions*, volume 66, 1947, pages 1400–06.

2. Rolled Steel Cores for Radio Transformers. *Electronics* (New York, N. Y.), volume 16, June 1943, page 110.

3. New Silicone Resins Boost Insulation Temperature Limits, G. L. Moses. *Westinghouse Engineer* (East Pittsburgh, Pa.), volume 4, September 1944, pages 138–41.

4. Electronic Transformers and Circuits (book), Reuben Lee. John Wiley and Sons, New York, N. Y., 1947, pages 41–44.

5. Silicone Insulation Proved by Test, T. A. Kauppi, G. Grant, G. L. Moses, R. F. Horrell. *Westinghouse Engineer* (East Pittsburgh, Pa.), volume 5, September 1945, pages 135–40.

No Discussion

An Analysis of Relay Servomechanisms

DAVID A. KAHN
MEMBER AIEE

LAPLACE transform theory is applied to the analysis of relay-type servomechanisms. Transient and periodic behavior is analyzed, and a method established for determining whether a given system is stable or unstable. The method is not restricted to cases of intermittently applied constant torque as in previous treatments, but is applicable to any controlled object whose unilateral transfer function can be written, as for example, a motor, a vacuum-tube circuit, or a complete servomechanism loop. This method of analysis is directly applicable to problems of missile guidance, torpedo direction, and gun tracking.

General Considerations

The methods of analysis and synthesis of linear servomechanism loops have been developed to the point where they have become a common and useful tool of a large group of engineers. This significant advance in the art of control resulted from the basic work of several writers in this field.[1-3] Comparable advances have not been made in the case of relay-type control although the latter kind of control is of great importance in many engineering fields. The reason for this failure is that the differential equations governing a relay controlled system are nonlinear in nature and cannot be solved by conventional means. In an excellent paper,[4] H. K. Weiss has applied the methods of phase-space to this problem with gratifying results. In the present treatment the system response is analyzed by accepting something less than a finite analytic expression for the solution of the governing differential equation. In the following development, the response of relay-controlled systems will be expressed as a series of terms, either finite or infinite in number, and by the proper use of this series the behavior of the system can be found.

Basic Relations

In this section a semigraphical method of solution is developed which is applied to second-order differential equations where the driving function is a step displacement or step velocity. Solution of higher order equations with arbitrary driving function is treated in a later section.

Figure 1 represents a conventional relay servomechanism loop. The differential equation governing the system is

$$Z(p)y(t)=\Gamma(x-y) \tag{1A}$$

where

$$\Gamma(x-y)=\begin{cases}+E & (x-y)>\varepsilon\\ 0 & -\varepsilon<(x-y)<+\varepsilon\\ -E & (x-y)<-\varepsilon\end{cases} \tag{1B}$$

Figure 2 illustrates three typical system responses, $y_i(t)$, and a plot of $\Gamma(x-y_1)$.

In order to introduce the method of obtaining a solution, consider the case wherein $\varepsilon=0$. The unilateral Laplace transform[5] of equation 1 is

$$Z(s)Y(s)-I(s)=\frac{E}{s}(1-2\epsilon^{-t_1s}+2\epsilon^{-t_2s}-2\epsilon^{-t_3s}+\ldots) \tag{2}$$

where

$Y(s)=\mathcal{L}[y(t)]$

$I(s)$ = a term containing the initial conditions

$t_1, t_2, t_3, \ldots$ are roots of the equation $x(t)-y(t)=0$

From equation 2 is obtained

$$Y(s)=\frac{E}{sZ(s)}(1-2\epsilon^{-t_1s}+2\epsilon^{-t_2s}-2\epsilon^{-t_3s}+\ldots)+\frac{I(s)}{Z(s)} \tag{3}$$

and the inverse transform of equation 3 is

$$y(t)=\eta(t)u(t)-2\eta(t-t_1)u(t-t_1)+2\eta(t-t_2)u(t-t_2)-\ldots+i(t) \tag{4}$$

where

$$\eta(t)=\mathcal{L}^{-1}\left[\frac{E}{sZ(s)}\right]$$

$$i(t)=\mathcal{L}^{-1}\left[\frac{I(s)}{Z(s)}\right]$$

$$u(t-t_i)=\begin{cases}0 & t<t_i\\ 1 & t\geq t_i\end{cases}$$

An alternative way of writing equation 4 is

$$y(t)=\begin{cases}\eta(t)+i(t) & 0\leq t\leq t_1\\ \eta(t)-2\eta(t-t_1)+i(t) & t_1\leq t\leq t_2\\ \eta(t)-2\eta(t-t_1)+2\eta(t-t_2)+i(t) & t_2\leq t\leq t_3\\ \cdot & \cdot\\ \cdot & \cdot\\ \cdot & \cdot\end{cases} \tag{4A}$$

Equation 4 states the interesting fact that the response $y(t)$ of the servomechanism loop may be considered to be composed of

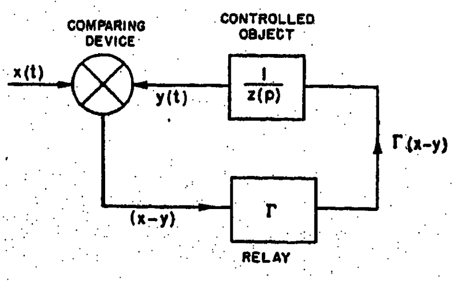


Figure 1. Block diagram of a relay servomechanism loop

Paper 49-224, recommended by the AIEE Joint Subcommittee on Servomechanisms and approved by the AIEE Technical Program Committee for presentation at the AIEE Pacific General Meeting, San Francisco, Calif., August 23-26, 1949. Manuscript submitted May 10, 1949; made available for printing July 11, 1949.

DR. DAVID A. KAHN is with the Cornell Aeronautical Laboratory, Inc., Buffalo, N. Y., formerly with the Curtiss-Wright Corporation, Columbus, Ohio.

This paper is a condensation of a dissertation presented in partial fulfillment of the requirements for the degree, Doctor of Philosophy, in the Graduate School of The Ohio State University.

The author is indebted to Professor C. E. Warren of The Ohio State University for suggestions and criticisms in carrying out this work.

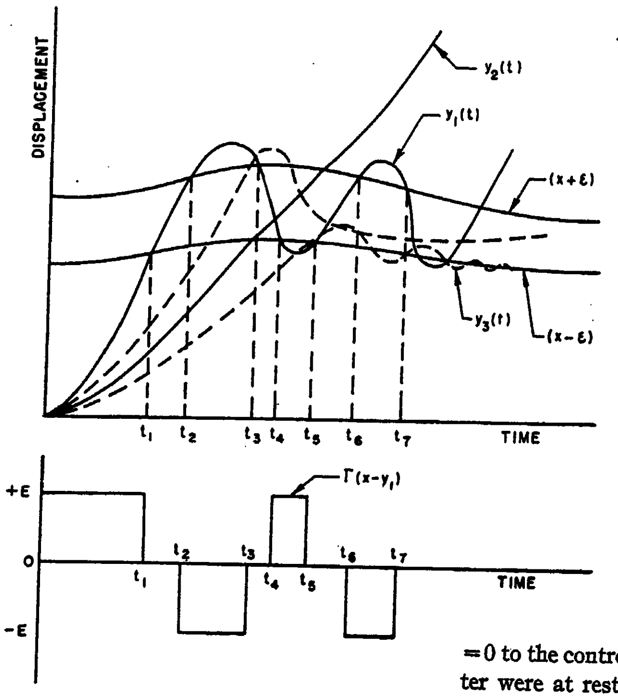


Figure 2 (left). Typical system responses

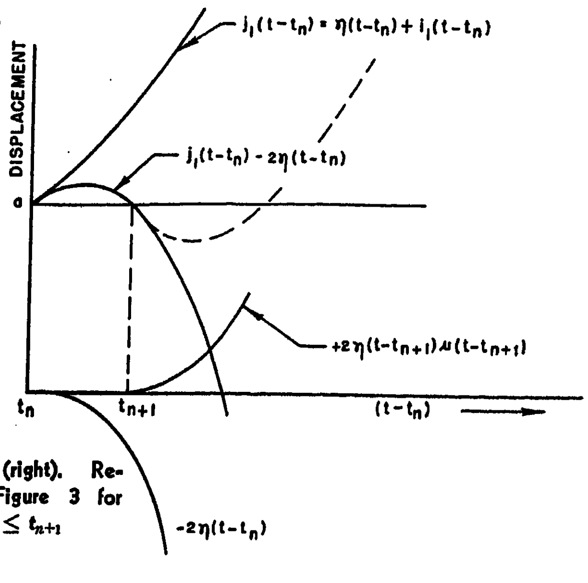


Figure 4(A) (right). Response of Figure 3 for $t_n \leq t \leq t_{n+1}$

a series of terms, each term after the first being of the form $2\eta(t-t_i)u(t-t_i)$.

Figure 3 illustrates equation 4, showing a typical response, $y(t)$, and the components from which it may be synthesized.

The basic quantity $\eta(t)$ which will play an important part in the analysis to follow, has this simple physical significance. It is the output of the controlled object which results from steady application of the input signal $+E$ when the controlled object is initially at rest. That is, if the relay were blocked closed so that its output remained at the constant value $+E$, and if this relay output were applied at $t=0$ to the controlled object when the latter were at rest, then the output of the controlled object would be just $\eta(t)$.

Referring to Figure 3, consider the form the response would take if the relay were blocked so that after some number of operations it remained closed in such direction as to continuously apply $+E$ to the controlled object. Suppose, for example, that this occurred at some value of t between t_{n-1} and t_n. Instead of continuing to oscillate (for illustrative purposes an oscillating response has been assumed) the system output will generate a curve shown qualitatively by the dashed line. If a co-ordinate system were set up with origin at $(t_n, 0)$, then calling the abscissa τ, the dashed curve would be

$$\eta(\tau)+i_1(\tau)$$

where $i_1(\tau)$ represents appropriate initial conditions.

Let

$$j(\tau)=\eta(\tau)+i_1(\tau) \tag{5}$$

All of the preceding remarks apply to a general system subject to the assumptions which were made concerning the form of $\Gamma(x-y)$. Next, however, consider the specific case of a motor having moment of inertia J and velocity friction f, and assume that the relay output appears as a constant torque of magnitude E intermittently applied to the motor shaft. Assume further that the input signal is a step displacement $au(t)$.

Then

$$Z(p)=Jp^2+fp \tag{6}$$

where

$$p=d/dt$$

and

$$\eta(t)=\mathcal{L}^{-1}\left[\frac{\frac{E}{J}}{s^2(s+\alpha)}\right]=\frac{E}{\alpha^2 J}(\epsilon^{-\alpha t}+\alpha t-1) \tag{7}$$

where

$$\alpha=f/J$$

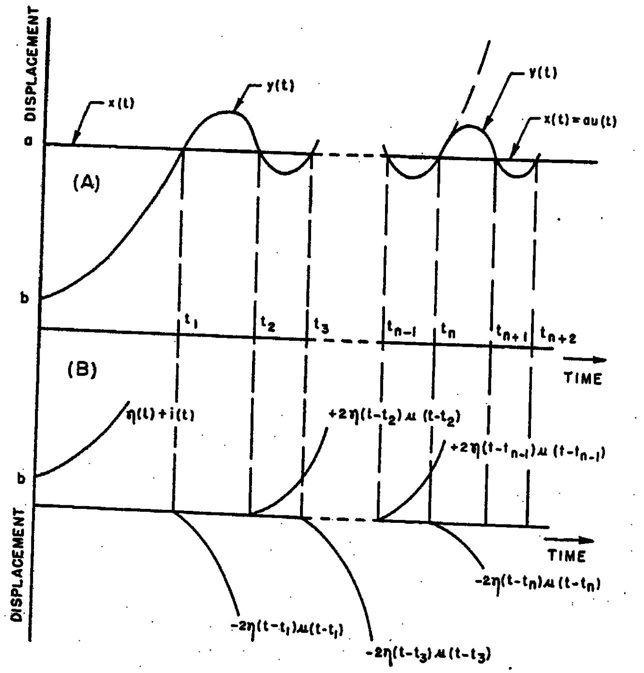
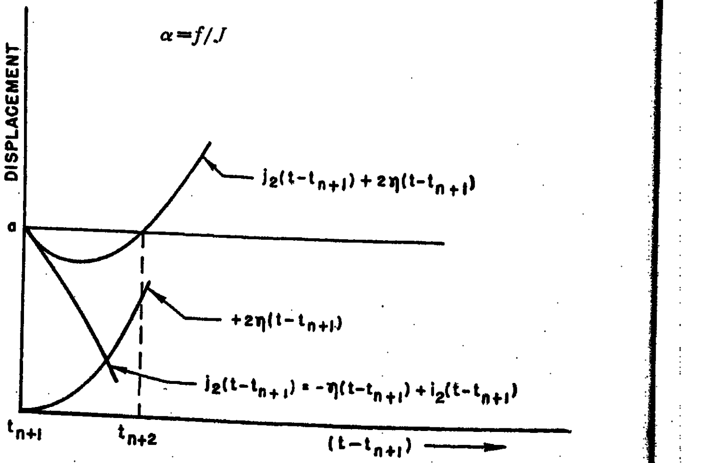


Figure 3 (left). Synthesis of a typical response

Figure 4(B) (right). Response of Figure 3 for $t_{n+1}\leq t\leq t_{n+2}$



By equation 4

$$i_1(t) = \mathcal{L}^{-1}\left[\frac{I(s)}{Z(s)}\right]$$

The form of $I(s)$ is given directly by the process of taking the transform of $Z(p)y(t)$

$$I(s) = Js \text{ (initial displacement)} + J \text{ (initial slope)} + f \text{ (initial displacement)}$$

and

$$i_1(t) = \mathcal{L}^{-1}\left[\frac{\text{initial slope}}{s(s+\alpha)} + \frac{\text{initial displacement}}{s}\right]$$

therefore

$$i_1(t) = \frac{\text{initial slope}}{\alpha}(1-\epsilon^{-\alpha t}) + \text{initial displacement} \quad (8)$$

By "initial slope" and "initial displacement" is meant the slope and displacement, respectively, when the argument of i_1 is zero.

Figure 4(A) is a plot of the portion of Figure 3(A) for which $t \geq t_n$. Since the intersection at $t = t_n$ was arbitrarily selected, the relations qualitatively illustrated are representative of what happens at any intersection of x and y at which y' is positive. The response of the system for $(t-t_n) > 0$ can be constructed by adding the terms $j_1(t-t_n) - 2\eta(t-t_n) + 2\eta(t-t_{n+1}) - \ldots$

It should be noted by reference to Figure 3 and Figure 4(A) that

$$y(t) \equiv j_1(t-t_n) - 2\eta(t-t_n) \quad t_n \leq t \leq t_{n+1} \quad (9)$$

Thus by inspection of Figures 3 and 4(A) one can write

$$i_1(t_{n+1}-t_n) - 2\eta(t_{n+1}-t_n) = a$$

$$y'(t_{n+1}) = j_1'(t_{n+1}-t_n) - 2\eta'(t_{n+1}-t_n) \quad (10)$$

Substitution of equations 7 and 8 in equation 10 leads to

$$y'(t_n) = \frac{E}{\alpha J}\left[\frac{\epsilon^{-\alpha(t_{n+1}-t_n)} + \alpha(t_{n+1}-t_n) - 1}{1-\epsilon^{-\alpha(t_{n+1}-t_n)}}\right] \quad \text{(11A)}$$

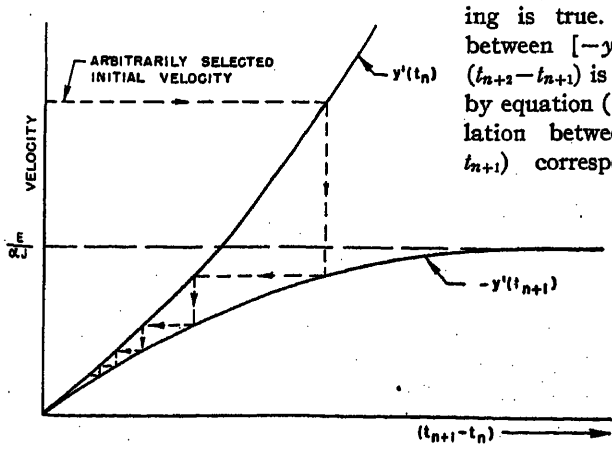


Figure 5. Plot of equations 11 showing the geometry for obtaining the system response

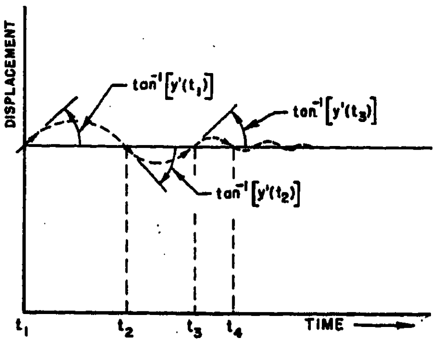


Figure 6. Plot of the information available from Figure 5

$$y'(t_{n+1}) = \frac{E}{\alpha J} \times \left[\frac{\epsilon^{-\alpha(t_{n+1}-t_n)} + \alpha(t_{n+1}-t_n)\epsilon^{-\alpha(t_{n+1}-t_n)} - 1}{1-\epsilon^{-\alpha(t_{n+1}-t_n)}}\right] \quad \text{(11B)}$$

Equations 11 may be qualitatively plotted by inspection and are shown in Figure 5. The two curves represent a functional relation between the velocities at successive intersections of x and y and the interval of time between intersections.

As the reader may have already deduced by observation of the directed dashed line of Figure 5, the solution of the problem is available directly from the two curves. The reason that this is true and the interpretation of the results are as follows.

Since t_n represents any intersection of x and y at which y' is positive, assume that $n = 1$. For any arbitrarily selected velocity assumed to exist at $t = t_1$ enter the curve of $y'(t_n)$ at this value. The abscissa thus determined is the interval $(t_2 - t_1)$, and associated with it is the velocity of the response at the end of the interval, namely, $y'(t_2)$; this is directly obtainable from the curve of $-y'(t_{n+1})$.

Because of certain symmetries in the particular problem selected, it may be shown from physical reasoning or by starting with Figure 4(B) and proceeding as in equations 10 and 11 that the following is true. The functional relation between $[-y'(t_{n+1})]$ and the interval $(t_{n+2} - t_{n+1})$ is identical to that expressed by equation (11A), and similarly, the relation between $[y'(t_{n+2})]$ and $(t_{n+2} - t_{n+1})$ corresponds to equation 11B.

Therefore, if the curve of $y'(t_n)$ is reentered at value of velocity $[-y'(t_2)]$, one may proceed, as illustrated in Figure 5, to find all of the succeeding intervals and the velocities at each intersection.

The information obtainable from Figure 5 is plotted in Figure 6. This is ordinarily ample for engineering design purposes.

Figure 7 illustrates a few typical solutions characteristic of those systems representable by one pair of curves. It will subsequently be shown that for most complex systems two or more pairs of curves are necessary to obtain the system response.

Figure 7(A) clearly indicates instabil-

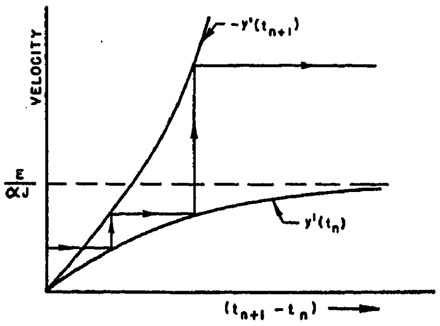


Figure 7(A). An unstable response

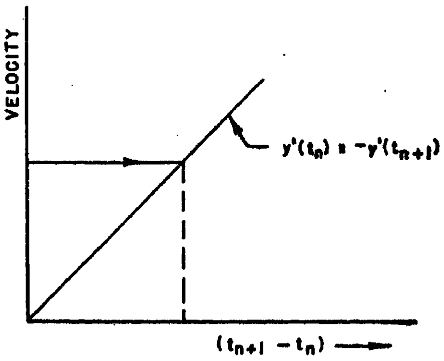


Figure 7(B). A conservative system

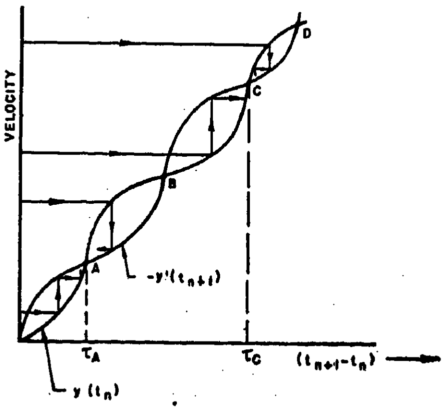


Figure 7(C). A system with limit points

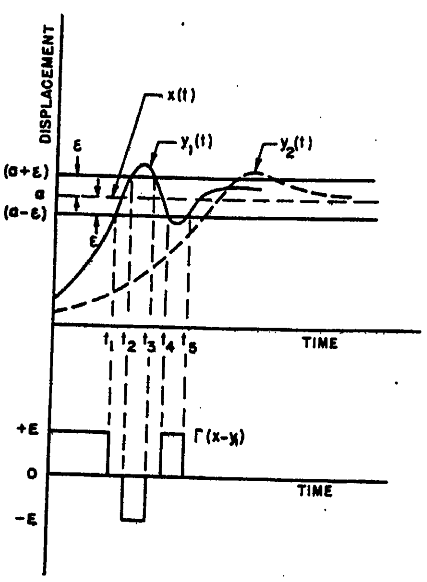


Figure 8(A) (left). Typical response with $\mathcal{E} \neq 0$

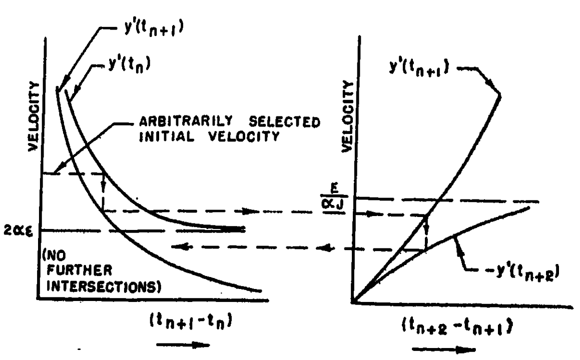


Figure 8(B) (right). Plot of equations 12

ity; at each succeeding intersection the velocity of the system increases and after a finite number of intersections no further zeros of the error $(x-y)$ occur. The curves of Figure 7(A) represent a system having moment of inertia and negative damping.

Figure 7(B) represents an ideal conservative system having inertia only. Here, the curves of $y'(t_n)$ and $[-y'(t_{n+1})]$ degenerate into the same straight line indicating that periodic motion occurs for any initial condition with period depending solely on the magnitude of the initial condition selected.

Figure 7(C) illustrates a system wherein the curves of $y'(t_n)$ and $[-y'(t_{n+1})]$ intersect several times. Points A, B, C, D and the origin are limit points of the solution which correspond to the limit cycles discussed by N. Minorsky.[6] The theorems concerning limit cycles developed by Minorsky may be applied to the limit points of the solution. The limit points may be analytically determined by a method to be described later in this paper.

Figure 8 illustrates the case of a controlled object having inertia and velocity friction. The input is again assumed to be a constant displacement with the slight additional complexity that it is no longer assumed that $\mathcal{E}=0$.

The equations defining the curves of Figure 8(B) are

$$y'(t_n)=\left[\frac{2\alpha\mathcal{E}}{1-\epsilon^{-\alpha(t_{n+1}-t_n)}}\right] \tag{12A}$$

$$y'(t_{n+1})=\left[\frac{2\alpha\mathcal{E}\epsilon^{-\alpha(t_{n+1}-t_n)}}{1-\epsilon^{-\alpha(t_{n+1}-t_n)}}\right] \tag{12B}$$

$$y'(t_{n+1})=\left[\frac{\frac{E}{J}(t_{n+2}-t_{n+1})}{1-\epsilon^{-\alpha(t_{n+2}-t_{n+1})}}\right]-\frac{E}{\alpha J} \tag{12C}$$

$$y'(t_{n+2})=\left[\frac{\frac{E}{J}(t_{n+2}-t_{n+1})\epsilon^{-\alpha(t_{n+2}-t_{n+1})}}{1-\epsilon^{-\alpha(t_{n+2}-t_{n+1})}}\right]-\frac{E}{\alpha J} \tag{12D}$$

The number of excursions across the band formed by $(a-\mathcal{E})$ and $(a+\mathcal{E})$ is determined by the initial conditions and the system parameters. It is readily shown that the system response is obtainable as follows. Enter the curve of $y'(t_n)$ at some arbitrarily selected initial velocity and proceed to determine $(t_{n+1}-t_n)$ and $y'(t_{n+1})$ as in the previous case. At the value of $y'(t_{n+1})$ thus found, enter the second pair of curves to determine $(t_{n+2}-t_{n+1})$ and $-y'(t_{n+2})$. Re-enter the curve of $y'(t_n)$ with this value, and repeat the above procedure. Inspection of Figure 8(B) indicates that there will always be a finite number of crossings of the band. The solution indicated by the directed dashed line of Figure 8(B) corresponds to one crossing of the band as illustrated by $y_2(t)$ of Figure 8(A).

In the derivation of equations 12 the method described previously may be followed in detail except for two minor variations. Firstly, the relay output is now assumed to be given by equation 1B with $\mathcal{E}\neq 0$, so that

$$\mathcal{L}[\Gamma(x-y)]=\int_0^{t_1}E\epsilon^{-st}dt-\int_{t_2}^{t_3}E\epsilon^{-st}dt+\int_{t_4}^{t_5}E\epsilon^{-st}dt\ldots$$
$$=\frac{E}{s}(1-\epsilon^{-t_1s}-\epsilon^{-t_2s}+\epsilon^{-t_3s}+\epsilon^{-t_4s}-\epsilon^{-t_5s}-\ldots) \tag{13}$$

Solution of equation 1 for this case will indicate that the system output $y(t)$ may be considered to be the sum of a series of terms identical in form and disposed along the time axis in the manner indicated in Figure 9. Since only a finite number of oscillations take place under the assumptions leading to equations 12, the series will actually possess a finite number of terms.

The second variation from the previous solution is that the form of $j_1(t)$ and of $j_2(t)$ is not precisely the same as in the derivation leading to equation 11. The form of $j_i(t)$ for any system can be found by physical reasoning, but this is frequently difficult to do. For this reason, the following simple rule for finding j is much preferable. Write down qualitatively the distribution of components comprising the total solution in similar manner to that illustrated in Figure 9. Then $j(t)$ at any instant may be found by summing the components all written with the same argument from $t=0$ up to but not including the instant under consideration and adding the undetermined initial condition.

For example, for the case illustrated by Figure 9

$$\begin{aligned}
j_1(t)&=\eta(t)+i_1(t) && 0<t\leq t_1\\
j_2(t)&=\phantom{\eta(t)+{}}i_2(t) && t_1<t\leq t_2\\
j_3(t)&=-\eta(t)+i_3(t) && t_2<t\leq t_3\\
j_4(t)&=\phantom{\eta(t)+{}}i_4(t) && t_3<t\leq t_4
\end{aligned} \tag{14}$$

The form of $i(t)$ is automatically determined in taking the transform of equation 1.

The discussion of this method of attack is concluded with two further illustrative examples. The first will illustrate how coulomb friction may be handled.

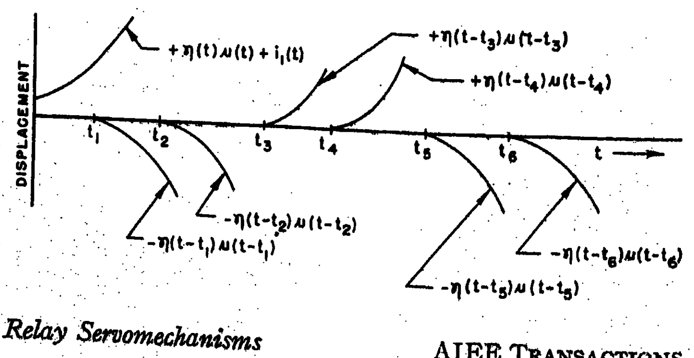


Figure 9. Components comprising the response indicated by Figure 12

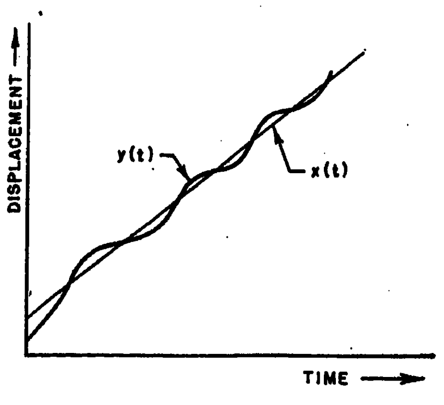


Figure 10(A). Response wherein y′(t) has no reversals of sign

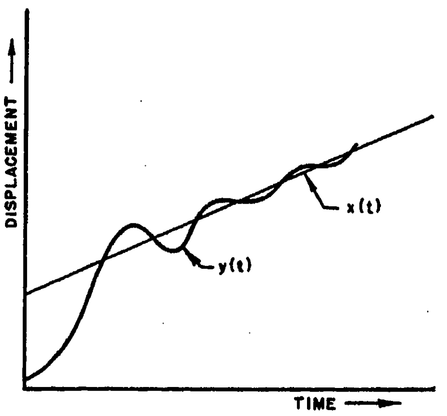


Figure 10(B). Response wherein y′(t) has a finite number of reversals of sign

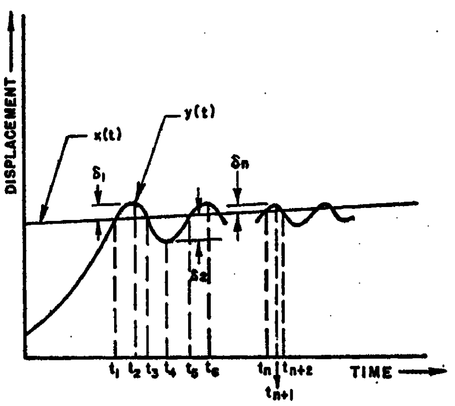


Figure 10(C). Response wherein y′(t) has a large number of reversals of sign

Consider a system having moment of inertia and velocity friction, and suppose that the input $x(t)$ is a velocity step; that is, $x(t) = btu(t)$. Let $C(t)$ = torque due to coulomb friction, and assume that the relay output provides constant torque control. For this example again let $\delta = 0$.

The system response is qualitatively shown in Figure 10. Depending on the system parameters the output velocity $y'(t)$ may reverse its sign zero times as in Figure 10(A) or a small number of times as in Figure 10(B) or a large number of times as in Figure 10(C). The equations of the system will be set up to describe a response of the form (C), and the solution will indicate just how many reversals in sign of $y'(t)$ occur.

Let

$$C(t) = \begin{cases} +K & y'(t) < 0 \\ -K & y'(t) > 0 \end{cases} \tag{15}$$

and let $t_2, t_4, t_6, \ldots$ be roots of $y'(t) = 0$, as shown in Figure 10(C).

Equation 1 now must be replaced by

$$Z(p)y(t) = \Gamma(x-y) + C(t) \tag{16}$$

The transform of equation 16 when solved for $Y(s)$ is

$$Y(s) = \frac{E-K}{sZ(s)}(1-\epsilon^{-t_1 s}-\epsilon^{-t_2 s}+\epsilon^{-t_3 s}+ \epsilon^{-t_4 s}-\ldots)-\frac{E+K}{sZ(s)}(\epsilon^{-t_1 s}-\epsilon^{-t_2 s}- \epsilon^{-t_3 s}+\ldots)+I(s) \tag{17}$$

and therefore

$$y(t) = (E-K)\eta(t)-2E\eta(t-t_1)u(t-t_1)+ 2K\eta(t-t_2)u(t-t_2)+2E\eta(t-t_3)u(t- t_3)-2K\eta(t-t_4)u(t-t_4)-2E\eta(t- t_5)u(t-t_5)-\ldots+i(t) \tag{18}$$

where

$$\eta(t) = \frac{1}{\alpha^2 J}(\epsilon^{-\alpha t}+\alpha t-1)$$

$$\alpha = \frac{f}{J}$$

$$i(t) = \frac{\text{initial velocity}}{\alpha}(1-\epsilon^{-\alpha t}) + (\text{initial displacement})$$

By following the procedure described previously, the following system of equations results

$$y'(t_n) = \frac{E+K}{\alpha J}\left[\frac{1-\epsilon^{-\alpha(t_{n+1}-t_n)}}{\epsilon^{-\alpha(t_{n+1}-t_n)}}\right] \tag{19A}$$

$$[\delta n - a(t_{n+1}-t_n)] = \frac{E+K}{\alpha^2 J}\times \left[\frac{1-\epsilon^{-\alpha(t_{n+1}-t_n)}}{\epsilon^{-\alpha(t_{n+1}-t_n)}}\right]-\left[\frac{E+K}{\alpha J}+a\right] (t_{n+1}-t_n) \tag{19B}$$

$$[\delta_n - a(t_{n+1}-t_n)] = \frac{E-K}{\alpha^2 J}\times [\epsilon^{-\alpha(t_{n+2}-t_{n+1})}+\alpha(t_{n+2}-t_{n+1})-1]+ a(t_{n+2}-t_{n+1}) \tag{19C}$$

$$y'(t_{n+2}) = -\frac{E-K}{\alpha J}[1-\epsilon^{-\alpha(t_{n+2}-t_{n+1})}] \tag{19D}$$

$$y'(t_{n+2}) = -\frac{E+K}{\alpha J}\left[\frac{1-\epsilon^{-\alpha(t_{n+3}-t_{n+2})}}{\epsilon^{-\alpha(t_{n+3}-t_{n+2})}}\right] \tag{19E}$$

$$[\delta_{n+1}+a(t_{n+3}-t_{n+2})] = \frac{E+K}{\alpha^2 J}\times \left[\frac{1-\epsilon^{-\alpha(t_{n+3}-t_{n+2})}}{\epsilon^{-\alpha(t_{n+3}-t_{n+2})}}-\alpha(t_{n+3}-t_{n+2})\right]+ a(t_{n+3}-t_{n+2}) \tag{19F}$$

$$[\delta_{n+1}+a(t_{n+3}-t_{n+2})] = \frac{E-K}{\alpha^2 J}\times [\epsilon^{-\alpha(t_{n+4}-t_{n+3})}+\alpha(t_{n+4}-t_{n+3})- 1]-a(t_{n+4}-t_{n+3}) \tag{19G}$$

$$y'(t_{n+4}) = \frac{E-K}{\alpha J}[1-\epsilon^{-\alpha(t_{n+4}-t_{n+3})}] \tag{19H}$$

Equations 19 are plotted qualitatively in Figure 11.

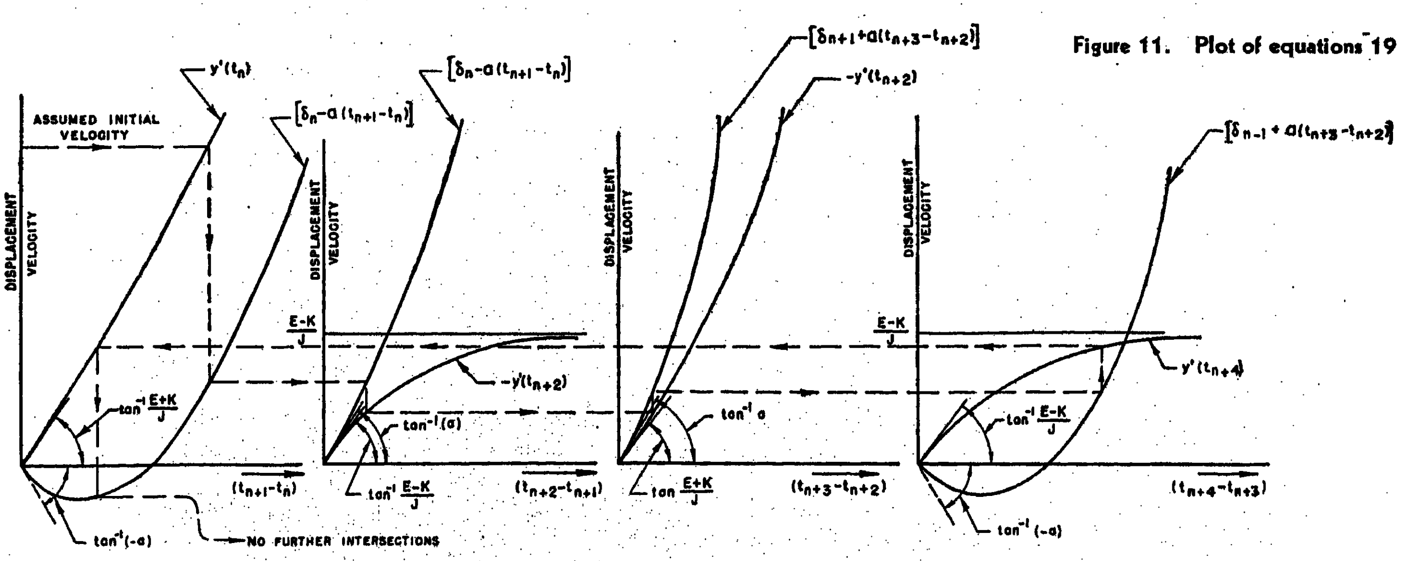


Figure 11. Plot of equations 19

For the particular value of initial velocity chosen as an illustration in Figure 11, there are just two reversals in sign of $y'(t)$. It may be seen from Figure 11 that if the initial velocity is chosen small enough, there will be zero reversals in sign of $y'(t)$, and that no matter how large the initial velocity is chosen, there will be only a finite number of such reversals. If it is desired to carry the solution into the region wherein $y'(t)$ is of one sign, equation 16 assumes the special form

$$Z(p)y(t) = \Gamma(x-y) - K \qquad (16A)$$

The solution of equation 16A follows the same pattern as equation 16 and will not be carried out here.

As a concluding example before proceeding to a discussion of periodic solutions, consider the following case. Suppose that the electromechanical unilateral transfer function of the controlled object is $Z(s)$; that is, $Z(s)$ is the ratio of input to output of the controlled object expressed as a function of the complex frequency s. Let the relay output be given by equation 1B and suppose that due to the physical nature of the circuit there is a time delay t_{d_1} associated with the relay operation; that is, t_{d_1} is the time interval between the instant at which the relay receives the signal and the instant at which the voltage appears at the relay output. Similarly, let t_{d_2} be the time delay associated with the relay release; that is, t_{d_2} is the time interval between the instant at which the signal to the relay is removed and the instant at which the relay contacts open. Assume further that the input signal is $x(t) = at+b$.

In dealing with an actual relay, the voltage differential of the relay may not be negligible. The relay voltage differential results in the relay closing at one value of error and reopening at some lower value. In this example voltage differential is taken into account and its effect is shown in Figure 12(A).

The complete solution of the foregoing problem will not be carried out, but the method will be indicated in detail.

For convenience in Figure 12 and in what follows let $t_{id_1} = t_i+t_{d_1}$ and $t_{jd_2} = t_j+t_{d_2}$.

The equation of the system when transformed and solved explicitly for $Y(s)$ is

$$Y(s) = \frac{E}{sZ(s)}\left(1-\epsilon^{-t_{1d_2}s}-\epsilon^{-t_{2d_1}s}+\epsilon^{-t_{3d_2}s}+\epsilon^{-t_{4d_1}s}-\ldots\right)+I(s) \qquad (20)$$

so that

$$y(t) = \eta(t)u(t)-\eta(t-t_{1d_2})u(t-t_{1d_2})-\eta(t-t_{2d_1})u(t-t_{2d_1})+\eta(t-t_{3d_2})u(t-t_{3d_2})+\ldots+i(t) \qquad (21)$$

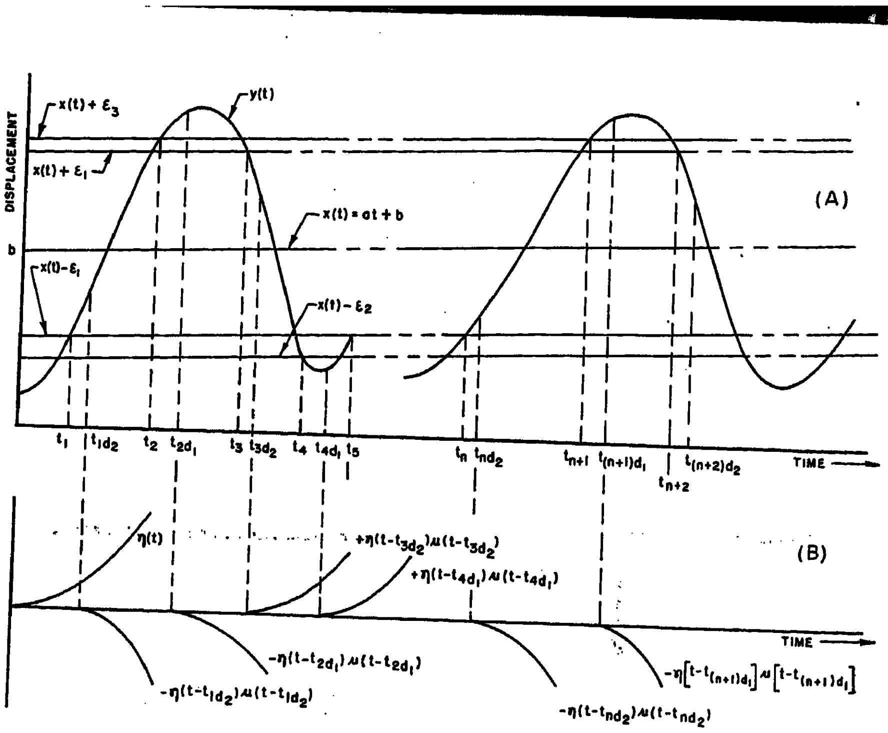


Figure 12. System response showing the components from which it may be generated

The components of which $y(t)$ is comprised are shown in Figure 12(B). The solution for $y(t)$ is available by drawing the eight pairs of curves

$$\begin{aligned}
&y'(t_n) \text{ and } \delta_n \text{ versus } (t_{nd_2}-t_n)\\
&\delta_n \text{ and } y'(t_{n+1}) \text{ versus } (t_{n+1}-t_{nd_2})\\
&y'(t_{n+1}) \text{ and } \delta_{n+1} \text{ versus } (t_{(n+1)d_1}-t_{n+1})\\
&\delta_{n+1} \text{ and } y'(t_{n+2}) \text{ versus } (t_{n+2}-t_{(n+1)d_1})\\
&y'(t_{n+2}) \text{ and } \delta_{n+2} \text{ versus } (t_{(n+2)d_2}-t_{n+2})\\
&\delta_{n+2} \text{ and } y'(t_{n+3}) \text{ versus } (t_{n+3}-t_{(n+2)d_2})\\
&y'(t_{n+3}) \text{ and } \delta_{n+3} \text{ versus } (t_{(n+3)d_1}-t_{n+3})\\
&\delta_{n+3} \text{ and } y'(t_{n+4}) \text{ versus } (t_{n+4}-t_{(n+3)d_1}) \qquad (22)
\end{aligned}$$

This concludes the presentation of the basic method for obtaining the desired solutions. A large amount of work can be done in expanding this analysis to cover cases of increasingly complicated transfer functions. An investigation also is in order to determine the possibility of generating the curves typified by Figure 11 in nondimensional form.

Periodic Solutions

It frequently occurs in physical systems of the kind here considered that the response ultimately becomes a periodic function of time. The curves of Figure 7(C), for example, represent a system which may oscillate with frequency $1/\tau_A$ or $1/\tau_C$ depending on the initial condition. Periodic oscillation also may occur in some very elementary types of cases involving time delays in the operation of the controlling relay.

In certain interesting cases wherein the solution is derived from two or more pairs of curves, the period of oscillation may not be apparent by inspection. For example, the system illustrated in Figure 13 ultimately oscillates with periodic motion as indicated by the fact that the directed line will ultimately approach a limiting rectangle. Figure 13 in fact represents the motion of a torsion pendulum having moment of inertia, viscous friction, and a restoring torque proportional to its displacement. In obtaining the solution of the problem one can readily draw the curves of Figure 13 in a qualitative way, thus determining that the system response does become periodic. In order to graphically determine the limiting rectangle the curves must be quantitatively correct and careful geometric plotting of the directed line must be accomplished. Instead of proceeding in such a manner, a much shorter procedure will be described which leads to analytic information concerning the periodic behavior of the system.

A theorem taken from the Laplace transform theory will be utilized in the following development. The theorem states essentially that if a transformable function of t is periodic, its transform can have no singularities except perhaps single order poles harmonically spaced along the imaginary axis.[7] That is, if some function $d(t)$ is known to be periodic, then its transform $D(s)$ must be of the form

$$D(s) = \frac{M(s)}{1-\epsilon^{-\delta s}}$$

where $M(s)$ has no poles. The equally spaced single order poles along the imaginary axis as required by the theorem are all contained in factor $1/(1-\epsilon^{-\delta s})$.

When the curves such as those typified by Figure 13 are plotted, a qualitative investigation will indicate that the response $y(t)$ ultimately becomes periodic; that is, the function $y_u(t)-x_u(t)$ has periodically spaced zeros where the subscript u designates the function for t large. Let

$$d_u(t)=y_u(t)-x_u(t) \tag{23}$$

and let

$$\mathcal{L}[d_u(t)]=D_u(s)$$

By $\mathcal{L}[d_u(t)]$ is meant the transform of a function having the form of $d_u(t)$ for all t. Then according to the theorem $D_u(s)=M(s)/(1-\epsilon^{-\delta s})$ where $M(s)$ is everywhere analytic. Just how this information is to be applied may be made clear by the following example.

Assume that the system response, when transformed, is given by equation 3 and that the input giving rise to equation 3 is $x(t)=au(t)$, so that $X(s)=a/s$.

It is assumed that $y_v(t)-x_u(t)$ is periodic with one positive excursion and one negative excursion comprising a period. For $t \geq t_n$, that is, after n intersections

$$\begin{aligned}D_u(s)&=Y_u(s)-X_u(s)\\&=\left[\frac{E\epsilon^{-t_ns}}{sZ(s)}+\frac{I(s)\epsilon^{-t_ns}}{Z(s)}-\frac{a}{s}\epsilon^{-t_ns}\right]-\\&\quad 2\frac{E}{sZ(s)}(\epsilon^{-t_ns}-\epsilon^{-t_{n+1}s}+\\&\quad\epsilon^{-(t_n+\delta)s}-\epsilon^{-(\delta+t_{n+1})s}+\ldots)\end{aligned} \tag{24}$$

Write

$$\epsilon^{-t_ns}(1-\epsilon^{-(t_{n+1}-t_n)s}+\epsilon^{-\delta s}-\epsilon^{-(\delta+t_{n+1}-t_n)s}+\ldots)=\frac{1-\epsilon^{-(t_{n+i}-t_n)s}}{1-\epsilon^{-\delta s}}$$

This is true for $Re(s)>0$ since the expression on the left is a geometric progression. Therefore

$$D_u(s)=\epsilon^{-t_ns}\left\{\frac{E+sI-aZ(s)}{sZ(s)}-\frac{2E(1-\epsilon^{-(t_{n+1}-t_n)s}}{sZ(s)(1-\epsilon^{-\delta s})}\right\}$$

$$D_u(s)=\epsilon^{-t_ns}\frac{[sI-aZ(s)](1-\epsilon^{-\delta s})-E(1-2\epsilon^{-(t_{n+1}-t_n)s}+\epsilon^{-\delta s})}{sZ(s)(1-\epsilon^{-\delta s})} \tag{25}$$

Let the right-hand side of equation 25 be equal to $M(s)/(1-\epsilon^{-\delta s})$ so that

$$M(s)=\frac{[sI(s)-aZ(s)](1-\epsilon^{-\delta s})-E(1-2\epsilon^{-(t_{n+1}-t_n)s}+\epsilon^{-\delta s})\epsilon^{-t_ns}}{sZ(s)} \tag{26}$$

In accordance with the theorem, $M(s)$ can have no poles. To illustrate how this requirement may be utilized, consider the case of a conservative system having moment of inertia J, so that $Z(s)$ is simply Js^2 and $I(s)=Jsa+Jy'(t_n)$. Equation 26 therefore becomes

$$M(s)=\frac{sy'(t_n)(1-\epsilon^{-\delta s})-\frac{E}{J}(1-2\epsilon^{-(t_{n+1}-t_n)s}+\epsilon^{-\delta s})}{s^3} \tag{27}$$

Let $M(s)=N(s)/(s^3)$ and expand $N(s)$ in a MacLaurin series. Then

$$M(s)=\frac{N(0)+sN'(0)+\frac{s^2N''(0)}{2!}+\frac{s^3N'''(0)}{3!}+\ldots}{s^3}$$

and if $M(s)$ is to have no poles, then

$$\begin{aligned}N(0)&=0\\N'(0)&=0\\N''(0)&=0\end{aligned} \tag{28}$$

Since

$$N(s)=sy'(t_n)(1-\epsilon^{-\delta s})-\frac{E}{J}(1-2\epsilon^{-(t_{n+1}-t_n)s}+\epsilon^{-\delta s})$$

therefore the conditions in equation 28 lead to

$$(t_{n+1}-t_n)=\frac{\delta}{2} \tag{29A}$$

$$y'(t_n)=\frac{E}{2J}(t_{n+1}-t_n)=\frac{E}{4J}\delta \tag{29B}$$

Equation 29A states that during periodic motion of the system under consideration, the positive excursion lasts one half a period. From equation 29B it can be found that if the system velocity is some value, say v_0, at t_n, then the period of oscillation, δ, is $4v_0J/(E)$.

Consider one further illustrative example. Suppose the input is of the form $x(t)=bt$ and let the transfer function of the controlled object $Z(s)$ be unspecified. Assume $\mathcal{E}\neq 0$ so that $\Gamma\ (x-y)$ is given by equation 1B and assume further that a time delay t_{d_1} is associated with relay "make" and that t_{d_2} is associated with "break". Then

$$\begin{aligned}D_{un}(s)=\epsilon^{-t_ns}\Bigg\{&\left(\frac{E}{sZ(s)}+\frac{I_n(s)}{Z(s)}-\frac{a}{s}\right)-\\&\frac{E}{sZ(s)}\Bigg(\frac{\epsilon^{-t_{d_1}s}+\epsilon^{-(t_{n+1}+t_{d_2}-t_n)s}}{1-\epsilon^{-\delta s}}-\\&\frac{+\epsilon^{-(t_{n+2}+t_{d_1}-t_n)s}+\epsilon^{-(t_{n+3}+t_{d_2}-t_n)s}}{1-\epsilon^{-\delta s}}\Bigg)\Bigg\}\end{aligned} \tag{30}$$

Equation 30 was obtained by writing the initial conditions with respect to t_n; that is, $I_n(s)$ will be a function of $y'(t_n)$. Equations similar to 30 may be written based on conditions at t_{n+1}, t_{n+2}, and t_{n+3}. The resulting equations can be put in the form

$$D_{uk}(s)=\frac{M_k(s)}{1-\epsilon^{-\delta s}}\quad k=1,2,3,4 \tag{31}$$

$M_k(s)$ may be treated in the manner just described for the conservative system. The resulting equations will determine a

Figure 13 (below). A case wherein the directed generating line approaches a limiting rectangle

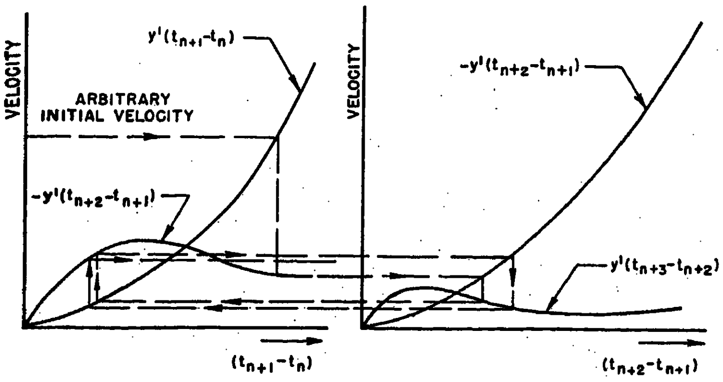
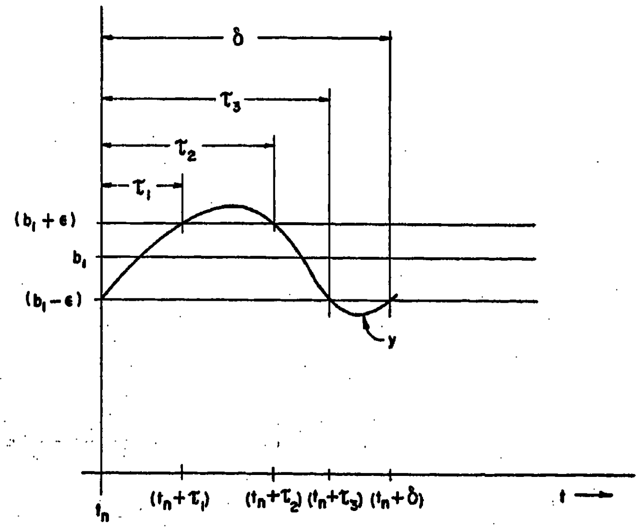


Figure 14 (right). One cycle of a periodic response



set of relations among the circuit parameters that must hold when the system oscillates with periodic motion.

Solution of Higher Order Equations

In order to summarize the methods described before, a particular equation now will be treated and information about its behavior obtained.

Consider as an example the equation

$$a_6\frac{d^6y}{dt^6}+a_5\frac{d^5y}{dt^5}+a_4\frac{d^4y}{dt^4}+a_3\frac{d^3y}{dt^3}+a_2\frac{d^2y}{dt^2}+ a_1\frac{dy}{dt}+a_0y=\Gamma(x-y) \qquad (32)$$

where the a_i are real, and

$$\Gamma(x-y)=\begin{cases}+E & (x-y)>\varepsilon\\ 0-\varepsilon\leqq(x-y)\leqq+\varepsilon\\ -E & (x-y)<-\varepsilon\end{cases}$$

When transformed, equation 32 becomes

$$Y(s)=\frac{\left(\frac{E}{a_6}\right)(1-\epsilon^{-t_1s}-\epsilon^{-t_2s}+\epsilon^{-t_3s}+\epsilon^{-t_4s}-\ldots)}{s\left(s^6+\frac{a_5}{a_6}s^5+\frac{a_4}{a_6}s^4+\frac{a_3}{a_6}s^3+\frac{a_2}{a_6}s^2+\frac{a_1}{a_6}s+\frac{a_0}{a_6}\right)}+ \frac{\frac{1}{a_6}I_0[s,y(0^+),y'(0^+),\ldots.y^{(5)}(0^+)]}{\left(s^6+\frac{a_5}{a_6}s^5+\ldots.+\frac{a_0}{a_6}\right)} \qquad (33)$$

where I_0 is determined in routine manner and contains the initial conditions. The t_i are as defined by Figure 2.

The expression

$$\left(s^6+\frac{a_5}{a_6}5^5+\ldots+\frac{a_0}{a_6}\right)$$

now must be written as

$$Q(s)=(s+\alpha_1)(s+\alpha_2)(s+\alpha_3)\ldots(s+\alpha_6)$$

Suppose that first one inquires as to the periodic behavior of the system when the driving function is

$$x(t)=b_1u(t) \qquad (34)$$

This simple form of $x(t)$ is chosen as an example although the method is equally applicable to any transformable $x(t)$.

Form $D_u(s)=Y_u(s)-X(s)$ as shown in the section on periodic solutions.

$$D_{u_1}(s)=\epsilon^{-t_ns}\{H(s)+I_1(s)-X(s)-H(s)(1+\epsilon^{-\tau_1s})-\epsilon^{-\tau_2s}-\epsilon^{-\tau_3s}+\epsilon^{-\delta s}+\epsilon^{-(\delta-\tau_1)}+\ldots\}$$

$$D_{u_1}(s)=\left\{\frac{(1-\epsilon^{-\delta s})\left[s\frac{1}{a_6}I_1-b_1Q(s)\right]}{sQ(s)(1-\epsilon^{-\delta s})}-\frac{\frac{E}{a_6}\left[\epsilon^{-\tau_1s}-\epsilon^{-\tau_2s}-\epsilon^{-\tau_3s}+\epsilon^{-\delta s}\right]}{sQ(s)(1-\epsilon^{-\delta s})}\right\}\times \epsilon^{-t_ns} \qquad (35)$$

$$D_{u_1}(s)=\left\{\frac{\left[\frac{N_1(s)}{s(s+\alpha_1)(s+\alpha_2)\ldots(s+\alpha_6)}\right]}{(1-\epsilon^{-\delta s})}\right\}\epsilon^{-t_ns} =\frac{M_1(s)\epsilon^{-t_ns}}{(1-\epsilon^{-\delta s})} \qquad (36)$$

The τ_i are defined by Figure 14.

In order to describe the periodic state, the following 28 quantities must be determined.

$$y(t_n),y'(t_n),\ldots y^{(5)}(t_n),y(t_{n+1}), y'(t_{n+1}),\ldots y^{(6)}(t_{n+1}), y(t_{n+2}),\ldots y(t_{n+3}),\ldots,\tau_1,\tau_2,\tau_3,\delta$$

It may be observed by reference to Figure 14 that

$$y(t_n)=y(t_{n+3})=b_1-\varepsilon$$
$$y(t_{n+1})=y(t_{n+2})=b_1+\varepsilon$$

so that there remain 24 undetermined quantities.

From the requirement that $M_1(s)$ be analytic, one can obtain the six conditions

$$N_1(-\alpha_i)=0 \qquad i=1,2,\ldots 6 \qquad (37)$$

By exactly the same procedure one may write the requirement that $D_{u_2}(s)$, $D_{u_3}(s)$, and $D_{u_4}(s)$ be periodic. The initial condition term for these cases will contain derivatives evaluated at (t_{n+1}), (t_{n+2}), and (t_{n+3}).

The form of $D_{u_2}(s)$, $D_{u_3}(s)$ and $D_{u_4}(s)$ may be found by reference to equations 24 and 13. The $D_{u_i}(s)$ are

$$D_{u_2}(s)=\epsilon^{-t_ns}\{I_2(s)-X(s)-H(s)[\epsilon^{-\tau_1s}-\epsilon^{-(\tau_2-\tau_1)s}-\epsilon^{-(\tau_3-\tau_1)s}+\epsilon^{-(\delta-\tau_1)s}]\}$$

$$D_{u_3}(s)=\epsilon^{-t_ns}\{-H(s)+I_3(s)-X(s)+H(s)[\epsilon^{-\tau_2s}+\epsilon^{-(\tau_3-\tau_2)s}-\epsilon^{-(\delta-\tau_2)s}-\epsilon^{-(\delta+\tau_1-\tau_2)s}]\}$$

$$D_{u_4}(s)=\epsilon^{-t_ns}\{I_4(s)-X(s)+H(s)\times[\epsilon^{-\tau_3s}-\epsilon^{-(\delta-\tau_3)s}-\epsilon^{-(\delta+\tau_1-\tau_3)s}+\epsilon^{-(\delta+\tau_2-\tau_3)s}]\} \qquad (38)$$

Each of these three equations will yield six conditions. These together with the six relations expressed by equation 37 will determine the 24 quantities which completely describe the periodic behavior of the system.

The following should be noted. The addition of an arbitrary function, say $g(t)$, to the right-hand side of equation 32 does not complicate the problem, except that for certain classes of function, for example, $g(t)=c_1t+c_2\epsilon^{-\gamma t}\sin\omega t$, the equations will show that periodic motion will not occur. Of course one may choose to wait in this case until the exponential causes the offending term to become negligible. It should further be noted that the addition of time delay, t_d, merely changes the τ_i to (τ_i+t_d).

It now will be shown how the transient solution can be found.

In treating higher order differential equations, the method of inspecting the first derivative to determine the system behavior is no longer directly applicable since corresponding inspection of all derivatives would require portrayal in some manner in multidimensional space. However, the more general aspects of this paper may be applied as follows.

Consider again equation 32 and to increase its generality slightly add a transformable but otherwise arbitrary function $G(t)$ to its right-hand side. Then the transformed equation is

$$Y(s)=H(s)(1-\epsilon^{-t_1s}-\epsilon^{-t_2s}+\epsilon^{-t_3s}+\epsilon^{-t_4s}-\ldots)+I(s)+G_1(s) \qquad (39)$$

where

$$H(s)=\frac{\frac{E}{a_6}}{s(s+\alpha_1)(s+\alpha_2)\ldots(s+\alpha_6)}$$

$$I(s)=\frac{\frac{1}{a_6}I_0(s,y(0),y'(0)\ldots y^{(5)}(0)}{(s+\alpha_1)(s+\alpha_2)\ldots(s+\alpha_6)}$$

$$G_1(s)=\frac{\frac{1}{a_0}G(s)}{(s+\alpha_1)(s+\alpha_2)\ldots(s+\alpha_6)}$$

The inverse transform of equation 39 is

$$y(t)=\eta(t)-\eta(t-t_1)u(t-t_1)-\eta(t-t_2)u(t-t_2)+\eta(t-t_3)u(t-t_3)+\ldots+i(t)+g_1(t) \qquad (40)$$

where

$$\eta(t)=\mathcal{L}^{-1}[H(s)]$$
$$i(t)=\mathcal{L}^{-1}[I(s)]$$
$$g_1(t)=\mathcal{L}^{-1}[G_1(s)]$$

Next assume some $x(t)$ for purposes of illustration. The $x(t)$ may be completely arbitrary; in fact, let it be assumed that $x(t)$ is available only as a graphical plot. Then from equation 40 one proceeds as follows:

Plot $\eta(t)$, $i(t)$, $g_1(t)$, $(x+\varepsilon)$, and $(x-\varepsilon)$. The intersection of $[\eta(t)+i(t)+g_1(t)]$ with $(x-\varepsilon)$ determines t_1. Having thus obtained t_1, plot $\eta(t-t_1)\ u(t-t_1)$. Then the intersection of $[\eta(t)-\eta(t-t_1)u(t-t_1)+i(t)+g_1(t)]$ with $(x+\varepsilon)$ determines t_2 so that $\eta(t-t_2)$ may be determined. One may proceed indefinitely in this manner obtaining $y(t)$ for any desired interval. It should be noted that it is not required to match boundary conditions at the discontinuities of $\Gamma(x-y)$ and that the only quantities which need be plotted are $\eta(t)$, $i(t)$, $g_1(t)$ and $x\pm\varepsilon$, since $\eta(t)$ may be put on a slip sheet and shifted along the time axis to represent $\eta(t-t_j)\ u(t-t_j)$.

This concludes the discussion of this particular example.

Conclusions

The method of attack described in this paper is applicable to a wide variety of on-off type problems. This method should be particularly useful to designers of control systems for turrets, pilotless aircraft, torpedoes, and to designers in certain industrial fields. The examples chosen to illustrate certain typical behaviors were all taken from the field of mechanics in order that a simple physical picture could be associated with the mathematical manipulation. In the examples discussed moment of inertia, friction, and torque were taken as parameters. These parameters could instead have been chosen as inductance, resistance, and voltage, for example.

References

1. The Analysis and Synthesis of Linear Servomechanisms (book), Albert C. Hall. The Technology Press, Massachusetts Institute of Technology (Cambridge, Mass.) 1943.

2. Fundamental Theory of Servomechanisms (book), LeRoy A. MacColl. D. Van Nostrand Co. Inc., New York, N. Y., 1945.

3. Behavior and Design of Servomechanisms, Gordon S. Brown. Printed November, 1940, under auspices of Fire Control Committee (Sec. D-2) of National Defense Research Committee.

4. Analysis of Relay Servomechanisms, H. K. Weiss. *Journal*, Aeronautical Sciences (New York, N. Y.), volume 13, July 1946, page 364.

5. Transients in Linear Systems (book), M. F. Gardner, J. L. Barnes. John Wiley and Sons, Inc., New York, N. Y., 1942.

6. Introduction to Non-Linear Mechanics (book), N. Minorsky. J. W. Edwards, Ann Arbor, Mich., 1947.

7. The Laplace Transform (book), D. V. Widder. Princeton University Press, Princeton, N. J., 1946.

Discussion

W. J. Schart (Consolidated Vultee Aircraft Corporation, San Diego, Calif.): All in all the paper is a very good presentation of a new technique, especially the concept portrayed in its simplest form by Figure 5. I did have a little trouble, however, in following the author's development and hope that my suggestions will be of help to the author.

The essence of the technique of finding the size of the successive time intervals is based on the fact that $y'(t_n)$ and $y'(t_{n+1})$ can be expressed in terms of the known quantities E, J, α, and the interval $T_{n+1}=t_{n+1}-t_n$. In other words $y'(t_n)$ and $y'(t_{n+1})$ are known functions of T_{n+1}.

Once this is known, then the use of Figure 5 shows how these known functions can be used to determine the intervals under discussion.

Having shown the essence of the technique for the simple case, I would suggest that the author point out, that for $x(t)=au(t)$, we have $y(t_n)=y(t_{n+1})$, and that under these circumstances it is possible to express $y'(t_n)$ and $y'(t_{n+1})$ in terms of known quantities and the interval T_{n+1}. It also could be mentioned at this point that the technique readily generalizes to the case where $x(t)=bt\,u(t)$, for under this condition $y(t_n)-y(t_{n+1})=x(t_n)-x(t_{n+1})$.

From these remarks the technique is readily generalized to the other systems discussed by the author.

These comments are necessarily quite brief, since technically there is nothing to criticise in the paper, and very little to be added since it is very complete and thorough in its presentation. My comments are merely concerned with the psychology of its presentation.

Ben Ciscel (Minneapolis-Honeywell Regulator Company, Minneapolis, Minn.): This paper is a most welcome addition to the lore of automatic control. The exposition of the simple system with no neutral zone, then the inclusion of a neutral zone, coulomb friction, dead time in relay operation, and differential relay response when opening and closing, all display a remarkable awareness of the practical problems in the field.

Of course, there are still other enigmas. The first that came to my mind was that of defining the output response with the controlled object initially at rest. The presence of generated electromotive forces will add another element of nonlinearity in many systems. There are other controls which have a narrow band of proportionality whose dynamic performance could be analyzed by the methods outlined in Dr. Kahn's paper. These include clutch servos and some amplifier-servo combinations which reach torque saturation with very small error signals.

As a personal note, I would like to raise one slight criticism. The practicing engineer is engrossed in a dual problem—one is that of doing his work and the other is that of keeping up with the technical advances of his field. The latter job could be made much easier if the technical papers were more consistent in their use of symbols, letters, and terminology. The little things that slow down the reading and assimilation of a paper such as this were: the use of t for roots and time, the use of E for voltage and for torque, the use of j in a nonimaginary sense, (especially when it has no relationship to capital J, nor any other apparent reason for its choice), the use of x for displacement and as a multiplication sign, and the inexplicable use of p as a differential operator in equation 6. These do not detract from the value of a paper, but they do prolong the reading time.

I am looking forward to the opportunity of applying some of this relay control theory to some of our systems, and appreciate this opportunity to offer a few comments on the paper.

R. J. Kochenburger (Massachusetts Institute of Technology, Cambridge, Mass.): Dr. Kahn's paper represents a timely treatment of a subject possessing considerable importance from the standpoint of engineering application, but which has been generally neglected as far as the development of methods of analysis and synthesis are concerned. Unlike continuous servomechanisms, which have received extensive treatment in the literature, this subject has been given consideration by relatively few authors. The most outstanding previous contributions to date are probably those of MacColl and Weiss shown as references 2 and 4 of the paper, and that of H. L. Hazen in 1939.[1]

The methods for treating relay servomechanisms described prior to this paper were generally applicable to only the simpler forms of such systems. The basic method of analysis proposed by Dr. Kahn possesses the advantage that it can be extended to more complicated systems (systems described by differential equations of higher order); naturally, the labor of computation would increase under such circumstances. The ability to treat more complicated systems is an important consideration in any method of analysis because servomechanisms including even the simpler forms of servomotors can involve complicated dynamics when suitable compensating or "computer" networks are introduced.

In view of the varied forms of treatment proposed for the analysis of relay servomechanisms, some review may be advisable. Three basic problems are considered by the contributors in the field. The first involves a method for determining the transient response of the system to various disturbances and obtaining the output or error response as a function of time. The second involves a method for determining the "relative stability" or damping of such oscillations by graphical methods without involving the necessity of computing the actual time response. The third involves the determination of conditions under which self-sustained oscillations may arise.

Dr. Hazen treats the first problem by means of the classical differential equation approach. Weiss and MacColl do not treat it directly but indicate how the time response may be determined by means of auxiliary constructions associated with the "phase-plane" method of approach. Dr. Kahn suggests a means of analysis whereby it is merely necessary to determine the system response to a step-function disturbance initiated by the relay. Each time that a new relay switching operation takes place, it may be taken into account by adding or subtracting this new step-function response, with a translated time axis, from the response relationship already developed by the accumulation of earlier switching operations. The process is reduced to a relatively simple graphical procedure and there is no apparent limit to its application as far as the complexity of system dynamics is concerned.

The second problem of determining relative stability without recourse to an actual determination of the time response depends upon the fact that, in simple systems, the response of the system to relay switching operations may be described in terms of two algebraic equations relating three quantities: the output, its first time derivative, and time itself. The phase plane method of approach has been employed by Minorsky (reference 6 of the paper) and by Andronow and Chaikin[2] in other problems concerning nonlinear systems. Weiss and MacColl propose the application of this method to relay servomechanisms. Time is eliminated as a variable and the two response relationships result in one simple relationship relating the output and its first time derivative. These are plotted against each other, the plane of this plot being called the "phase plane." Weiss and MacColl show how the response of the servomechanism may be determined by a simple graphical construction of the "trajectory paths" in the phase plane. Dr. Kahn eliminates a different variable, the output, and obtains plots of the output's time derivative versus time itself. This method also results in a simple graphical construction which enables the relative stability of the system to be determined. When the discusser handled several typical problems by both the phase plane method and by the method proposed by Dr. Kahn it did appear that the latter entailed less computational and graphical labor. The phase plane method is definitely restricted to systems of simple dynamics; possibly Dr. Kahn's method could be extended to more involved systems although his paper does not illustrate how this might be done.

The third problem of determining conditions of self-sustained oscillations also is handled in a novel fashion. Hazen handled this problem by means of the transient response relationships. He assumed that such oscillations occurred by specifying that the responses at the beginning and end of some given period were identical and then solved for the conditions necessary to satisfy the assumed periodicity. Weiss used essentially the same approach although he employed the equations describing the phase-plane trajectory. Dr. Kahn's method for treating this problem, described in the last two sections of his paper, appears somewhat more complicated at the outset, but has the advantage that is more readily extended to systems having complicated dynamics. An interesting phenomenon encountered by both Weiss and Kahn is the existence of both convergent and divergent equilibrium operating points when the conditions leading to the self-sustained oscillations are determined. Only the convergent points correspond to true oscillations in a physical system. Dr. Kahn's Figure 7C is an example, where intersection points A and C represent convergent equilibrium while the origin and points B and D represent divergent equilibrium. This phenomenon is peculiar to nonlinear systems. Dr. Kahn's discussion of this was necessarily brief and an interested reader is referred to Andronow and Chaikin[2] for further consideration of this effect.

As the title of the paper implies, the treatment described here is chiefly intended for purposes of analysis. In the case where a system is to be synthesized and appropriate compensating networks are to be selected, the method could be applied only by performing repeated analyses with various trial values of design constants. This is not mentioned as a defect of the treatment, which is adequate within itself, but as a suggestion for further work in the field.

In the case of continuous servomechanisms, the frequency response method has proved very applicable for this purpose. This method is described in references 1, 2, and 3 of the paper. A corresponding method of treatment applied to relay servomechanisms would be of considerable aid to designers in this field.

In the past, relay servomechanisms have frequently been avoided in instances where they could have provided the most suitable answer to an engineering problem for the simple reason that they were not readily subject to analysis. It is believed that papers such as the one discussed should do much to correct this situation. Dr. Kahn is to be congratulated on his treatment and presentation of the subject.

1. THEORY OF SERVOMECHANISMS, Harold L. Hazen. *Journal*, Franklin Institute (Philadelphia, Pa.) volume 218, number 3, September 1934, pages 279–330.

2. THEORY OF OSCILLATIONS, A. A. Andronow, C. E. Chaikin. Moscow, 1937. English edition, Solomon Lefschitz, Princeton University Press, Princeton, N. J., 1949.

David A. Kahn: The writer wishes to thank Dr. Kochenburger and Mr. Ciscel and Mr. Schart for their comments. As pointed out by Dr. Kochenburger, the treatment of the relay servomechanism problem described in the paper is chiefly intended for purposes of analysis. The writer has done a small amount of investigation of the application of these methods to the problem of synthesis of compensating networks. It appears that in a large number of cases, the sign of $\eta''(t)$ serves as a useful criterion of over-all performance. Networks that change the form of $\eta(t)$ in such manner that $\eta''(t)$ becomes negative improve the system stability. However, much additional work on this problem is needed.

The Design of D-C Motors for Use in Automatic Control Systems

PAUL LEBENBAUM, JR.
MEMBER AIEE

THE increased use and accuracy of automatic control systems for industrial and military applications has made it essential that system components be reexamined in terms of over-all system operation. It is the purpose of this paper to study the application of d-c motors in these systems in order to: determine the motor constants which affect system performance; convert the important motor system constants into design factors, such as armature diameter and length, flux and copper densities, and so forth; and to determine the motor design which will give optimum over-all system performance.

Summary of Conclusions

When a separately excited shunt-wound d-c motor is used as the output member of a closed-cycle automatic control system, the motor time constant is the motor constant which the system designer uses in calculating the stability, allowable amplification, accuracy, and speed of response of the system. It is defined as the time in seconds to accelerate the motor and load inertia to 63.2 per cent of its final speed when a d-c voltage of constant magnitude is suddenly applied to the motor terminals. Optimum system design requires that this factor be as small as possible.

The motor time constant is a function of the motor armature circuit resistance, the inertia of the motor and the load, and the torque and generated voltage constants of the motor. (The armature circuit inductance and steady-state load and friction torques have been neglected in this derivation. These assumptions are discussed in the section on The D-C Motor as an Output Member of a Closed-Cycle Automatic Control System.)

$$T_o=\frac{RJ}{K_T K_V}\text{ seconds} \qquad (1)$$

When this equation is converted to design terms, it is found that the time constant is directly proportional to functions of the ratios of armature diameter to armature length, load inertia to motor inertia, and commutating field conductor copper density to armature conductor copper density; and inversely proportional to functions of the pole face flux density, and the ratios of pole pitch to pole arc, armature slot depth to armature diameter, and pole face flux density to armature tooth flux density at the tooth root. Equation 8 gives the mathematical relations between the various factors.

If these design factors and ratios are given optimum values which make the time constant a minimum, the following equation is obtained

$$(T_o)_{\min}=1.25\times10^{-3}\,\frac{K_J}{K_s}\left(1+\frac{J_L}{J_M}\right)\times\left[1+\frac{\pi}{P_M\cos\theta}+(1.2K_C)\left(\frac{B_{CCP}}{B_{CA}}\right)\left(\frac{P_c}{P_M}\right)\right]\text{ seconds at 75 degrees centigrade} \qquad (2)$$

Equation 2 shows that the minimum possible motor time constant is fundamentally a function of the armature diameter to length ratio and is not a function of the machine rating. Figure 1 is a plot of this equation for a line of 4-pole 1,750-rpm motors, ranging in size from 5 to 60 horsepower, having negligible load inertia.

A study of the equations derived in this paper reveals that the following points should be considered in designing a motor with a minimum time constant:

1. The flux density at the root of the armature tooth should be high.
2. The pole face flux density should have a value of about one-half the flux density at the root of the armature tooth.
3. The armature slot depth should have a value as close to one-eighth of the armature diameter as possible.
4. The ratio of pole arc to pole pitch should be large.
5. The armature slot activity should be high.
6. The ratio of commutating field to

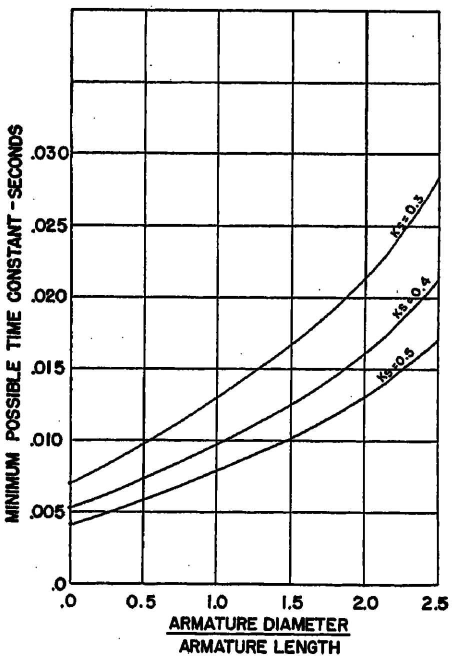


Figure 1. Minimum possible motor time constant as a function of the ratio of armature diameter to length, for a line of four pole, commutating pole, 1,750 rpm motors, ranging in size from 5 to 60 horsepower

Negligible load inertia, one commutating field circuit, commutating field to armature copper density ratio of one-half, and 1.15 compensation

---

Paper 49-225, recommended by the AIEE Rotating Machinery and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted November 17, 1948; made available for printing September 1, 1949.

PAUL LEBENBAUM, JR., is with the Motor Engineering Department of the General Electric Company, West Lynn, Mass.

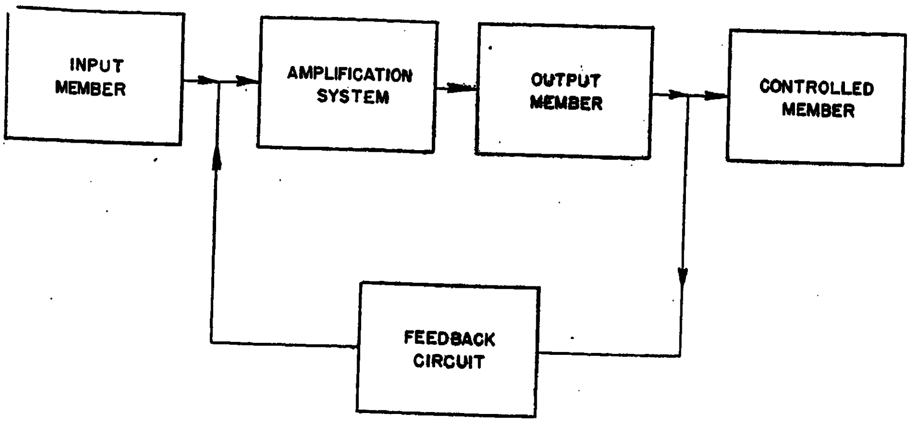


Figure 2. Components of a closed-cycle automatic control system

armature conductor copper density should be small.

7. The ratio of armature diameter to length should be small, when the load inertia is negligible. When the load inertia is not negligible, an increase in this ratio may actually give a smaller motor time constant. Under this condition, a larger value of the ratio may result in a greater decrease in armature circuit resistance than the corresponding increase in motor and load inertia.

The motor time constant is only one of several time constants found in automatic control systems. Another important time constant is the generator time constant, which is defined as the time for the generator voltage to reach 63.2 per cent of its final value when its field excitation is suddenly applied. Both motor and generator time constants enter into system performance studies.

The relative importance of the motor time constant depends on the type of motor power supply. If the motor is fed from an electronic power supply, the motor time constant is of major importance; if it is fed directly from a special d-c generator, such as an amplidyne, having a small time constant, the motor time constant has the same relative importance as that of the generator; and finally if it is fed directly from a standard d-c generator, the motor time constant is usually negligible compared to that of the generator. A more complete discussion of the conclusions is given in the section entitled Discussion of Conclusions.

Automatic Control Systems

An automatic control system is a device or combination of devices which controls the output of the system without the use of a human operator. Systems of this type are of two principal kinds: "open-cycle" and "closed-cycle". In an open-cycle system, the output is a function of an independent parameter; in a closed-cycle system, the output is a function not only of an independent parameter but also of the output itself. An example of the first type is the time-operated traffic signal control where the output is solely a function of an independent time setting. The second type can be represented by the common house heating system in which the sensitive element of a thermostat controls the amount of heat supplied, which in turn effects the indication of the thermostat in such a manner that the temperature of the house is held at an approximately constant value. The difference between open-cycle and closed-cycle automatic control is, therefore, that in the latter an indication of the output is fed back so as to modify the input to the device.

The term servomechanism is often applied to a closed-cycle system in which the output element is so actuated that the difference between the output and input indications tends to approach zero.

Common applications of closed-cycle systems are in continuous industrial processes such as steel, paper, and textile mills, where the speed of the material being processed must be accurately held; side register control, where the position of a moving material must be accurately held in a direction perpendicular to its motion; gun positioning, and target locating and tracking devices in the military field; and automatic steering devices for ships and airplanes, where the direction of the line of travel is held by comparing the actual direction with a compass setting.

In general the components of a closed-cycle system are those shown in Figure 2.

In order to demonstrate the functions of the various components, a system used to control the speed of an output shaft, shown in Figure 3, will be explained.

The input member in this case is a potentiometer supplying a constant d-c voltage input to the system. The amplifier, amplidyne, and d-c generator act as an amplification system to increase the power input from the input member and the feed-back circuit to a level sufficient

---

Nomenclature

A_A = number of armature circuits
A_{CF} = number of commutating field circuits
A_{CA} = area of armature conductor, square inches
A_{CCP} = area of commutating field conductor, square inches
B_{CA} = armature conductor copper density, amperes per square inch
B_{CCP} = commutating field conductor copper density, amperes per square inch
B_{PF} = pole face flux density, lines per square inch
B_t = armature tooth flux density at root, lines per square inch
d = armature slot depth, inches
D = armature core diameter, inches
g = gravitational constant, 32.2 feet per second squared
I_a = armature current, amperes
J = total motor armature and load inertia, referred to motor shaft speed, pound feet per second squared
J_L = load inertia, referred to motor shaft speed, pound feet per second squared
J_M = motor armature inertia, referred to motor shaft speed, pound feet per second squared
K_C = commutation compensation, per unit
K_t = iron stacking factor
K_J = motor armature inertia factor (Figure 6)
K_s = armature slot activity = $\dfrac{\text{armature slot copper area}}{\text{armature slot area}}$
K_T = motor torque constant, foot pounds per ampere
K_V = motor generated voltage constant, volts per radian per second
L = armature core length (iron only), inches
MLT_A = mean length of turn of armature conductor, inches
MLT_{CP} = mean length of turn of commutating field conductor, inches
N = number of armature slots
θ = angle between armature winding end turns and armature end punchings, degrees
ϕ_P = flux per pole, lines
P_c = number of commutating poles
P_M = number of main poles
R = total motor armature circuit resistance, ohms
R_A = armature resistance, ohms
R_{CP} = commutating field resistance, ohms
ρ = specific resistance of copper $= 0.825\times10^{-6}$ ohms per inch per square inch at 75 degrees centigrade
$\tau = \dfrac{\text{pole arc}}{\text{pole pitch}}$
T_0 = motor time constant, seconds
w = armature slot width, inches
Z = total number of armature conductors

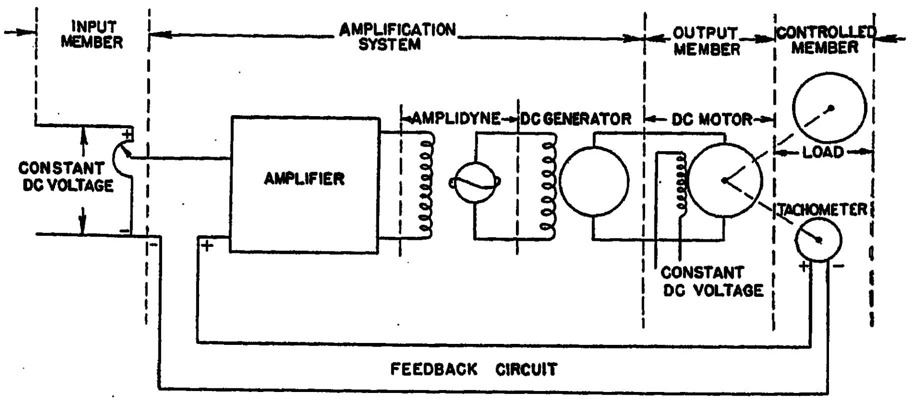


Figure 3. Closed-cycle automatic control system for controlling the speed of an output shaft

to drive the output member, which is a d-c motor. The motor in turn drives the controlled member or load, which might be the roll of a paper machine, steel mill, printing press, and so forth, whose speed is to be held constant at some preset value. The feedback, in this example, is the output voltage of a tachometer generator driven by the same shaft as the load. This voltage, proportional to the load speed, is fed back to the input of the amplifier in such a manner that the net voltage applied to the amplifier is the difference between it and the voltage supplied from the potentiometer. Thus if the speed of the load decreases, the input to the amplifier is increased, the voltage applied to the motor is increased, and the load speed increases to its normal value; similarly, if the load speed increases, the input to the amplifier is decreased, the voltage applied to the motor is decreased, and the load speed decreases to its normal value. The speed setting may be varied by an adjustment of the potentiometer of the input member.

The problem of the servomechanism designer, then, is to design a system which will be accurate and at the same time stable. In the example given of the speed control system, it is necessary that the deviation from the preset speed be as small as possible with all changes in load, and, if the speed does change, that it be returned to its preset value as rapidly as possible with a minimum of oscillation about the mean speed.

The two factors which affect the stability of the system are the amplification of the system, and the phase displacement between a signal caused by a disturbance at some point in the system and the correction signal which returns to that point through the feed-back circuit. If the phase displacement is 180 degrees, the connections of the feed-back circuit are such that the correction signal will add directly to the initial signal. If at the same time the system amplification is such that the correction signal is greater than the initial signal, then the signals will continually increase in magnitude and the system will be unstable. To correct the situation either the amplification must be reduced until the correction is less than the disturbing signal so that the oscillations will eventually die out, or the phase displacement must be decreased. Reduction in amplification will make the system less accurate. Therefore, methods of keeping the phase displacement small must be investigated.

The over-all system phase displacement is a function of the phase displacement between the input and output of each individual system component. Since the d-c motor is used as the output member of many closed-cycle control systems, the relation between its input (armature voltage) and output (shaft speed) is important in determining the performance of these systems. The slower the response of shaft speed to a change in armature voltage, the greater is the over-all system phase displacement and the greater is the tendency of the system to be unstable. The relation between input and output for a d-c motor and methods of reducing the time delay between the two quantities will now be investigated.

The D-C Motor as an Output Member of a Closed-Cycle Automatic Control System

Consider a separately-excited shunt-wound d-c motor and its load suddenly connected to a source of constant voltage, E, as shown in Figure 4.

The voltage impressed across the motor terminals is equal to the sum of the armature circuit resistance drop and the motor generated voltage, neglecting the effect of armature circuit inductance. Also, the motor electrical torque must equal the mechanical shaft torque, which is the torque required to accelerate the motor and load inertia, if steady-state load and friction torques are neglected.

The validity of the assumptions concerning armature circuit inductance and steady-state load and friction torques should be checked for each specific case. In general, the armature circuit resistance inductance time constant is small compared to the motor time constant, especially where compensated d-c motors are used. Also the accelerating torques are usually large compared to the steady-state load and friction torques. If these assumptions cannot be made, the effects which have been neglected must be taken into account in the analysis, and resulting equations will be more complex.

It also is assumed that the torque and generated voltage constants, K_T and K_V, are not changed by demagnetizing effects of the load current. D-c motors for automatic control systems are usually designed in this manner. If normal designs are not satisfactory from this standpoint, compensated designs are used.

In mathematical form, the foregoing statements become

$$E = I_aR + K_Vn \text{ volts} \tag{3}$$

$$T = J\left(\frac{dn}{dt}\right) = K_TI_a \text{ foot pounds} \tag{4}$$

where

E = applied voltage, volts
I_a = armature current, amperes
n = motor speed, radians per second
T = torque, foot pounds

Eliminating the armature current, I_a, from equations 3 and 4, gives

$$E = \frac{RJ}{K_T}\left(\frac{dn}{dt}\right) + K_Vn \text{ volts} \tag{5}$$

The solution of this differential equation for a motor starting from rest is

$$n = \frac{E}{K_V}\left(1 - \epsilon^{-\frac{K_TK_V}{RJ}t}\right) \text{radians per second} \tag{6}$$

Equation 6 is similar in form to that obtained for the current in a resistance-inductance circuit when an external voltage is suddenly applied.

$$I = \frac{E}{R}\left(1 - \epsilon^{-\frac{R}{L}t}\right) \text{ amperes} \tag{7}$$

Figure 5 represents a plot of equation 6.

The time required for the speed to reach 63.2 per cent of its final value is equal to RJ/K_TK_V, and this time is defined as the motor time constant, in a manner similar to the familiar time constant of the resistance-inductance circuit.

$$T_0 = \frac{RJ}{K_TK_V} \text{ seconds}$$

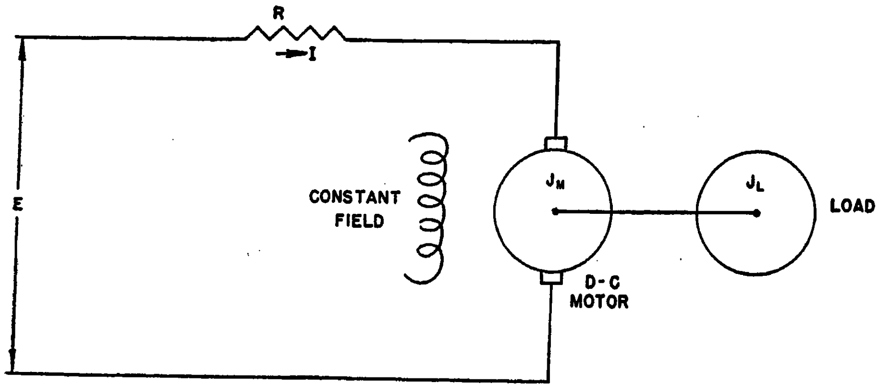


Figure 4. Diagram of a d-c motor and load supplied from constant voltage source, *E*

Thus, the smaller the motor time constant, the more rapidly does the motor speed reach its final value, and the smaller is the phase displacement between input voltage and output shaft speed.

The problem of the motor designer is to make this time constant as small as possible. The system designer will then be given a component which will not force him to lower the system amplification, and thus the system accuracy, in order to gain stability.

The Motor Time Constant in Design Terms

To design a motor for minimum time constant, it is necessary for the motor designer to convert the motor time constant equation, as defined in equation 7, into design terms, such as dimensions, flux and copper densities, and so forth. Knowing the limitations of these various factors, he can then vary them in such a manner so as to obtain a motor design having the smallest possible time constant.

The conversion is made for a commutating pole, noncompensated d-c motor in Appendix I and the resulting equation is

$$T_0=\frac{8.5\times 10^4 K_J\left(1+\frac{J_L}{J_M}\right)\times\left[1+\frac{\pi}{P_M\cos\theta}\left(\frac{D}{L}\right)+(1.2K_C)\left(\frac{B_{CCP}}{B_{CA}}\right)\left(\frac{P_C}{PM}\right)\right]}{K_S B_{PF}{}^2\tau^2\left(\frac{d}{D}\right)\left[1-2\left(\frac{d}{D}\right)-\frac{B_{PF}}{K_tB_t}\right]}$$

seconds at 75 degrees centigrade (8)

The two assumptions involved in this conversion are:

1. The armature and commutating field mean lengths of turns are

$$MLT_A=2L+\frac{2\pi D}{P_M\cos\theta} \qquad (9)$$

$$MLT_{CP}=2.4L \qquad (10)$$

Equation 10 gives an approximate value for the mean length of turn of a machine with commutating poles only. If motors having compensating windings are used, the numerical factor should be increased accordingly.

2. The motor armature inertia is given by the equation

$$J_M=6\times 10^{-6}K_JD^4L$$

K_J is an empirical constant, greater than unity, which takes into account the fact that the end windings, commutator, armature leads, shaft extensions, fans, and so forth, make the inertia of the armature greater than the value calculated from the D^4L value of the core portion alone. Figure 6 shows the variation of K_J with the ratio of armature core diameter to length for a line of 1,750-rpm 4-pole motors, ranging in size from 5 to 60 horsepower.

The two main variables which affect the value of the denominator are the pole face density, B_{PF}, and the ratio of armature slot depth to armature diameter, d/D.

The values of these variables which make the denominator a maximum, and, therefore, the time constant a minimum, are

$$B_{PF}=\frac{1}{2}K_tB_t \qquad (11)$$

$$\frac{d}{D}=\frac{1}{8} \qquad (12)$$

Optimum use of material requires that the tooth flux density be kept as high as possible. For the limiting value of this density of 140,000 lines per square inch, the pole face density will be about 66,500 lines per square inch, which is high by normal design standards, but which is theoretically possible.

With a sufficient number of armature slots the ratio of pole arc to pole pitch can be made equal to 0.7.

The substitution of these values and equations 11 and 12 into equation 7 gives a minimum value of time constant at 75 degrees centigrade of

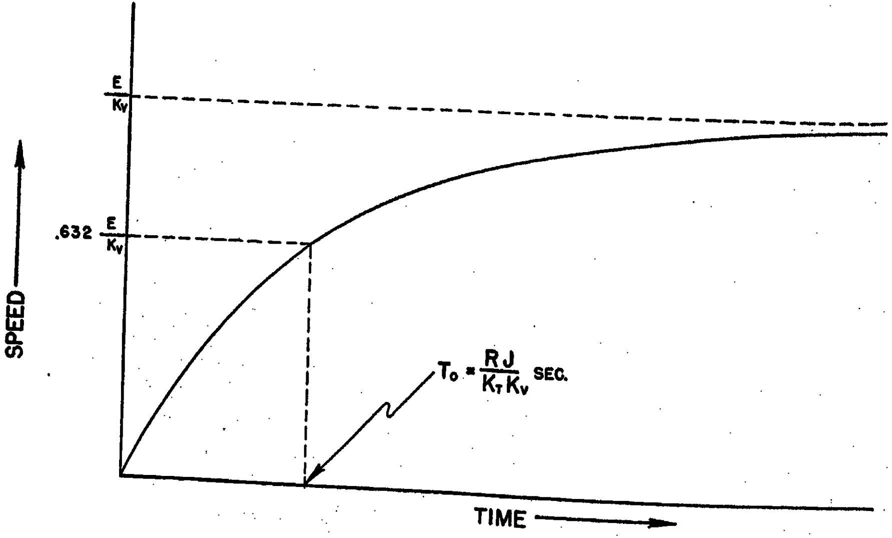
$$(T_o)_{\min}=1.25\times 10^{-3}\frac{K_J}{K_S}\left(1+\frac{J_L}{J_M}\right)\times\left[1+\frac{\pi}{P_M\cos\theta}\left(\frac{D}{L}\right)+(1.2K_C)\left(\frac{B_{CCP}}{B_{CA}}\right)\left(\frac{P_C}{P_M}\right)\right]\text{ seconds} \qquad (13)$$

Discussion of Conclusions

The results of this study were summarized previously where it was shown what factors must be considered in the design of a d-c motor in order to obtain the smallest motor time constant.

Before discussing the results and the factors which limit the minimum value of the motor time constant, it may be well to review the assumptions on which this analysis has been based. The following assumptions have been made:

Figure 5. Speed-time curve for a d-c motor and load started from a constant voltage source



1. The effects of armature circuit inductance have been neglected.

2. The steady-state load and friction torques have been considered negligible compared to accelerating torques.

3. The torque and generated voltage constants are not affected by the demagnetizing effects of load current.

The validity of these assumptions was discussed in a previous section.

An inspection of equation 13 for the minimum possible motor time constant indicates that its value is mainly dependent on the ratio of armature diameter to length. The reason for this can best be explained by studying the original motor time constant equation

$$T_o=\frac{RJ}{K_TK_V}\text{ seconds}$$

The values of the generated voltage and torque constants are fixed by the motor speed and voltage. Therefore, the motor time constant is only changed by a variation in the armature circuit resistance and total motor and load inertia. Both of these factors are functions of the ratio of armature diameter to length. As this ratio is increased (or decreased), while motor speed and voltage, pole face density, and the ratio of armature slot depth to diameter are held constant, it is found that the armature circuit resistance decreases (or increases) by approximately the same amount as the motor armature inertia increases (or decreases). Thus their product, except for secondary effects, remains constant and the motor time constant is independent of the ratio.

The secondary effects result from the presence of commutators, fans, end windings, and so forth, on the armatures. These "nonuseful" electromagnetic parts result in a more complex variation of motor armature inertia and armature circuit resistance with the ratio of armature diameter to length. The secondary effects tend to increase the motor time constant as the ratio is increased. Thus, when the motor has negligible load inertia, the ratio should be kept as small as possible.

When the load inertia is not negligible, it may be advantageous to use a larger value of the ratio. If the motor time constant equation is rewritten as

$$T_o=\frac{1}{K_TK_V}(RJ_M+RJ_L)\text{ seconds}$$

it can be seen that while the first term in the bracketed expression increases with increase in the ratio, as in the case for a motor with negligible load inertia, the second term decreases. Therefore, at some point, the motor time constant will reach a minimum value as the armature diameter-to-length ratio is increased. It should be noted, however, that an increased ratio may result in larger motors as well as larger generators, so that cost, size, and weight considerations must be weighed against motor time constant requirements.

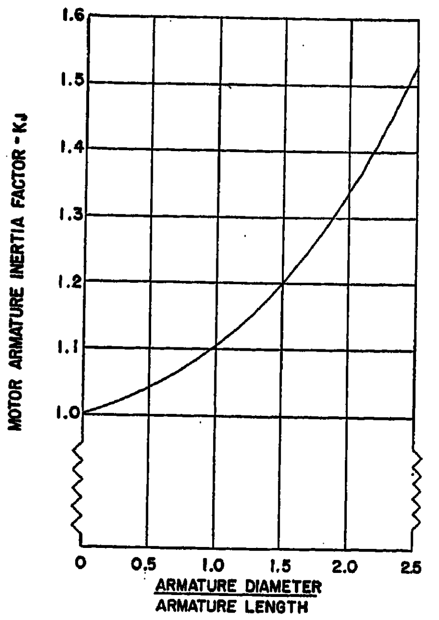


Figure 6. Motor armature inertia factor as a function of the ratio of armature diameter to length, for a line of four pole, commutating pole, 1,750 rpm motors, ranging in size from 5 to 60 horsepower

The factors which limit the minimum value of the time constant are the maximum possible values of the flux density at the root of the armature tooth, the slot activity factor, the ratio of pole arc to pole pitch, and, for larger size machines, the armature slot depth which can be used. Of these limitations, the first two depend mainly on the materials used in the machine, and the second two involve the commutating ability of the machine.

The maximum flux density which present-day magnetic materials can carry is limited to about 140,000 lines per square inch. The slot activity is a function of the insulation materials which can be used. Thinner insulation will give higher slot activities and thus lower time constants. Another decrease in time constant can be obtained from an increase in this factor by the use of comparatively low voltage machines having fewer turns per coil and requiring less insulation. However, the size of commutators, number of brushes, and size of line cables all limit the minimum value of voltage which can be used.

The third and fourth limitations involve the commutating ability of the machine. A certain minimum space between the main poles must be left for the commutating pole and thus the maximum value of the ratio of pole arc to pole pitch is fairly well fixed. Also commutation reactance voltage, which is a function of slot depth, has a certain limiting maximum value, and, therefore, limits the ratio of slot depth to armature diameter in larger size machines. For example, the slot depth in machines ranging from 30 to 180 inches in diameter may vary from 1.25 to 2.5 inches, so that the slot depth to diameter ratios for these machines are far from the optimum value of one-eighth. Hence, improvements in design, which will raise the maximum allowable commutation reactance voltage, also will lead to lower time constants.

The results of this paper can be effectively used in the design of d-c motors for closed-cycle automatic control systems, when the motor time constant is found to be an important consideration. Reductions in motor time constant of considerable magnitude over the time constants of the usual general purpose design are indicated. The main reduction is made by increasing the pole face flux density to a value equal to approximately one-half of the flux density at the root of the armature tooth. The motor designer can, therefore, give the system designer a component, when necessary, which will enable him to design systems of greater accuracy and stability.

Appendix I. Derivation of the Equation for Time Constant in Design Terms

Starting with the general expression for the time constant of a d-c motor

$$T_o=\frac{RJ}{K_TK_V}\text{ seconds} \tag{14}$$

each of the terms in this equation will be considered separately and expressed as a function of design factors.

Total Motor Armature Circuit Resistance, R

The resistance of any winding is equal to the specific resistance of copper, ρ, divided by the area of copper times the length of the winding, or

$$R_A=\frac{\rho}{A_{CA}}\left(\frac{MLT_A\times Z}{2a_A{}^2}\right)$$

$$=\frac{\rho B_{CA}(MLT_A)Z}{2I_aa_A}\text{ ohms} \tag{15}$$

$$R_{CP}=\frac{\rho}{A_{CCP}}\left(\frac{P_CK_C\times MLT_{CP}\times Z}{2P_Ma_Aa_{CF}}\right)$$

$$=\frac{\rho B_{CCP}P_CK_C(MLT_{CP})Z}{2I_aP_Ma_A}\text{ ohms} \tag{16}$$

Using the approximations

$$MLT_A = 2L + \frac{2\pi D}{P_M \cos\theta} \text{ inches} \qquad (17)$$

$$MLT_{CA} = 2.4L \text{ inches} \qquad (18)$$

and substituting these into equations 15 and 16, and adding, gives the total motor armature circuit resistance, R.

$$R = \frac{\rho L Z B_{CA}}{I_a a_A}\left[1 + \frac{\pi}{P_M \cos\theta}\left(\frac{D}{L}\right) + (1.2K_C)\left(\frac{B_{CCP}}{B_{CA}}\right)\left(\frac{P_C}{P_M}\right)\right] \text{ ohms} \qquad (19)$$

Total Motor Armature and Load Inertia

The moment of inertia of a cylinder is given by the equation

$$J = \frac{W}{g}\,\frac{D_c^2}{8} \text{ pounds feet per second squared} \qquad (20)$$

where

D_c = diameter of cylinder, inches
W = weight of cylinder, pounds

Assuming that the inertia of a motor armature is equal to some constant times the inertia of the core portion, and taking the average specific gravity of iron, copper, and insulation, as 0.28 pound per cubic inch

$$J_M = 6\times10^{-6}K_J D^4 L \text{ pound feet per second squared}$$

$$J = J_M + J_L$$

$$= 6\times10^{-6}K_J D^4 L\left(1 + \frac{J_L}{J_M}\right) \text{ pound feet per second squared} \qquad (21)$$

where J_M and J_L are motor armature and load inertias referred to the motor shaft speed.

Motor Torque Constant, K_T

The torque constant, K_T, the torque per ampere of the motor, is given by the equation

$$K_T = \frac{T}{I_a} = \frac{\phi_P Z P_M}{8.52\times10^8 a_A} \text{ foot pounds per ampere} \qquad (22)$$

The flux per pole, ϕ_P, is equal to the pole face flux density, B_{PF}, times the area of the pole face.

$$\phi_P = \left(\frac{\pi D \tau L}{P_M}\right) B_{PF} \text{ lines} \qquad (23)$$

Substituting equation 23 into 22 gives

$$K_T = 0.368\times10^{-8}\frac{B_{PF} D \tau L Z}{a_A} \text{ foot-pounds per ampere} \qquad (24)$$

Motor Generated Voltage, K_V

The generated voltage constant, K_V, is defined by the equation

$$K_V = \frac{\text{cemf}}{\text{rad/sec}} = \frac{10^{-8}}{2\pi}\times\frac{\phi_P Z P_M}{a_A} \qquad (25)$$

Substituting equation 23 into 25, gives

$$K_V = 0.5\times10^{-8}\frac{B_{PF} D \tau L Z}{a_A} \text{ volts per radian per second} \qquad (26)$$

Motor Time Constant, T_o

If the equations for R, J, K_T, and K_V (equations 19, 21, 24, and 26) and the value for the resistivity of copper, ρ, at 75 degrees centigrade are substituted into equation 14, there results

$$T_o = \frac{26.9\times10^4 K_J D^2\left(1 + \frac{J_L}{J_M}\right)\times\left[1 + \frac{\pi}{P_M \cos\theta}\left(\frac{D}{L}\right) + (1.2K_C)\left(\frac{B_{CCP}}{B_{CA}}\right)\left(\frac{P_C}{P_M}\right)\right]}{B^2{}_{PF}\tau^2\left(\frac{Z I_a}{a_A B_{CA}}\right)} \text{ seconds} \qquad (27)$$

The term $ZI_a/a_A B_{CA}$ in the denominator of equation 27 represents the total copper conductor area of the armature, since Z is the total number of conductors and $I_a/a_A B_{CA}$ is the area of each conductor. This expression also can be written as

$$\left(\frac{Z I_a}{a_A B_{CA}}\right) = K_s \times \text{total slot area} \qquad (28)$$

The "total slot area" of an armature having rectangular slots is given by the equation

$$\text{Total slot area} = Nwd \text{ square inch} \qquad (29)$$

However, the flux density at the base of the armature teeth is

$$B_t = \frac{\text{Flux per pole}}{\text{Teeth per pole}\times\text{core length}\times K_t\times\text{tooth width at bottom of slot}}$$

$$= \frac{B_{PF}\left(\frac{\tau\pi D L}{P_M}\right)}{\left(\frac{N_T}{P_M}\right)(L)(K_t)\left[\frac{\pi}{N}(D-2d) - w\right]} \qquad (30)$$

Eliminating the armature slot width, w, from equations 29 and 30 and solving for "total slot area" gives

$$\text{Total slot area} = \pi d D\left[1 - 2\left(\frac{d}{D}\right) - \frac{B_{PF}}{K_t B_t}\right]\times \text{ square inch} \qquad (31)$$

If equations 28 and 31 are substituted into equation 27, the motor time constant becomes

$$T_o = \frac{8.5\times10^4 K_J\left(1 + \frac{J_L}{J_M}\right)\times\left[1 + \frac{\pi}{P_M \cos\theta}\left(\frac{D}{L}\right) + (1.2K_C)\left(\frac{B_{CCP}}{B_{CA}}\right)\left(\frac{P_C}{P_M}\right)\right]}{K_S B^2{}_{PF}\tau^2\left(\frac{d}{D}\right)\left[1 - 2\left(\frac{d}{D}\right) - \frac{B_{PF}}{K_t B_t}\right]} \text{ seconds at 75 degrees centigrade} \qquad (32)$$

References

1. Dynamic Behavior and Design of Servomechanisms, G. S. Brown, A. C. Hall. *Transactions*, American Society of Mechanical Engineers (New York, N. Y.), volume 68, 1946, pages 503–24.

2. Theory of Servomechanisms, H. L. Hazen. *Journal*, Franklin Institute (Philadelphia, Pa.), volume 218, 1934, pages 279–331.

3. Principles of Servomechanisms (book), G. S. Brown, A. C. Hall. John Wiley and Sons, New York, N. Y., 1948.

4. Fundamental Principles of Amplidyne Applications, F. E. Crever. *Electrical Engineering* (*AIEE Transactions*), volume 62, September 1943, pages 603–06.

5. Proceedings at the Convention on Automatic Regulators and Servomechanisms. *Journal*, Institution of Electrical Engineers (London, England), volume 92, part IIA, numbers 1 and 2, 1947, pages 1–307.

No Discussion

Limitations in Design of D-C Adjustable Speed Motors

L. G. OPEL
ASSOCIATE AIEE

A d-c adjustable speed motor is one in which its speed is varied by changing the value of current in its shunt field. The speed of the motor can be varied over a wide range in this manner with a very small amount of power. It is a very popular motor and has many applications where variable speed is required, such as machine tools, reel drives, fans, and others. These motors have been built successfully with speeds adjustable over as high as an eight-to-one range.

There are many factors to consider when designing an adjustable speed motor. The same factors exist when designing any d-c machine, but usually they are well below the critical values. The four main factors which limit the design of adjustable speed motors are commutation, speed regulation, heating, and the maximum safe speed of the motor. Each factor will be discussed in this paper.

Commutation

All data given pertaining to the reactance voltage are based on values calculated by Lamme's equations[1] and have been proved in tests on actual motors.

Commutation becomes a serious problem in adjustable speed motors. The greater the speed range the more difficult commutation becomes. For a motor supplying a constant or increasing horsepower load, the maximum operating speed will be the limiting condition and the commutation factors should be calculated for this condition.

Table I gives the limiting values for the reactance voltage per coil and the reactance voltage per brush. These values are based on tests of a large number of motors and successful commutation can be obtained if they are not exceeded. These two factors should always be calculated on a new design. It is also necessary to calculate the voltage which will be obtained between commutator bars. If this voltage becomes too great, there is danger of a flashover between commutator bars and between brush arms.

An analysis of Lamme's equation for calculating the reactance voltage of the armature coils undergoing commutation indicates how this voltage varies with the machine constants and how by careful design we can minimize this voltage by proportioning the machine constants properly.

The reactance voltage is proportional to speed, current in armature conductor, total armature conductors, and the equivalent number of armature turns per coil

$$E_c = \frac{K(W_t I_c T_c R_s \pi)}{10^8}$$

For a 2-circuit winding

$$T_c = \frac{T_{ca} P}{2}$$

For a multiple-wound armature

$$T_c = T_{ca}$$

The reactance voltage can then be kept to a minimum by using as few armature turns per coil as economical. For example, a 4-pole 2-circuit winding would have double the reactance voltage for a 2-turn coil than it would have for a 1-turn armature coil. Of course, a 1-turn armature coil requires that twice the number of commutator bars be used than with a 2-turn coil. For a given diameter of commutator we are physically limited to the maximum number of commutator bars that we can use.

Increasing the armature core length or diameter permits the use of fewer armature turns for a given rating. This is equivalent to increasing the frame size.

The constant K in the equation is a flux which is the sum of the interpolar, armature end windings, and slot fluxes of the motor. Each of these factors is discussed in detail in the following paragraphs.

The interpolar flux is kept to a minimum by using the same number of commutating poles as main poles. This factor becomes zero with the same number of interpoles as main poles when the interpoles are as long as the core length. When using half the number of interpoles as main poles, the length of interpole has to be divided by two when applying the equation

$$\text{Interpolar Flux} = \frac{2PD(L-L_1)C_1}{(0.25P+0.5)(\pi D - PW_p)}$$

The armature end turn flux varies directly with the armature diameter and inversely with the number of poles. N_1 is the number of slots spanned by the coil pitch and increases as the total number of slots increase.

$$\text{Armature End Flux} = \frac{4D}{P}(0.9+0.035\,N_1)\,C_2$$

Table I. Maximum Values of Reactance Voltage Per Coil and Voltage Per Brush When Using a High Quality Electrographitic Grade of Brush*

Type of Brush	Voltage Per Coil	Voltage Per Brush
Solid brush	5	16
Double brush with a single finger	7	23
Double brush with two fingers	8	27

* Values given apply to industrial size motors ranging from 1 to 250 horsepower at 1,200 rpm.

Paper 49-226, recommended by the AIEE Rotating Machinery Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted November 16, 1948; made available for printing September 21, 1949.

L. G. Opel is with the Westinghouse Electric Corporation, Buffalo, N. Y.

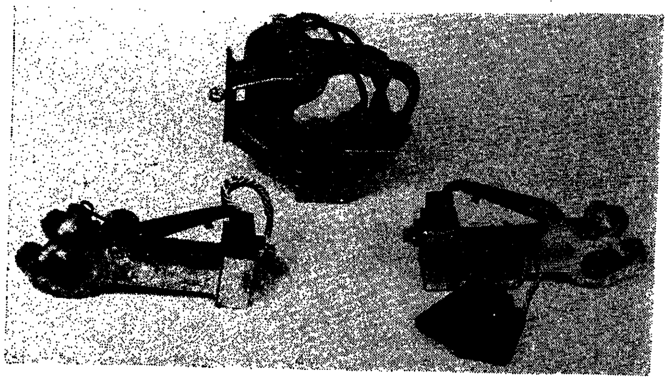


Figure 1. Left to right: a solid brush, a double brush in a double holder, and a double brush in a single holder

The slot flux is directly proportional to armature core length and is influenced by the ratio of slot depth to width. The greater the ratio, the greater the slot flux and the reactance voltage. The tooth pitch decreases with an increase in the total number of slots which reduces the slot flux.

$$\text{Slot Flux} = \frac{L}{b_s}(4/3\ d_s + 0.52 + P_t)C_3$$

For a normal design of three commutator bars per slot, a maximum slot ratio of 5/1 is desirable.

The type, grade, and thickness of the carbon brush used affects commutation. The thickness of the brush determines the number of commutator bars undergoing commutation. The reactance voltage per brush is the product of the volts per coil and the number of bars per brush. Increasing the brush thickness will then increase the reactance voltage per brush. However, increasing the thickness will also reduce the reactance voltage per coil, therefore, the increase in voltage per brush is not proportional to the brush thickness. The constant C_3 for the slot flux is influenced by brush thickness.

$$C_3 = \frac{\text{Bars per slot}}{\text{Commutating Zone in Bars}}$$

The commutating zone increases as the number of bars per brush increase.

The magnitude of the reactance voltage per armature coil and the reactance voltage per brush are two factors which are very important in determining whether the motor will commutate successfully. They are related to each other and both have to be kept within the limits as given in Table I, with the brushes being used today.

The ordinary carbon brush will commutate $2^1/_2$ volts, as a limit, per brush without any visible signs of sparking. Therefore, if we can adjust our interpole compensation to compensate for all but $2^1/_2$ volts per brush, good commutation will be obtained. If the motor were compensated to within 90 per cent of perfect compensation, we would be able to commutate 25 volts per brush.

It is not too difficult to adjust a motor to be within 25 per cent of being perfectly compensated, but it is next to impossible to adjust to 100 per cent compensation. Therefore, it becomes necessary to set up limits for the reactance voltage per brush as given in Table I.

It should be recognized that an improvement in commutation cannot always be made by merely changing the type of brush used. The commutating pole air gap may require a different adjustment, or if the current loading of the motor is increased, interpole saturation may be encountered.

The normal voltages and speed ranges over which adjustable speed motors operate are:

115- and 230-volt motors—four to one
440-volt motors—three to one
550-volt motors—two to one

Motors have been designed and built which operate over greater speed ranges. You will notice that the speed range becomes less as the voltage is increased. The reason for this is that the upper limit of the commutation factors is reached at lower speeds. For a given rating and speed the number of armature conductors, increases as the voltage increases. The voltage conversion factor, therefore, increases. For example: A 10-horsepower 230-volt 38-ampere motor has 135 bars, a 1-turn armature coil, four poles, and a 2-circuit winding.

$$E_c = \frac{k(135\times1\times2\times19\times2\times rpm)}{10^8 \qquad 60}$$

A 10-horsepower, 460-volt motor would have a current of 19 amperes and 135 commutator bars, a 2-turn armature coil, four poles and a 2-circuit winding. Its factor would be

$$E_c = \frac{k(135\times2\times2\times9.5\times4\times rpm)}{10^8 \qquad 60}$$

This motor has twice the reactance voltage of the 230-volt motor for the same speed.

Therefore, its speed range will be one-half that of the 230-volt motor. The speed range of the higher voltage motors can be increased by increasing the size per rating. Increasing its armature dimensions decreases the number of armature conductors required for the same speed and therefore increases the speed at which the motor can be operated and still commutate successfully.

Another factor to be considered is the voltage between commutator bars. This voltage has to be kept below certain values in order to build a motor which will not flash over under normal operating conditions. The limit of the flashover voltage is influenced by the resistance of the armature. High resistance armature windings permit greater voltages to be used between commutator bars. The thickness of the mica insulation between bars limits the maximum voltage per bar permitted. A factor of 1.10 volts per mil thickness of mica is used to determine the permissible value of voltage that should be used between commutator bars. The following values are for 25-mil thick mica and motor sizes ranging from 1 to 250 horsepower at 1,200 rpm:

1. The average voltage between bars $= (EP)/B$ maximum $= 15$ volts.
2. The maximum voltage under the

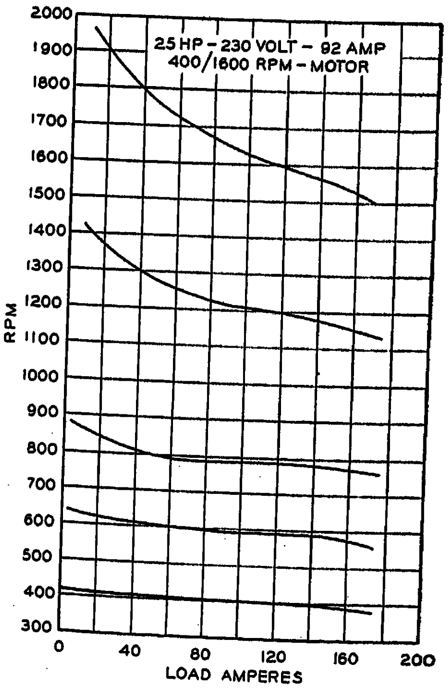


Figure 2. Typical speed regulation curves of adjustable speed motors

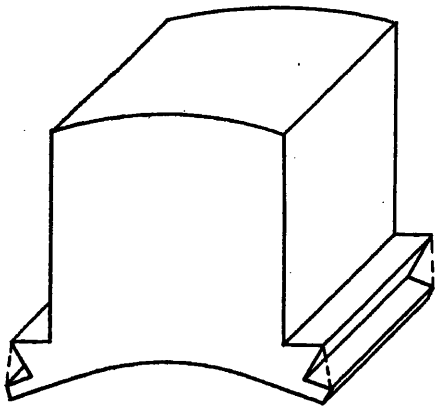


Figure 3. A type of pole tip notch

poles at no load = $(EP)/(Bk)$ maximum = 23 volts.

$$k=\frac{\text{Pole Circumference }(P)}{\text{Armature Circumference}}\cong 0.65$$

3. The maximum voltage under load = $(EP)/(Bk)\ \phi_1/\phi_2$, maximum equal to 35 volts per bar.

Where

ϕ_1 = flux at value of ampere turns equal to the ampere turns required to give ϕ_2 plus the armature distortion ampere turns
ϕ_2 = flux at maximum operating rpm

Speed Regulation

The speed regulation of standard adjustable speed motors varies over the operating speed ranges. At low speeds the regulation is low, and at top speeds the regulation becomes high. See Figure 2 for typical regulation curves.

The armature distortion effects the regulation as it does not reduce the flux the same amount at all values of operating fluxes. Because of the reduced flux under load, it becomes necessary to add a series field to the motor to compensate for the reduced flux in order to obtain a drooping speed characteristic with load. Since the saturation curve is not linear over its range of operation, the flux changes and speed changes with load and will be very high at high speeds.

The speed regulation can be improved by special notching of the field poles as shown in Figure 3. The pole tip notch causes the main pole tip to become saturated at low values of ampere turns on the main poles. Since the pole tip is saturated, the flux through it will not change appreciably as the armature distortion ampere turns add and subtract from the main field ampere turns. Therefore, there will not be as much reduction in the flux under load as there was before the pole tips were notched, and the motor will require less series field ampere turns to stabilize it. Less series ampere turns means that the motor will have a smaller variation in speed between no load and full load at the high speeds.

By designing machines with compensating windings (or pole face windings) the speed regulation of adjustable speed motors can be made more uniform over their range of operation. This is accomplished because the armature distortion effect is greatly reduced when compensating windings are used. This also reduces the maximum voltage between commutator bars and the danger of flashovers. Compensating windings are not used very frequently on the smaller size motors because of the high cost involved.

Other methods involving special windings and circuits on the main field poles have been devised which give improved speed regulation and some improvement in commutation. One such method is used on the eight-to-one speed range motor.[2]

Maximum Safe Speed

The maximum safe speed the motor armature can safely operate at and not fail mechanically has to be considered in the design. The excessive speed regulation at weak fields, or high speeds at no-load, determine the maximum speed the armature has to be made safe for. This means special mechanical designs for very high speed motors.

Heating

Since the adjustable speed motor operates over a wide range in speed, the amount of ventilating air through the motor will vary as the speed changes. At the low speeds very little air circulation is obtained relative to that which is obtained at the higher speeds. Figure 4 shows how the watts dissipated from an armature rotating in free air varies with its peripheral velocity. Note that it is a straight line variation until a critical velocity A is reached, after which it increases very slowly.

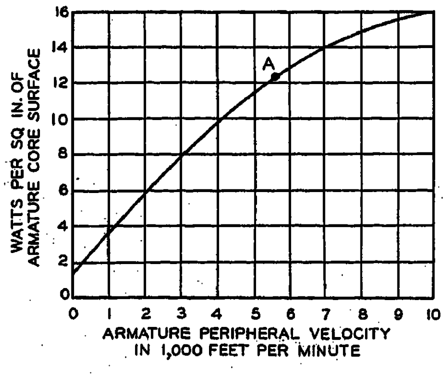


Figure 4. A typical armature heat transfer curve

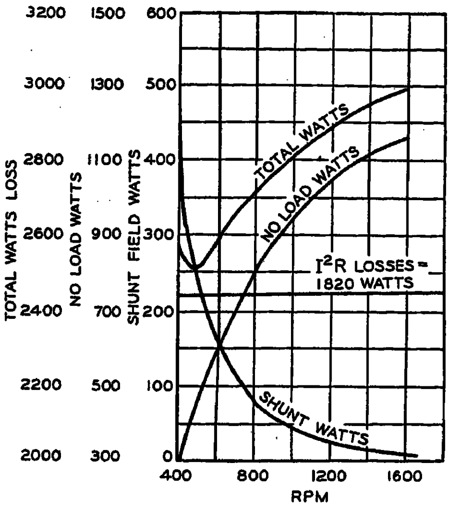


Figure 5. Variation in watts loss with rpm of a 25-horsepower 230-volt 400/1,600-rpm motor

The losses of the motor do not vary proportionally to the speed. Figure 5 shows how the losses of a constant horsepower motor vary with the speed. The losses at the low speed are even greater than the losses at intermediate speeds.

A comparison of the losses against the amount of air circulation obtained at various speeds clearly indicates that the motor will have the highest temperature rise at the lower speeds. If the motor were designed to meet its temperature rise at its lowest speed, it would be larger than necessary to meet the temperature rise at the higher speeds. The fact must not be overlooked that it is possible to operate the motor at a sufficiently high speed so that the increase in losses will be greater than the increase in the ventilation. The motor will then have a greater rise at these high speeds than at some lower speed.

It is recognized that in most adjustable speed motor applications, the motor will not operate at its base speed continually. A standard has been set up so that the motor will be designed to meet its specified temperature rise at 150 per cent of its base speed. It is felt that this condition is a good average of the operating range of the motor.

Nomenclature

E_c = commutating volts
W_t = total conductors on armature

I_c = load amperes flowing in armature conductor
T_c = equivalent turns per armature coil
T_{ca} = actual turns per armature coil
R_s = revolutions per second
P = number of main poles
D = armature diameter in inches
L = armature core length in inches
L_1 = effective interpole length in inches
W_p = width of main pole body in inches
N_1 = slots spanned by one coil pitch
b_s = width of slot in inches
d_s = depth of slot in inches
P_t = tooth pitch in inches
C_1, C_2, C_3 = constants
k = constant
B = number of commutator bars
E = induced armature voltage
K = constant
ϕ = flux

References

1. THEORY OF COMMUTATION AND ITS APPLICATION TO COMMUTATING POLE MACHINES, B. G. Lamme. Westinghouse Electric Corporation, East Pittsburgh, Pa., 1919.
2. DUAL FIELDS IN D-C MOTOR PROVIDE SIMPLE 8-TO-1 SPEED CONTROL, R. W. Moore. *Product Engineering* (New York, N. Y.) volume XVIII, number 8, 1947, pages 166–69.

No Discussion

Use of Cumulative and Differential Series Fields in the Parallel Operation of D-C Motors

A. W. KIMBALL
ASSOCIATE AIEE

D-C MOTORS are used in parallel on a large number of applications. In some of these, the division of load is of relatively minor importance. However, in others it is essential if the best performance is to be obtained.

An example of this is the sectional printing press used in printing newspapers. In this case, the press sections and motors are so designed that the total inertia load and horsepower rating of the motors are balanced. The press then can be accelerated or decelerated at the maximum rate with a resulting minimum loss of time for change of rolls of paper, and so forth. Also, one or more sections can be dropped from operation without affecting the performance of the press as a whole. However, the torque produced by each motor must match that of each of the others within close limits throughout the complete cycle of operation in order that best performance can be obtained without overstressing any part of the press or tearing the paper. The use of cumulative and differential series fields accomplishes this with simplicity and a minimum of control elements.

Motors with One Series Field

The manner in which two or more motors, when operated from the same power source and connected to the same load, will divide it between them is determined by their regulation curves. Figure 1(A) shows such a system using normal standard motors. Assume the series fields have been set to give 10 per cent regulation in each case and the shunt field rheostats have been set so that all units carry 100 per cent load at 100 per cent speed. The regulation curves then will be as shown by the solid line curves in Figure 1(B). This system then is set up for parallel operation and it will function with good load division until some change takes place to upset the adjustment.

To take an extreme case, suppose that motors A and B have been operating continuously and are up to temperature, but motor C has been idle and is therefore cold. In addition to this, assume that the effect of this temperature difference is such that its speed load curve is lowered 10 per cent. This is shown by the dash curve for this motor. Now if additional pages are to be added to the paper and section C with its motor connected into the press and electric system, the loads will no longer be balanced. The press will operate at 97 per cent speed, and motors A and B will carry $1^1/_3$ load each, while motor C will carry only $^1/_3$ load. This is quite an unsatisfactory condition of operation, as it would unduly stress parts of the press and would probably result in tearing the paper during the accelerating cycle.

Figure 1(C) illustrates the performance which would be obtained if, instead of adjusting the series field for 10 per cent regulation no load to full load, the design and adjustment had been such as to obtain 60 per cent speed droop. In this case, if section and motor C were added to the system cold, then the load division would be much better. Motors A and B would each carry 105.5 per cent load and motor C would carry 89 per cent load. The press again would operate at 97 per cent speed. This would be good parallel operation. However, a regulation of 60 per cent is impossible to obtain if the motors are to be operated over a wide speed range by field control. This range is usually 4-to-1. Regulations of approximately 5 per cent at full field and 15 to 20 per cent at weak field are obtainable in shunt wound motors and result in good maintenance of speed with changes in load.

Motors with Two Series Fields

The problem then becomes one of obtaining heavily compounded motor performance for parallel operation while preserving the shunt wound motor characteristic for over-all performance. This is obtained by using two series fields in each motor. If one series turn per pole is required to give the desired shunt motor

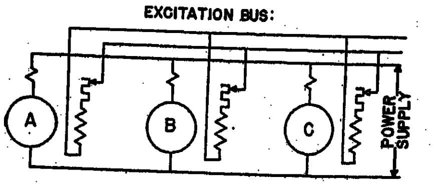


FIGURE I (a) CONNECTION DIAGRAM

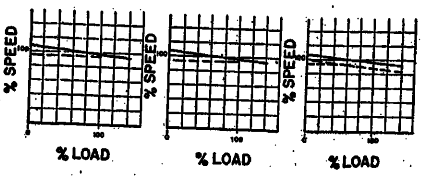


FIGURE I (b) REGULATION CURVES WITH 10% DROOP

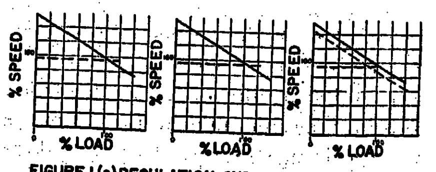


FIGURE I (c) REGULATION CURVES WITH 60% DROOP.

Figure 1. System with only cumulative series

Paper 49-227, recommended by the AIEE Rotating Machinery Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted November 16, 1948; made available for printing September 22, 1949.

A. W. KIMBALL is with the Westinghouse Electric Corporation, Buffalo, N. Y.

characteristic and four series turns required to give the compound motor characteristic, then the motor is built having a cumulative series of four turns per pole and a differential series of three turns per pole. Figure 2(A) shows the connection diagram for such a system. The differential series coils are connected between the equalizer bus and one line and adjusted by means of series resistances so that they divide current between them in proportion to the rated current of their respective motors. The shunt field rheostats are then adjusted so that the motors divide the load in proportion to their respective ratings.

Any variation in the current division between the differential series fields is compensated by the adjustment of the shunt field rheostat. It, therefore, of course, influences the matching of the characteristics of the motors, but division within ±5 per cent is generally satisfactory and not particularly difficult to obtain. Since the voltage drops are low, it is sometimes found desirable to solder all connections to prevent change.

The system then will operate as a combined unit as though each motor had a regulation curve as shown by the solid lines in Figure 2(B). However, the motors will divide the load (assuming operation at 100 per cent load) as though each had a regulation curve as shown by the dash lines. This is true because, for a given operating condition, the power required to drive the press is practically constant. Therefore, current drawn from the power source also is practically constant. The differential series fields will divide this current between them in proportion to the rating of their respective motors. This was a condition of the adjustment and since we have assumed 100 per cent load operation, this will be rated current in each case.

The equalizer connection isolates the differential series portion of the circuit from that of the motor armatures and cumulative series fields and permits the current to divide in this part of the circuit according to the resulting regulation curve for each unit. Since the differential series field is constant, the combined effect of it and the shunt field is such that, if the motor were uncoupled, it would operate at 160 per cent of full load speed.

However, at full load current the cumulative series is such that the field is strengthened and the motor will operate at 100 per cent speed. The motor armatures will, therefore, divide the load in accordance with the regulation curves shown by the dash lines. If the power requirements are reduced so that the total load is only 50 per cent of the sum of the ratings of all the motors, then the resulting regulation curves for load division will be as shown by the dot-dash lines.

The control required for this system is quite simple. Usually a resistance-type starter is provided for each motor to bring it up to base speed. The steps of these starters are so matched that the same voltage is applied to each motor on corresponding steps. The step contactors are then operated from the master control at the same time. This permits acceleration of any group or combination of motors as desired by simply opening or closing a given motor circuit. The control of the motors from base speed to maximum speed is simplified by exciting the shunt fields from a variable voltage bus. This range then can be covered by varying the field excitation of the exciter supplying the bus.

This design has been used for systems having as many as six motors connected to the same load and operating in parallel. Figure 3 shows the test curves of a motor of this type having $3^1/_2$ turns cumulative and $2^1/_2$ turns differential series per pole. These motors operate consistently and divide the load from 40 to 100 per cent load over a 4-to-1 speed range within 10 per cent of rated current. This system also has been used for drives having mixed horsepower rating. Care must be used, in such cases, to match the saturation curves, regulation curves with and without the differential series, and the shunt field design. When this is done, satisfactory performance is obtained.

Designs having cumulative and differential series fields also have been used in the parallel operation of generators with very good results. They can be made with under-, flat-, or over-compounded machines with equally good results. The only undesirable features of the system are the necessity of an equalizer, and that the adjustment of the differential series field circuit be made accurately and in such fashion that it will not change appreciably with time.

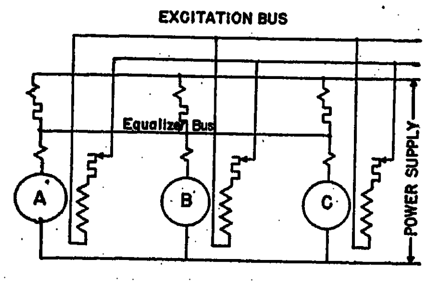


FIGURE 2(a) CONNECTION DIAGRAM

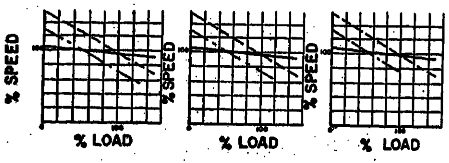


FIGURE 2(b) REGULATION CURVES

Figure 2 (left). System with cumulative and differential series fields

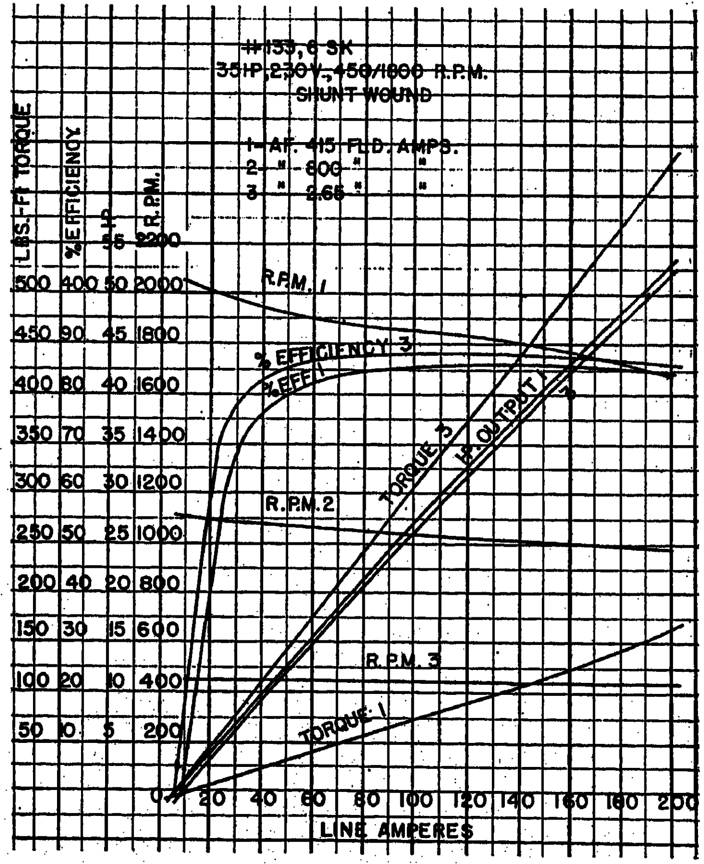


Figure 3 (right). Test regulation curves with both series fields excited by line current

Conclusions

Motors built with this type of winding provide shunt motor regulation characteristics over a wide speed range with good division of load. This permits sectionalizing of mill or press drives with the resulting simplification of design. It also permits operation at full efficiency when sections of the mill or press are not required for the particular operation being performed.

No Discussion

Transient Characteristics of D-C Motors and Generators

A. T. McCLINTON
MEMBER AIEE

E. L. BRANCATO
MEMBER AIEE

ROBERT PANOFF
ASSOCIATE AIEE

SINCE the first installation of electric power on board the *USS Trenton* in 1883, the United States Navy has always been a major user of d-c power. After the introduction of 440-volt alternating current into the Navy with installations on the Farragut-class destroyers in 1932 there was still a continued use of a sizable amount of direct current. All vessels having a-c systems required some direct current for powering excitation supplies, searchlights, battery chargers, and interior communications. Although alternating current had become the accepted standard, smaller vessels such as landing craft and patrol craft retained d-c systems in the interest of simplicity. Other vessels, such as large tenders, repair ships, tugs and submarines had relatively large d-c power plants installed for electric propulsion purposes.

Thus the Navy, having extensively used direct current for ship's service and propulsion power plants for over a long period of years, has always been vitally interested in the question of transient performance of d-c rotating machinery and the effect of the transients on system operation. Of special interest has been that phase of system study work which deals with fault current analysis and the application of protective devices to the power plant. In the early days of small capacity plants, fault current analysis was not of major importance since the available amount of short-circuit current in the system was relatively small and most of the protective devices, which had been primarily designed for shore station use, had more than adequate interrupting capacity to meet the application requirements. Thus accurate knowledge of fault currents that could be obtained was inconsequential. As the electric loads on Naval vessels started to increase at a great rate shortly before World War II; with the resultant increase in generating capacity and available fault currents, system study work received a new emphasis. Unfortunately, the mathematical tools available to the a-c system designer, which give him the ability to calculate such items as subtransient reactances from the geometry of the machines, are not readily available to the d-c system designer. The engineers concerned with d-c system work had to satisfy themselves with rule-of-thumb methods for calculating available short-circuit currents in the system and used excessively large safety factors in their protective devices to offset the probable inaccuracy in their system study work. In most cases, however, this worked out entirely satisfactorily because of the fact that high-interrupting capacity protective equipment was being applied to low-capacity power systems. Where test data could be obtained on the machinery to be used, a more thorough and accurate system study design could be made. Generally speaking, however, very little test data were available to the system designer.

In connection with the design of a naval vessel having a large d-c propulsion system, the Navy initiated the development of completely new design rotating machinery. Machines of new voltage ranges, speeds, and ratings were designed. Associated with this machinery development program was the design of new fuses, circuit breakers, and contactors. Thus the application engineer was faced with the problem of setting up design and performance specifications for this new protective equipment without accurate knowledge of the conditions under which the equipment might have to operate. In the case of shipboard d-c systems where freedom from commutator flashover and reliable circuit breaker operation are extremely important, consideration of the performance of d-c machinery under fault conditions is extremely significant. This is especially true in the larger capacity systems, where excessive fault currents cause commutator flashover and extremely low-fault currents make the attainment of circuit breaker selectivity extremely difficult. In order to successfully set up design specifications for protective devices that will operate reliably with the rotating machinery, the application engineer should have the following information:

1. The maximum short-circuit current of motors and generators in the system.
2. The rate of rise and decrement of the short-circuit current of the motors and generators.

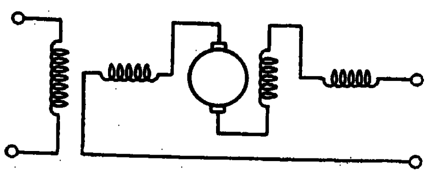


Figure 1(A). Schematic diagram of d-c machine

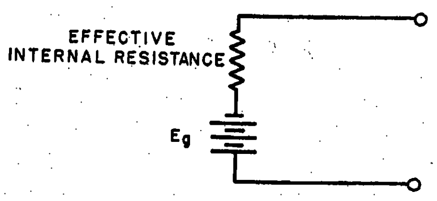


Figure 1(B). Equivalent representation of d-c machine

Paper 49-229, recommended by the AIEE Rotating Machinery Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 19, 1949; made available for printing September 1, 1949.

A. T. McCLINTON and E. L. BRANCATO are with the Naval Research Laboratory, Washington, D. C. ROBERT PANOFF is with the Bureau of Ships, Navy Department, Washington, D. C.

The authors acknowledge the contributions of R. E. Whipple, J. P. O'Connor, and J. G. Allard for the computations and experimental work performed to make this paper possible.

3. Information relative to the flashover characteristics of the rotating machinery.

4. Information relative to the effect of changes in voltage, speed, and machine loading on the short-circuit characteristics of the machinery.

In attempting to set up design specifications for this particular power plant which would result in the smallest possible size of protective equipment, an investigation was made into the various methods of calculating the transient performance of d-c machinery. Some theoretical treatises on this subject were available; however, most of these were conflicting in theory and results, and generally all were too unwieldy to be of use to the application engineer. In this instance where the machine design work was paralleling the protective device development, the system engineer was hampered by the fact that there was not a thoroughly reliable mathematical tool available with which he could calculate the transient performance of the rotating equipment. It was immediately apparent that if the Navy was to continue using d-c systems, and in some cases this could not be avoided, some means must be established for the accurate calculation of the transient performance of d-c machinery from the geometry of the machines concerned.

After a complete review of the various technical papers on this subject and many discussions with manufacturers of Navy d-c rotating machinery, three technical papers,[1,2,3] were investigated for application to shipboard system analysis. Using certain methods prescribed in these papers for the d-c calculations, various sizes of this newly designed rotating machinery were calculated to determine the peak short-circuit current. In all cases, the results obtained appeared excessive.

Table I. Short-Circuit Constants of Machines for Operation at Normal Full Load Ratings

Machine	Rated Voltage, Volts	Rated Speed, Rpm	Rated Current, Amperes	Current Rise Amps/Sec. $\times 10^6$	Effective Internal Resistance, Ohms	Zero Resistance Short-Circuit Current, Per Unit, Calculated
Generator	415	750	2,650	2.0	0.019	8.5
Generator	345	1,200	870	1.43	0.0355	12.0
Motor	415	1,300	2,600	2.5	0.021	7.5
Motor	415	280	2,650	2.5	0.032	5.0
Motor	250	1,750	298	0.2	0.085	10.0
Motor	250	550	176	0.04	0.170	8.5
Motor	250	1,600	73	0.04	0.360	9.5

Since these machines had not as yet been built, there were no means available for checking these calculations. Accordingly, it was deemed necessary at that time that the theoretical considerations set forth in the foregoing papers should be checked with test data on Navy-type equipment. In order that enough test data could be obtained to completely evaluate the theoretical considerations, a mock ship's system was set up, complete with propulsion motors, propulsion generators, and auxiliary motors in various horsepower ranges. Instrumentation was installed in this system for a twofold purpose:

1. To measure the peak short-circuit current and rate of rise and decrement of short-circuit current under various conditions of loading, speed, voltage, and external fault resistance.

2. Instrumentation to determine the internal phenomena under fault conditions. This involved measuring the change in flux in various portions of the machine.

It was anticipated that the first phase of this test program would result in data which could easily be checked against values obtained by calculation. The second phase of the program regarding the internal phenomena of the machines is a longer range program, the results of which are expected to simplify present theory or evolve new theory.

The purpose of the following discussion is to demonstrate the scope of these tests and their relationship to the theoretical considerations of the problem. The data described herein merely covers the highlights of the test work performed and is presented to the extent that it assists in an understanding of the factors involved in the transient characteristics of d-c machines.

Equivalent Circuit

The selection of a suitable equivalent circuit for adequate representation of the

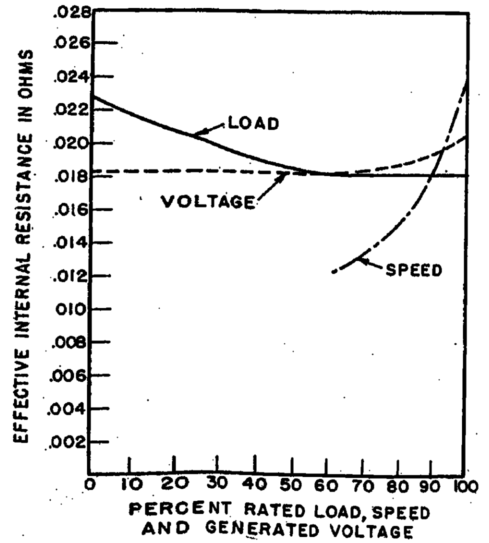


Figure 2. Effect of initial operating conditions on effective internal resistance, 1,100-kw 415-volt 750-rpm generator

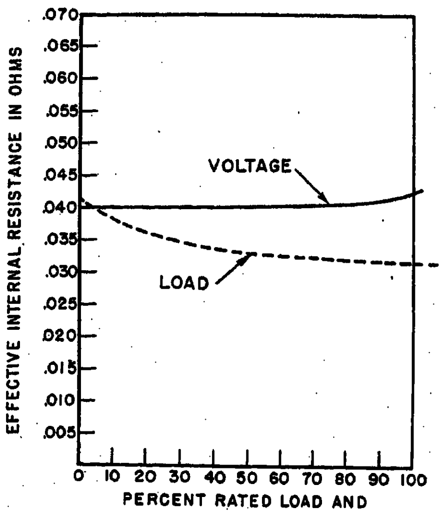


Figure 3. Effect of initial operating conditions on effective internal resistance, 300-kw 345-volt 1,200-rpm generator

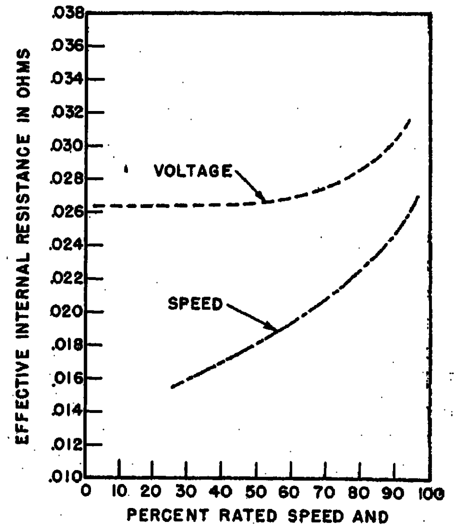


Figure 4. Effect of initial operating conditions on effective internal resistance, 1,375-horsepower 415-volt 280-rpm motor

d-c machine presented schematically in Figure 1 (A) will depend upon the application of the circuit in later studies. One form of simplified representation is found in Figure 1 (B). The effective internal resistance represents all factors tending to restrict the flow of current from the generator. These factors are armature circuit ohmic resistance, brush contact drop, reactance voltage, and flux reduction and distortion. The voltage E_g represents the generated voltage prior to short circuit which is assumed to remain constant during the short circuit whereas the flux reduction and distortion are treated as ohmic resistance.[1]

It should be noted that this circuit is applicable only for the computation of peak values of short-circuit current for individual machines or for the total value of several in a system. Reasonable accuracy can be expected in a system calculation if the time to reach peak current is essentially equal for all machines on the system and if the effective internal resistance is accurately known. Where the transient of the current is desired, a more nearly complete representation of the machines with an equivalent circuit will be required. Such a circuit would require resistance and inductances to represent the armature and field circuits. However, the required accuracy of calculations would have to justify such elaborate computations as this would imply.

Table I presents values of effective internal resistance and generated voltage that have been found for several machines by laboratory tests. The values shown in this table are for operation at rated conditions prior to application of the fault. Discussion to follow will indicate the effect of such factors as speed, load, and voltage on these equivalent values.

Table II. Generator—1,100 Kw, 415 Volts, 2,650 Amperes, 750 Rpm

Volts Generated	Load	Speed	r_e	r_d'	$I_{\text{Calc.}}$	I_{Test}	Percentage
428.8	2,300	715	0.2407	0.3557	2.91	3.00	94.2
427.3	2,000	715	0.01450	0.13085	7.86	7.02	112.0
415	0	715	0.01213	0.12667	7.89	7.74	102.0
314.1	2,600	715	0.01448	0.12657	5.97	5.70	104.7
214.2	2,400	715	0.01453	0.12407	4.15	3.92	105.6
116.9	850	619	0.0152	0.1126	2.50	2.41	103.7
116.7	800	428	0.0150	0.0936	3.00	3.02	99.5
206.6	1,200	715	0.0142	0.1258	3.96	3.77	105.0
204.8	600	715	0.0148	0.1241	3.98	3.47	104.5

Table III. Generator—300 Kw, 345 Volts, 870 Amperes, 1,200 Rpm

Volts Generated	Load	Speed	r_e	r_d'	$I_{\text{Calc.}}$	I_{Test}	Percentage
270	0	1,200	0.02172	0.21261	4.88	5.07	96.4
283	1,045	1,200	0.02172	0.21241	5.125	6.71	76.4
345	0	1,200	0.01238	0.1315	7.6	8.45	90.0
365	1,028	1,200	0.01238	0.1325	7.99	10.52	76.0

Table IV. Motor—1,375 Horsepower, 415 Volts, 2,600 Amperes, 1,300 Rpm

Volts Generated	Load	Speed	r_e	r_d'	$I_{\text{Calc.}}$	I_{Test}	Percentage
209	1,900	950	0.01235	0.09753	5.17	6.30	82.1
205	1,700	1,240	0.01229	0.11409	4.34	5.70	76.2
134	1,800	718	0.01450	0.0696	4.63	4.71	98.3
133	1,720	418	0.01430	0.0714	4.52	4.97	91.1
122	1,600	718	0.01203	0.06714	4.36	5.70	76.5
122	1,800	450	0.01197	0.06026	4.87	5.20	93.6
230*	2,470	1,300	0.01660	0.1221	4.53	3.65	124.2
247*	100	1,300	0.01490	0.1205	4.95	4.88	101.4
256	1,725	1,300	0.01415	0.1055	5.16	5.64	91.6

* Motor action during pre-short condition.

Table V. Motor—1,375 Horsepower, 415 Volts, 2,650 Amperes, 280 Rpm

Volts Generated	Load	Speed	r_e	r_d'	$I_{\text{Calc.}}$	I_{Test}	Percentage
394	2,600	270	1.065	1.2101	0.785	0.679	115.5
390	2,700	267	0.0231	0.1672	5.62	4.15	135.4
279	2,400	267	0.02304	0.16277	4.13	3.43	120.3
173	2,400	267	0.02375	0.15918	2.62	2.15	121.8
148	2,450	91	0.02546	0.11984	1.82	1.25	145.5
88	2,650	92	0.02529	0.10307	2.06	1.64	125.5
253*	1,745	280	0.02323	0.1659	3.68	4.0	92.0
247.4	100	280	0.0249	0.16738	3.56	3.23	110.0
237	2,300	280	0.02874	0.1709	3.34	2.15	155.0

* Generator action prior to short circuit.

Effective Internal Resistance Variation

The experimentally obtained transient resistances are plotted in Figures 2 through 4 against the parameters of load, speed, and voltage. Referring to Figures 2 and 3, it is noted that the magnitude of load prior to short circuit has a sizable influence on the resulting peak short-circuit currents or effective internal resistance, and that the peak current increases and resistance decreases with increasing load. The reason for this behavior is not evident, although it is suspected that higher resistance in the field circuit at lower loads may result in a faster deterioration of flux linkages which may augment the reduction in air gap flux beyond that anticipated from the increase of leakage flux during transient loading. A comparison of the percentage decrease in internal resistance between a lightly differentially compounded machine of Figure 2 and a more heavily differentially compounded machine of Figure 3 tends to verify the inference, namely the percentage increase in field circuit resistance, between full load and no load, and in the transient field current, due to transformer action of the armature circuit during a short circuit, is smaller for a lightly than a heavily differentially compounded machine. Thus the change in the rate of destruction of flux linkages for a change of load from full load is percentagewise higher in the heavily differentially compounded machine. By this token, it is expected that the magnitude of short-circuit currents from a cumulatively compounded machine would not be unduly influenced by the initial load.

The speed also influences the machine effective resistance. The speed characteristics were obtained for the two large machines and are graphed on Figures 2 and 4. The effect of speed change is consistent for all units, that is, the internal resistance rises and the peak current correspondingly decreases with increasing speed. This trend is expected for the speed proportionally affects the

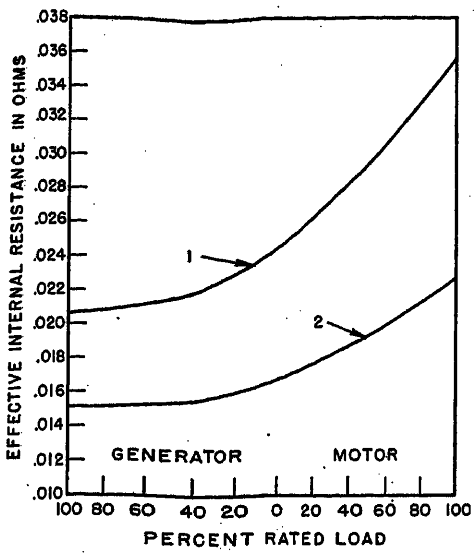


Figure 5. Effect of change from motor to generator action as initial condition on effective internal resistance

(1) 1,375-horsepower 415-volt 1,300-rpm motor

(2) 1,375-horsepower 415-volt 280-rpm motor

reactance voltage which, appearing as an equivalent brush drop, contributes to the magnitude of machine effective resistance.

If the machine effective resistance were constant, then the peak current would increase with the generated voltage. However, it is observed in Figures 2, 3, and 4, that this resistance is constant up to 50 per cent voltage but above this voltage the machine resistance rises. It is inferred that this effect is due to an increase in leakage flux owing to increased saturation of the pole iron. In addition, there is an accompanying increase in reactance voltage since the crowding of the flux in the pole tips increases the steepness of the flux distribution curve at the neutral point and results in a decrease in commutation time.

The behavior of a machine, relative to short-circuit contribution, when operating as a motor or as a generator, is of practical interest. Two motors were operated, each as generator and as motor, at similar load, voltage, and speed conditions. Referring to Figure 5, it is noted that the peak short-circuit current is less for motor action than for generator action; and that this difference in current decreases as the initial load is decreased.

Peak Short-Circuit Current

In an attempt to verify the validity of existing theory relating to short-circuit characteristics of d-c machines, calculations of peak short-circuit currents were made for the fault conditions to which these machines have been subjected. The method of calculation is outlined in Appendix I. The results of these computations are embodied in Tables II through V. These tables outline the pertinent conditions and resistances and compare the calculated values to the actual test results. A review of this comparison convinces one that the relations employed in Appendix I merit further improvements.

These improvements may result only from the acquisition of a more fundamental knowledge of the machine internal transient phenomena. Hence this work is being continued along the theoretical approach with this objective in mind. The factors of internal and external ohmic resistance, decay in air gap flux, and the development of a reactance voltage opposing the generated electromotive forces are considered to be all the variables that enter into the calculation of short-circuit currents. However, the weighting of these effects in the proportions occurring in a d-c machine may not be accurately expressed by the existing relations.

This observation is illustrated by a set of experimental characteristics on the 1,100-kw generator which are plotted in Figure 6 and compared with a corresponding set of computed characteristics. These curves attempt to demonstrate graphically the relation of peak short-circuit currents developed with variations of operating conditions of load, speed, and generated voltage.

Focusing the attention on the curves showing the effect of load, it is observed that the peak short-circuit current is a function of preshort-circuit current while the calculated values are entirely independent of initial load. There is no factor in the equations which recognizes the effect of load, yet a change in load from zero to 50 per cent raises the peak current by approximately 20 per cent, a factor that cannot be neglected even in an approximation.

Referring to the effect of voltage, it will be recalled that the effective resistance of a machine is independent of generated voltage for voltages below 50 per cent and rises appreciably beyond it. The equations involve the voltage only in the flux reduction factors, r_x', for uncompensated or partially compensated machines. Although the increase in the effective resistance of this machine, from half to full voltage, is 30 per cent, the calculated rise in value for r_x' over the same region is only 10 per cent. As r_x' is one of the three factors embraced by the effective internal resistance, r_a', while the other two are independent of voltage, it is evident that the effective resistance is inadequately influenced by the change in the calculated r_x' with voltage.

In comparing the experimental to the calculated curves showing the effect of speed, it is observed that the calculated values describe a nearly straight line relation; the experimental values, although following the same trends, indicate a sharper decrease in peak current with speed, at the larger magnitudes of speed. The calculated effect of speed is apparently insufficiently descriptive of the physical behavior of the machine. The speed enters in the calculations of the reactance voltage factor, r_b', and the flux reduction factor r_x', as a linear function, except at very low speeds where the correction factor for distribution of current in the brush face becomes appreciable when calculating r_b'.

Transient Armature Current

A second consideration, and of equal importance with computation of peak short-circuit current, is the prediction of rate of rise and decrement of the transient current. The accuracy with which the rate of rise can be predicted has been demonstrated by test and calculation to be as accurate as the computation of armature inductance for conditions prior to the short circuit. Curves 1 and 6 of Figures 7 and 8, and curves 1 and 5 of

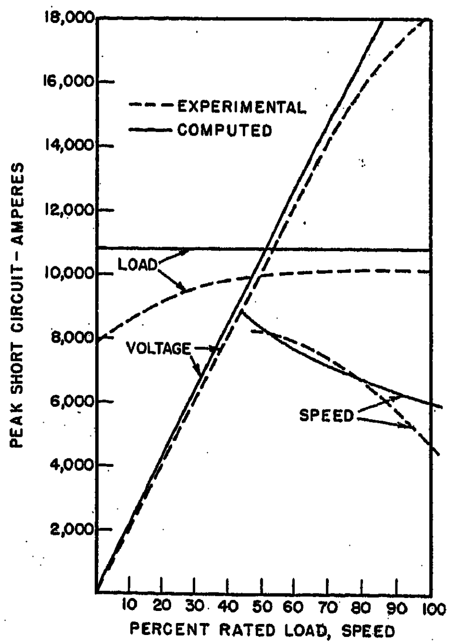


Figure 6. Comparison of computed and experimental data on peak short-circuit current

1,100-kw 415-volt 750-rpm generator

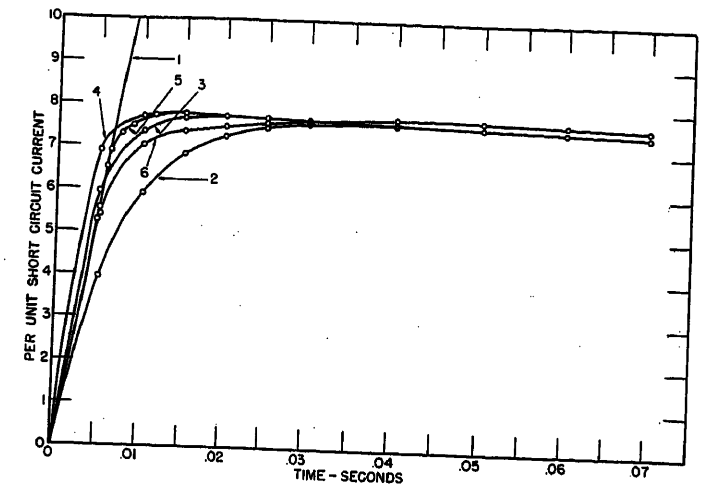


Figure 7. Computed transient short-circuit current, 1,100-kw 415-volt 750-rpm generator

(1) Computed maximum rate of rise—2.95×10^6 amperes per second
(2) Computed curve for σ_a
(3) Computed curve for $2\sigma_a$
(4) Computed curve for $3\sigma_a$
(5) Curve $3\sigma_a$ shifted to 0.67 curve (1)
(6) Curve for actual oscillographic test. Maximum rate of rise—4.18×10^6 amperes per second

Figures 9 and 10 indicate the agreement between computed and experimental data.

The calculation of the complete transient build-up is difficult to achieve due to the effect of saturation, change of flux linkages, and reduction in generated voltage during the current rise. It has been proposed that the assumption should be made that the time constant of the armature circuit be decreased by two-thirds as the armature current reaches its peak values.[1] Experimental evidence indicates that this represents too great a decrease in inductance. Results presented in Figures 7 and 8 show that for these machines the inductance decreased approximately 50 per cent as indicated by curve 3, whereas data shown in Figures 9 and 10 indicate that the oscillographic trace can be more nearly duplicated with a curve representing a fixed value of armature inductance. However, such an assumption is basically incorrect since the rate of rise at any point on the curve is influenced by the voltage as well as the inductance at the corresponding interval. Thus a procedure which recognizes only an inductance change is incomplete.

The remaining portion of the transient, that is the decay of current after peak value, is associated with the shunt field circuit and its time constant. Accurate measurement of this time constant is difficult to achieve because of the necessity of duplicating the saturation of the armature and pole faces and distortion of field flux that accompany the high-armature currents under short-circuit conditions. Therefore the shunt field inductance was computed on the basis of complete saturation of the armature teeth. This reduces the inductance to approximately one-tenth the base value at no load. Although the results of the

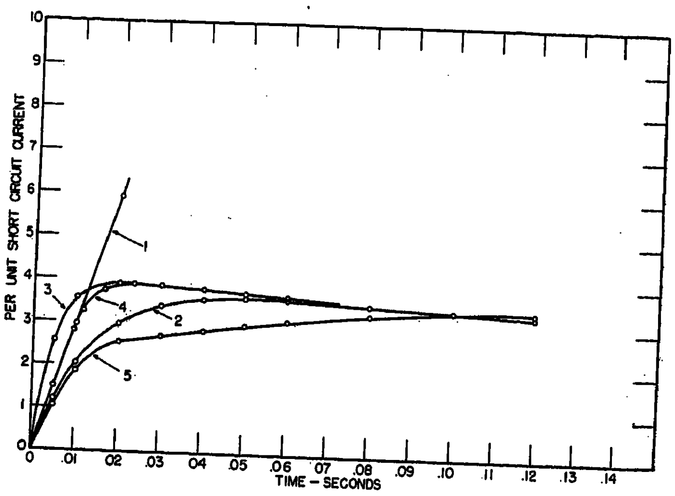


Figure 9. Computed transient short-circuit current, 1,375-horsepower 415-volt 280-rpm motor

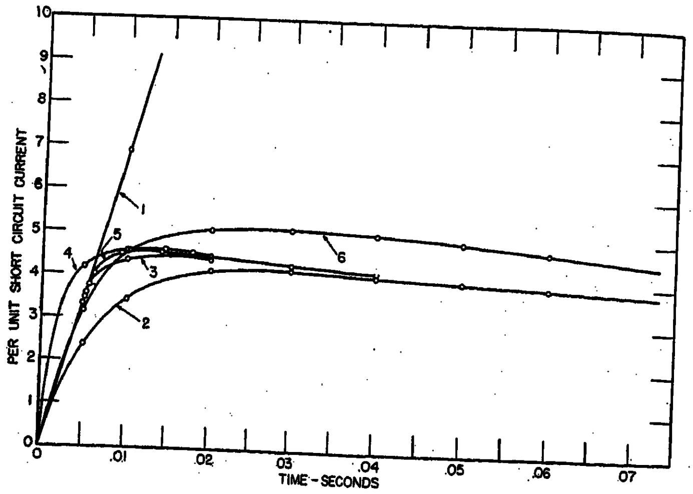
(1) Computed maximum rate of rise $=0.79\times10^6$ amperes per second
(2) Computed curve for σ_a
(3) Computed curve for $3\sigma_a$
(4) Curve $3\sigma_a$ shifted to 0.67 curve (1)
(5) Curve for actual oscillographic test—2 rates of rise noted. First $=1.14\times10^6$ amperes per second. Second $=0.53\times10^6$ amperes per second

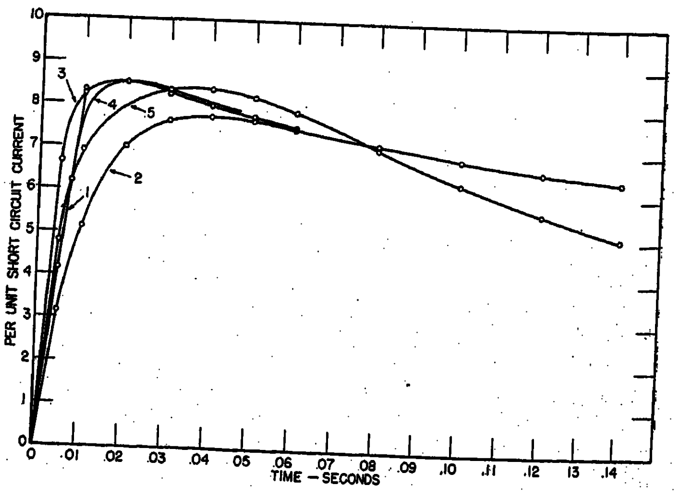
Figure 8. Computed transient short-circuit current, 300-kw 345-volt 1,200-rpm generator

(1) Computed maximum rate of rise $=0.792\times10^6$ amperes per second
(2) Computed curve for σ_a
(3) Computed curve for $2\sigma_a$
(4) Computed curve for $3\sigma_a$
(5) Curve $3\sigma_a$ shifted to 0.67 curve (1)
(6) Curve for actual oscillographic test, maximum rate of rise $=0.80\times10^6$ amperes per second

Figure 10. Computed transient short-circuit current, 90-horsepower 250-volt 1,750-rpm motor

(1) Computed maximum rate of rise $=0.248\times10^6$ amperes per second
(2) Computed curve for σ_a
(3) Computed curve for $3\sigma_a$
(4) Curve $3\sigma_a$ shifted to 0.67 curve (1)
(5) Curve for actual oscillographic test, maximum rate of rise $=0.29\times10^6$ amperes per second





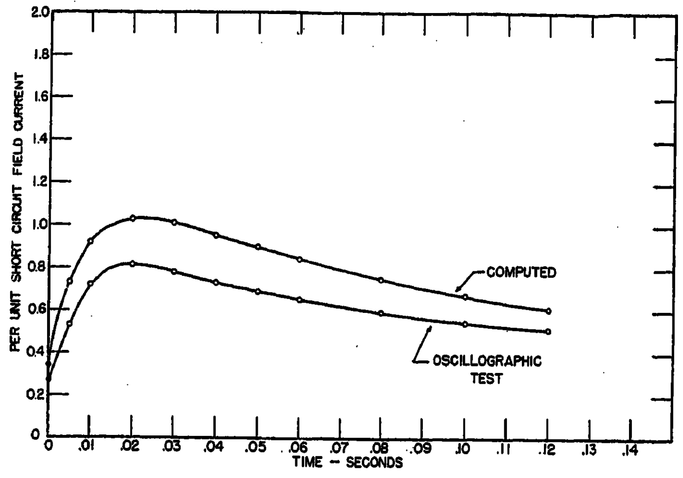


Figure 11. Computed transient field current, 1,100-kw 415-volt 750-rpm generator

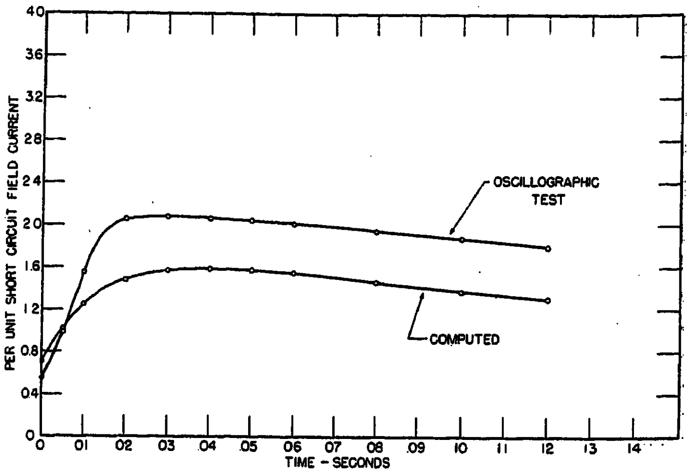


Figure 13. Computed transient field current, 1,375-horsepower 415-volt 280-rpm motor

approximations are fairly close to test data, there are additional factors such as mutual inductance and compensation which must be considered. It is important that accurate predictions of the shunt field inductance be possible since it can materially influence the peak value of short-circuit current as evidenced from Figure 10. The computed value of peak current without giving consideration to this decrement is 9.27 per unit, whereas this becomes 7.74 per unit when the decay of the shunt field is considered. Thus it becomes evident that the influence of the shunt field on the peak fault current cannot be ignored.

Transient Shunt Field Current

Computations of the shunt field current traces compare favorably with observed data for rate of rise and decrement. However, considerable difference is observed for the peak value as can be seen in Figures 11 through 14.

An examination of equation 8, in Appendix I, for transient field current will show that an approximation of the change in current from steady state will be

$$i_f \text{ (transient)} = \frac{r_x'}{r_a'} \frac{e_0}{n_0 n_f} \text{ per unit} \qquad (1)$$

Where e_0 is the per unit armature voltage before short circuit, n_0 is the per unit speed, n_f is the flux leakage coefficient, r_x' is a per unit value of resistance which indicates the effect of fault currents on field flux producing the generated electromotive force, and r_a' represents the total transient resistance. Thus equation 1 shows that the per unit increase in field current is a function of the reduction in net air gap flux.

Consideration of the term r_a' used in computing the curves of Figures 11 through 14 shows that it is in error by no more than 20 per cent, whereas the oscillograph trace of field current is approximately 230 per cent of the computed value. This leads to the observation that the possible flux change is greater than that predicted by r_x'. As has been noted previously, proper application of the constant flux linkage theorem may increase this term to a larger value. A second factor that may require consideration is the coupling between the series and shunt field. This is not adequately covered by equation 1.

Conclusions

In view of the difference between experimental and computed values of short-circuit currents using present techniques, it is necessary that the theory of transient characteristics of d-c machines be extended to provide the accuracy necessary for attaining the optimum in shipboard system design. Although the factors contributing to the limitations of short-circuit current, namely, armature and external circuit ohmic resistance, brush contact drop, reactance voltage and flux distortion and reduction have all been considered, the application of these in calculating transient response from machine constants appears to be inadequate. The analysis of the transient response must include those factors that

Figure 12. Computed transient field current, 300-kw 345-volt 1,200-rpm generator

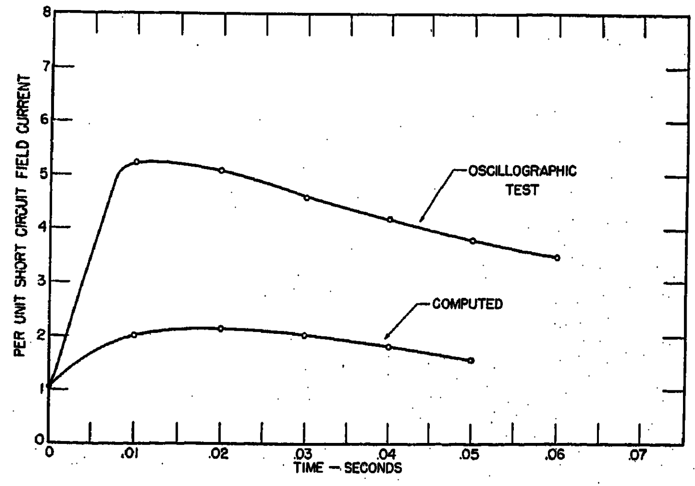
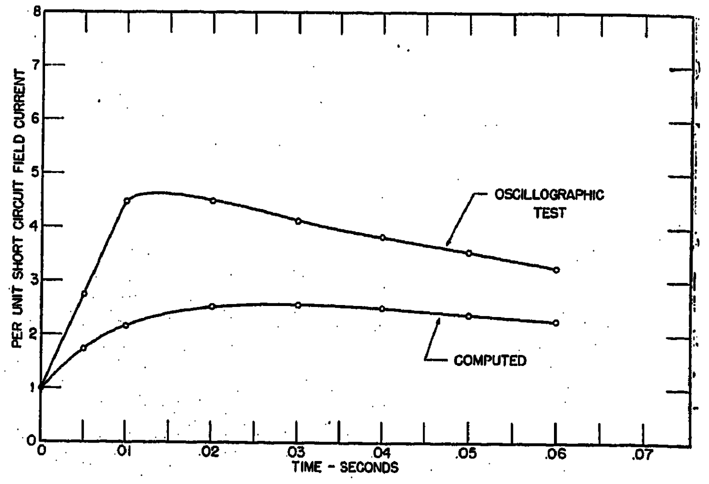


Figure 14. Computed transient field current, 90-horsepower 250-volt 1,750-rpm motor



will show the variation in initial load and voltage, establish a more nearly complete analysis to illustrate the magnitudes of flux change and distortion, and to possibly take cognizance of the fact that the generated voltage is a variable.

The large number of factors that enter into the transient response of a d-c machine under short-circuit conditions is very significant. As a result, the development of suitable relationships for armature and field current appears to be difficult. Additional work is being done to secure an accurate measure of the internal phenomena that are associated with this condition. At the same time results of the study are being correlated with system performance studies to assure completion of a theoretical approach that will be applicable to this over-all problem of system analysis under transient conditions.

Appendix I. Equations for Short-Circuit Calculations

The relations which were derived by Linville[1] and employed in making the calculations of transient current are as follows.

Peak short-circuit current

$$I_a'=\frac{e_o}{r_d'} \tag{1}$$

Effective internal resistance

$$r_d'=r_w+r_b'+r_x' \tag{2}$$

When external circuit resistance is considered, it is added to r_d'.

Reactance voltage and brush drop factor

$$r_b'=n_oE_rb\left(\frac{\epsilon^x+1}{\epsilon^x-1}\right)+V_c \tag{3}$$

where

$$x=\frac{n_oE_rb}{V_c}$$

Flux reduction factor

(A) for fully compensated machines

$$r_x'=\frac{K_3n_fn_o}{1+n_f} \tag{4}$$

(B) for partially or uncompensated machines

$$r_x'=\frac{10n_fe_o+n_f(K_1+10K_2)(10K_3n_o-e_o)}{100(n_f+K_2)+10K_1} \tag{5}$$

Transient current

$$i_a=\frac{e_o}{r_d}-\frac{e_o}{r_d'}\epsilon^{-\sigma at}+e_o\frac{r_d-r_d'}{r_dr_d'}\epsilon^{-\sigma_f t} \tag{6}$$

Maximum rate of rise

$$\frac{di_a}{dt}=\frac{e_o\sigma_a}{r_d'} \tag{7}$$

The transient field current is

$$i_f=\frac{e_o}{n_o}\left[1+\frac{r_x'}{r_d'n_f}(\epsilon^{-\sigma_f t}-\epsilon^{-\sigma at})\right. \tag{8}$$

Steady-state effective resistance

$$r_d=r_w+r_b+r_x \tag{9}$$

When external circuit resistance is considered, it is added to r_d.

For uncompensated machine

$$r_x=\frac{10e_o+n_o\left(10K_3-\frac{e_o}{n_o}\right)(K_1+10K_2)}{100} \tag{10A}$$

For compensated machine

$$r_x=\frac{e_o+n_o\left(10K_3-\frac{e_o}{n_o}\right)}{10} \tag{10B}$$

where

$$K_1=\frac{P_p\phi_t}{P_aM_{al}C_c}$$

$$K_2=\frac{1.25gK_t}{g+d_s}$$

$$K_3=\frac{(b\pm 2b_s)PM_{al}}{B}\pm C_sM_s$$

Use $+2b_s$ for brush shift in direction of rotation and $+C_sM_s$ for differential field.

Partial compensation factor

$$C_c=1-\frac{P_p}{P_a}\,\frac{M_{bl}}{M_{al}}$$

$$r_b=E_r(1-E_c)b\left(\frac{\epsilon^x+1}{\epsilon^x-1}\right)+V_c \tag{11}$$

where

$$x=\frac{E_r(1-E_c)b}{V_c}$$

Decrement factors

$$\sigma_a=\frac{r_d'}{L_a} \tag{12}$$

$$\sigma_f=\frac{r_f}{L_f} \tag{13}$$

Nomenclature

b = brush overlap in segments
b_s = brush shift from polar axis in segments
B = total number of segments in commutator
d_s = depth of armature slot in inches
E_c = estimated ratio (under sustained short-circuit conditions) of actual interpole field strength to field strength if no saturation occurred (generalized value = 0.3)
e_o = armature electromotive force before short circuit in per unit
E_r = average per-unit reactance voltage per coil at rated full load current
g = average main pole air gap in inches
i_a = instantaneous short-circuit current in per unit
i_f = instantaneous short-circuit field current in per unit
I_a' = maximum short-circuit current in per unit (based on rated current)
K_t = air gap plus tooth ampere turns divided by air gap ampere turns at rated voltage, base rated speed, no load
L_f = inductance of field circuit divided by (R_1)
M_{al} = armature reaction magnetomotive force at full rated load in per unit
M_{fl} = base ampere turns = field ampere turns per pole at rated voltage, rated base speed, no load
n_f = ratio total field flux linkages minus linkages from flux responsible for armature electromotive force to linkages from flux responsible for armature electromotive force
n_o = speed at which machine is short circuited in per unit
p_a = pole arc in inches
p_b = pole pitch in inches
P = number of poles
r_b = steady-state resistance equivalent to reactance voltage and brush contact in per unit
r_b' = transient resistance equivalent to reactance voltage and brush contact in per unit
r_d = steady-state effective total resistance of machine under short circuit in per unit
r_d' = transient effective total resistance of machine under short circuit in per unit
r_e = external resistance in per unit
r_f = resistance of field circuit in per unit
r_x = steady-state resistance equivalent to flux reduction in per unit
r_x' = transient resistance equivalent to flux reduction in per unit
r_w = total resistance of windings in armature circuit in per unit
R_1 = base ohms = V_1/I_{al}
V_1 = base volts = rated voltage in volts
V_c = normal brush contact drop at full load in per unit
ϕ_1 = base flux = flux per pole in maxwells at rated voltage, rated base speed, no load
ϕ_t = iron flux per pole in per unit = average section of armature tooth in square inch times number of teeth in pole arc by 123,000 lines per square inch divided by ϕ_1
σ_a = armature circuit decrement factor
σ_f = field circuit decrement factor
C_s = series field coupling factor
M_s = series field ampere turns, in per unit
M_{bl} = compensating winding ampere turns on pole face, in per unit

References

1. SOLID SHORT CIRCUIT OF DIRECT CURRENT MOTORS AND GENERATORS, T. M. Linville, H. C. Ward, Jr. *AIEE Transactions*, volume 68, part I, 1949, pages 119–24.

2. CURRENT AND TORQUE OF D-C MACHINES ON SHORT CIRCUIT, T. M. Linville. *AIEE Transactions*, volume 65, 1946, pages 956–65.

3. THE SHORT-CIRCUIT CHARACTERISTICS OF D-C GENERATORS, G. E. Frost. *Electrical Engineering* (*AIEE Transactions*), volume 65, June 1946, pages 394–402.

No Discussion

The Eccentric Pole Face—A Means to Better D-C Machine Performance

M. A. BAKER
MEMBER AIEE

NO ONE factor, calculation, or design constant will tell the designer whether or not his design will commutate satisfactorily. Many factors must be considered simultaneously, and in the end judgment and experience must be used in interpreting the calculations and predicting commutation. It is beyond the scope of this paper to attempt an analysis of all factors affecting commutation, but some of the more common design guides will be discussed briefly. The primary purpose of this paper is to show how the flaring main pole gap improves the commutation and performance of d-c motors and generators. While this is not new, recent studies of eccentric pole face design may be of interest to d-c designers. While much of the following discussion applies equally well to all d-c motors and generators, the flaring air gap is of special importance on adjustable speed motor designs.

Commutation Guides

Predicting commutation is not an exact science, and designers have certain empirical formulas and rules which they follow. A few of the factors that should be considered in predicting commutation are as follows:

1. Reactance voltage.
2. Commutating pole dimensions.
3. Neutral or clearance zone.
4. Magnetic densities.
5. Volts per bar.

Brushes and brushholders are not discussed here; good brushes and holders that give good riding qualities are taken for granted in this paper.

Reactance voltage often is used as a commutation index on d-c machines. Many formulas are available for calculating this, and each designer has one that he follows. Experience tells him what the maximum practical values are. On noncommutating pole machines this is probably the most important commutation guide. However, reactance voltage does not, in itself, cause sparking on machines equipped with commutating poles. It is the uncompensated reactance voltage, or the difference between reactance voltage and the voltage induced by the commutating poles, that causes sparking. If reactance voltage could be perfectly compensated for, it would not be a limiting factor of commutation. Since there is a practical limit in the accuracy to which we can neutralize reactance voltage, it is one of the standard yardsticks by which designers predict commutation. However, as progress in the art continues and we learn how to better compensate for reactance voltage, we should be able to increase the limit.

The design of the commutating pole is, of course, of utmost importance in the commutation of a d-c machine. The proper tip width must be used to adequately cover the commutating zone, and the correct air gap must be used to give the correct flux pattern. Pole body width must be great enough to avoid saturation at all anticipated loads and the proper amount of brass shims must be used to take up any excess over-compensation.

The coils undergoing commutation lie in a small stationary zone midway between the main pole tips frequently referred to as the commutating zone, see Figure 1. The width of this zone is a function of brush overlap and many designers have rules of thumb which they follow that limit the brush overlap to certain maximum values. However, it has been the author's experience that this is of little importance provided the proper commutating pole tip and gap are used and sufficient clearance, or neutral zone, is provided between the edge of the commutating zone and the main pole tip. Experience indicates that one slot pitch margin should be provided between each side of the commutating zone and the main pole tip. This is of great importance in the design of adjustable speed motors, but satisfactory performance of constant speed motors and generators often can be obtained with slightly less clearance.

Magnetic densities in the magnet frame and armature core may affect the commutation since these parts carry both main and commutating pole flux. In adjustable speed motors it is of prime importance to keep the densities in these two parts below the knee of the saturation curve in order to obtain satisfactory commutation at both base and top speeds. On constant speed motors or constant voltage generators, some saturation in the magnet frame and armature core parts can be accommodated by increasing the strength of the commutating field.

If there is any one factor that all d-c designers calculate on their designs, it is probably volts per bar, in some form or other, and most designers have rules which they follow to a certain degree. The average volts per bar of a given design gives little insight as to its commutating ability. It is of importance only in that the maximum volts per bar, volts per bar at the pole tip, and so forth, are proportional to it. Experience has shown that the average volts per bar should be limited to approximately 20 in normal d-c machines in the medium and large sizes.

The maximum volts per bar is of importance because flashover will occur if

---

Paper 49-230, recommended by the AIEE Rotating Machinery Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted March 21, 1949; made available for printing September 1, 1949.

M. A. Baker is with the General Electric Company, Fort Wayne, Ind.

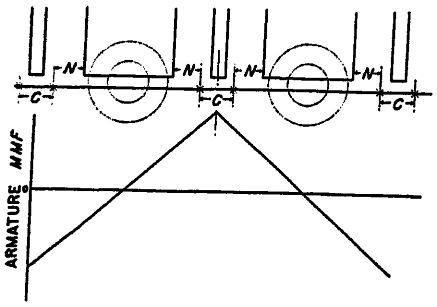


Figure 1. Armature flux

C=commutating zone
N=neutral or clearance zone

this becomes too great. Experience has shown that flashover is liable to occur if the maximum volts per bar exceeds approximately 45 on large machines with 30–40 mil mica. However, on medium-sized and smaller machines this value can often be exceeded without danger of flashover. The maximum volts per bar under load will occur at the main pole tip on uncompensated machines with concentric or uniform air gaps. If the pole face is chamfered or flared the maximum volts per bar will occur under the pole face, see Figure 4.

The author's experience on adjustable speed motor designs leads him to believe that one of the most important factors in the commutation of these machines is the volts per bar at the leading edge of the main pole tip (trailing edge of a generator). This factor is a measure of the flux concentration at the pole tip near the commutating zone and if this is too great, leakage flux from the main pole will fringe into the commutating zone causing poor commutation and speed instability. Experience has shown that if the volts per bar at the pole tip is limited to approximately 30, and one slot pitch margin is provided between the main pole tip from the commutating zone, good commutation and speed regulation can be obtained. Because of the importance of tip volts per bar on adjustable speed motors, a study of several pole arc shapes has been made to determine their effect on this important factor.

Calculation of Flux Distribution at Weak Field

When load is applied to a d-c machine, the armature sets up a stationary magnetic field that is a maximum underneath the commutating pole and zero under the center of the main pole. Since the armature magnetomotive force is of opposite polarity under each half of the main pole, a circulating flux is set up in the main pole face as shown in Figure 1. This adds to the magnetic densities at one half of the pole and subtracts on the other. In case of motors the flux density is increased on the leading half and reduced on the trailing half. In a generator the converse is true. The armature magnetomotive force is superimposed on the main field excitation, and under weak field conditions, such as top speed on an adjustable speed motor, the flux under part of the pole may be reversed.

Since the voltage induced in the armature coils is directly proportional to the flux, at constant speed, the induced volts per bar under the main pole arc gives a direct measurement of the flux distribution under the main pole. The average volts per bar is calculated from the following simple equation

$$V_{b_{\text{avg}}}=\frac{(V)(P)}{S} \tag{1}$$

where

$V_{b_{\text{avg}}}$=average volts per bar
V=rated voltage
P=number of poles
S=number of commutator segments

However, since voltage is induced only in the active conductors under the main pole arc (plus fringe area), the average volts per bar under the main pole is appreciably higher than the average volts per bar calculated by equation 1. The average volts per bar under the main pole arc can be readily calculated from the following equation

$$V_{b_p}=\frac{(V)(P)}{(S)(\tau)}K \tag{2}$$

where

V_{b_p}=average volts per bar under main pole arc
τ=per cent pole arc=pole arc divided by pole pitch
K=fringing factor=flux entering armature $\dfrac{\text{directly under main pole arc}}{\text{total armature flux per pole}}$

Under weak field condition, saturation can be neglected and the flux can be assumed to be directly proportional to the magnetomotive force and inversely proportional to the air gap. Thus the volts per bar at any point under the pole arc can be expressed as follows

$$V_b=\frac{(V)(P)}{(S)(\tau)}K\frac{G}{g}\,\frac{(NI+a)}{NI}$$
$$=\left[\frac{(V)(P)(K)(G)}{(S)\,(\tau)(NI)}\right]\left[\frac{NI+a}{g}\right] \tag{3}$$

where

V_b=volts per bar at point under consideration
G=average air gap
g=air gap at point under consideration
NI=air gap ampere turns
a=armature reaction at point under consideration.

To calculate the volt per bar curve across the main pole face, it is necessary to either calculate the air gap at several points or scale it off an accurate layout. The armature reaction at any point can be calculated readily since it is uniformly distributed and is zero under the center of the main pole and a maximum midway between the main pole tips. A simple equation for this calculation is

$$a=\frac{(A)(2)(d)}{PP} \tag{4}$$

where

A=maximum armature reaction in ampere turns per pole
d=distance of point in question from center of main pole arc
PP=pole pitch

If it is desired to know the volts per bar at the main pole tip without calculating the entire curve, this can be done from the following equation.

$$V_{b_t}=\left[\frac{(V)(P)(K)(G)}{(S)\,(\tau)(G_t)}\right]\left[\frac{NI+(\tau)(A)}{NI}\right] \tag{5}$$

where

V_{b_t}=volts per bar at main pole tip
G_t=air gap at main pole tip (other terms defined previously)

To check the validity of the foregoing equations, several volts per bar tests were made on a 30/40-horsepower 400/1,600 rpm 230-volt shunt-wound-motor operating at maximum speed. The test procedure used consisted of the following. Small holes were carefully punched ex-

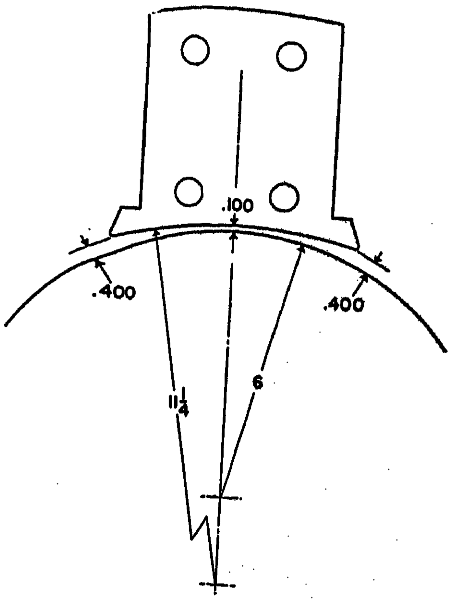


Figure 2. Typical eccentric pole face

actly one commutator bar pitch apart in a narrow strip of tough kraft paper. This strip was then laid on the commutator between two adjacent brush studs and secured to the holders to prevent movement. With the machine running at top speed, normal voltage, voltage readings were taken from a brushholder stud to each hole in succession with a pencil sticker. The data were carefully plotted and a smooth curve drawn through the points. The difference in voltage between any two adjacent points was read off the curve and plotted to give the volts per bar curve; this is a direct measure of the flux distribution. An attempt was made to read volts per bar directly by placing pencil stickers in two adjacent holes, but accurate readings were difficult to obtain and it was felt that the stud-to-bar method gave more accurate results since errors could be detected and minimized by plotting a stud-to-bar voltage curve. Data were taken with the machine operating both at no load and at full load. The machine tested had an eccentric pole face with a 2.8:1 flare. The test and the calculated data are plotted in Figure 3.

The accuracy of the calculations is to a great extent dependent upon the accuracy with which the fringing flux can be estimated. Approximate checks have indicated this to be in the order of magnitude of 10 per cent, so K was assumed to be 0.9 in making the calculations plotted in Figures 3 and 4. The increased error at the pole tip is due, in part at least, to the fact that the actual pole tip had a liberal radius at the corner of the pole arc while this was assumed to be a sharp corner in making the calculation.

Figure 3 clearly indicates the accuracy which can be expected of equations 3 and 5. While not extremely accurate, they do give approximate values of sufficient accuracy to aid in predicting commutation and to be of value in studying various pole arc shapes.

Effect of Pole Arc Shape on Tip and Maximum Volts Per Bar

The fact that increasing the main pole gap reduces the circulating flux and minimizes flux distortion is well known to d-c designers. This is obvious by inspection of Figure 1 and can be evaluated for weak field condition by equations 3 and 5. However, increasing the main pole air gap has the disadvantage of increasing the excitation and is therefore subject to certain limitations. By reducing the center gap slightly and chamfering the pole tips to give a larger tip gap, the circulating flux can be reduced without increasing the excitation. This is commonly done but has the disadvantage of concentrating the flux at a point where the chamfer starts and causing high maximum volts per bar at this point. By designing a main pole arc with a constant radius which is considerably greater than the air gap radius, a gradually flaring gap is obtained without the disadvantage of the chamfered tip. Figure 2 is typical of the eccentric pole face design. This pole was designed for a 12-inch diameter armature. The $11^1/_4$-inch pole face radius gives a tip gap of approximately 0.400 with a center gap of 0.100 or a 4:1 flare with approximately 60 per cent pole arc.

In an attempt to evaluate several different shapes of main pole arcs, the flux distribution or volt per bar curve was calculated for several assumed pole arc shapes for a standard 25/30-horsepower 400/1,600 rpm 230-volt motor. Calculations were made for 1,600 rpm full load with equation 3. These calculated data are plotted in Figure 4. The air gap data for these curves are given in Table I.

Discussion of Results

Attention is called to the fact that the average air gap and hence excitation was maintained constant for all conditions shown in Figure 4. These curves clearly show that the tip and maximum volts per bar can be appreciably reduced, on adjustable speed motors, by increasing the tip gap and reducing the center gap to maintain constant excitation. Curve A is for a uniform gap and the volts per bar increases linearly from the center line on the leading half of the pole and decreases in a similar manner in the trailing half. The volts per bar at the leading edge are very high and it is doubtful that satisfactory performance could be obtained with this design. Curve B shows how the tip volts per bar are reduced approximately 50 per cent by using a 2.8:1 flare with an eccentric pole face. Curves C and D show still further reductions by the use of a 4:1 and 6.2:1 eccentric gap. Curves E and F show that chamfering the main pole face (concentric center gap) materially reduces the volts per bar at the pole tip but results in very high maximum volts per bar under the pole. For this reason curve B, C, or D is preferable. There is no sharply defined optimum ratio of maximum to minimum air gap. Increasing this ratio reduces the volts per bar at the main pole tip. However, the author feels that a 4:1 ratio is a good compromise for adjustable speed motors since greater ratios result in a very small center gap.

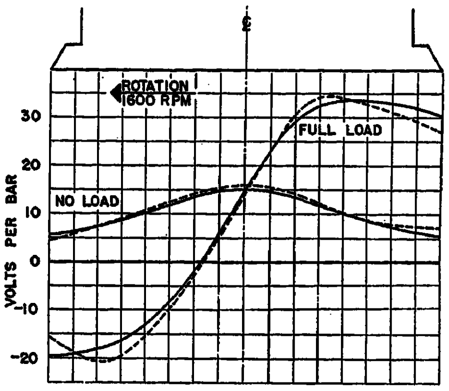


Figure 3. Comparison of test and calculated volts per bar CD 1138-4-30/40 HP-400/1600 RPM-230 volt

———Calculated
-----Test

Table I

Curve	Type	Flare	Air Gap (Inches) Minimum	Air Gap (Inches) Maximum	Average
A	Concentric gap	1:1	0.130	0.130	0.130
B	Eccentric pole face	2.8:1	0.090	0.250	0.130
C	Eccentric pole face	4:1	0.078	0.312	0.130
D	Eccentric pole face	6.2:1	0.066	0.408	0.130
E	Chamfered tip	2.9:1	0.108	0.312	0.130
F	Chamfered tip	4:1	0.102	0.408	0.130

It is interesting to note that regardless of the main pole arc shape, all the full load volts per bar curves go through zero at the point where the armature reaction equals the air ampere turns. While not illustrated in Figure 4, increasing the air gap moves the crossover point farther to the left. If the same ratio of minimum to maximum air gap is maintained and the average is increased, the curve is rotated about the point where it crosses the main pole center line.

Numerous adjustable speed motors ranging from 25 horsepower to 200 horsepower have been built with 4:1 eccentric air gap. Exceptionally good performance was obtained and the motors had a drooping speed characteristic throughout the speed range without the use of a series stabilizing field. Similar motors with less flare required the use of a stabilizing field.

On constant speed motors and generators where the armature teeth are well saturated, the flux tends to distribute itself uniformly across the pole face in spite of the varying air gap. However, the reluctance of the armature cross path is increased by the flaring air gap, and performance of these machines is also improved by the use of the eccentric pole face. The flux distribution or tip volts per bar is not easily calculated on machines operating under saturated conditions, but experience has shown that commutation is materially improved by using the eccentric pole face on all machines.

The tendency of the eccentric pole face to crowd the flux toward the center of the pole increases the noload core loss slightly. This would tend to increase the heating and reduce the conventional efficiencies slightly. However, the eccentric pole face will reduce the load loss by minimizing the field distortion and the resulting double frequency core loss at weak fields.

Thus the actual efficiency and heating on adjustable motors is not seriously affected. On constant speed motors and high speed generators, the core loss is a larger percentage of the total armature loss than it is on adjustable speed motors and therefore has more effect on heating. On these machines the increased no load core loss resulting from the eccentric pole arc may present a more serious problem. No direct comparative data are available.

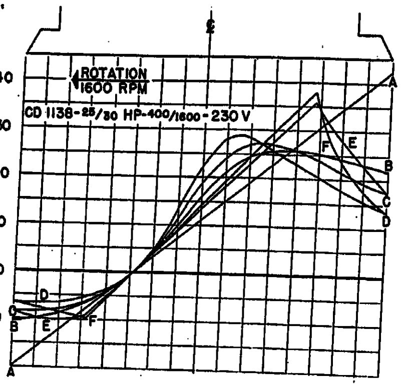


Figure 4. Effect of pole arc shape on unsaturated flux distributed at full load

A—concentric air gap	1:1 flare
B—eccentric pole face	2.8:1 flare
C—eccentric pole face	4:1 flare
D—eccentric pole face	6.2:1 flare
E—chamfered tip	2.9:1 flare
F—chamfered tip	4:1 flare

Crowding the main pole flux towards the center of the pole is of definite advantage in exciters and generators that must operate self-excited over a wide range of voltage. Concentrating the flux at the center of the pole tends to saturate the armature teeth at this point and increases the voltage stability. This effect is not great enough to produce a visible effect in the saturation curve, but experience indicates that it has a definite effect on stability. Self-excited generators have been built for operation at 25 per cent voltage with an eccentric pole face. No pole slots or partial shims were used to bend the saturation curve.

Conclusion

The eccentric pole face offers the designer a means of reducing the tip volts per bar and improving commutation and speed regulation on adjustable speed motors. A 4:1 ratio of maximum to minimum gap has given excellent performance on motors and generators as well. While no optimum ratio can be derived, this appears to be a good compromise.

The eccentric pole face has been used to advantage on constant speed motors and generators to improve voltage regulation, and on variable voltage generators and exciters the eccentric gap improves the voltage stability. The need for the eccentric pole face does not exist on fully compensated machines, but a slightly flaring gap may be of advantage on partially compensated machines.

One of the disadvantages of the eccentric pole face is the reduced center gap for a given excitation. However, this is not serious if a reasonable ratio of maximum to minimum gap is adhered to. The most serious disadvantage is the increased no load core loss. This is offset partially by reduced load loss and presents no serious problem on adjustable speed motors. On constant speed motors and generators this must be taken into consideration in deciding on the amount of flare to use.

No Discussion

An Analysis of Rotating Amplifiers

BERNARD LITMAN
MEMBER AIEE

ELECTRICAL amplifying devices exist in a number of distinct forms such as electronic, magnetic, and rotating amplifiers, each with a distinct field of usefulness. The rotating amplifier is the most prominent in those applications where relatively large power outputs are required. However, even rotating amplifiers have been used in the past only as small exciters to excite the fields of larger machines. This paper considers the incorporation of amplification in large sized generators where questions previously neglected must be considered.

These machines take advantage of the fact that it is possible to produce voltage between selected pairs of brush arms on a multipolar commutator machine by exciting particular poles and to utilize these voltages to excite other sets of poles so that amplification can be obtained. The presence of amplification enables these machines to be used for regulation of their output voltage or any other desired quantity.

In order to make complete use of its capacity as a power generator, all the brush arms will be required to carry their normal share of the output load current as they would in a standard machine and, in addition, any required internal circulating currents. It is necessary to have a multiplicity of windings on the poles, arranged to produce the required fluxes and to neutralize the demagnetizing effects of any one stage of amplification upon the other stages.

To illustrate how amplification is added to a standard generator, one may take as an illustration the simplest case of the 4-pole machine. It is well known that a relatively small amount of power in the field winding of a standard generator will control a much larger amount of power in the armature. Therefore a field winding is placed on two diametrically opposite poles as if they alone formed a 2-pole machine. This will generate a voltage between the two corresponding brushes and if they are joined together, a current will flow. Hence these brushes are joined with a heavy copper winding which is wound on all the poles to excite them as a standard machine. Since the internal currents give rise to other (undesirable) fields, still other field windings must be added to neutralize these fields. Thus in the most complex case, the lead coming from each brush arm may pass through windings on all the poles before it reaches the external load.

Thus several magnetic and electrical circuits have been superimposed on one physical structure. As treated in detail later the stator windings for any machine will be designated by a matrix which tabulates all the windings which each brush lead must pass through. This matrix characterizes the machine and from it the machine properties can be determined. Typical dimensionless curves of machine characteristics will be obtained and performance under various conditions will be considered.

Notation, Units, and Sign Conventions

In order to study these machines, expressions will be set up for all the reactions within the machine when subjected to any unbalanced set of currents, magnetomotive forces, and so forth.

The system consists of a structure containing N main poles and N commutating poles. To designate the various poles, currents and so forth, an index notation is used throughout. See Figure 1.

The various air gaps may in general be unequal. The air gap reluctance of the i'th main pole is represented by R_i and the reluctance of the i'th commutating pole by r_i. The i'th commutating pole is the one immediately clockwise of the i'th main pole, while the i'th brush arm is directly under main pole i. There is a leakage reluctance between adjacent poles which is the same for all poles and is represented by A. There also is a reluctance path from the rotor through the shaft and back to the stator, which is designated by B.

A per-unit system is used throughout so as to eliminate most numerical constants and make numerical values fall within a narrow range for all sizes of machines. Unit voltage is taken as rated output voltage and unit flux is that existing in one main pole when unit output voltage exists at no load. Unit magnetomotive force is that across the main pole air gap when it carries unit flux. One unit of magnetic reluctance is that in one main pole air gap when it has its nominal design value. A special time unit is used to make induced voltages and speed voltages compare, without any factor depending on rotor speed. This time unit, of course, depends on rotor speed and is given by $1/4\,f$ seconds where f is the rotor frequency in cycles per second.

The following sign conventions are employed: positive fluxes and magnetomotive forces point from the pole to the rotor; positive currents point outward from each brush arm and internal armature currents are positive when traveling from one arm to the next clockwise arm. Voltages between arms are positive when the potential increases as one travels clockwise. Thus a balanced machine will have alternately positive and negative values.

Fluxes Due to Stator and Rotor Excitations

For convenience in analysis our magnetic structure is replaced by the equivalent electrical circuit of Figure 1.

To determine the effects of a complex

Paper 49-231, recommended by the AIEE Rotating Machinery Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted June 7, 1949; made available for printing September 9, 1949.

BERNARD LITMAN is with the Arma Corporation, Brooklyn, N. Y.

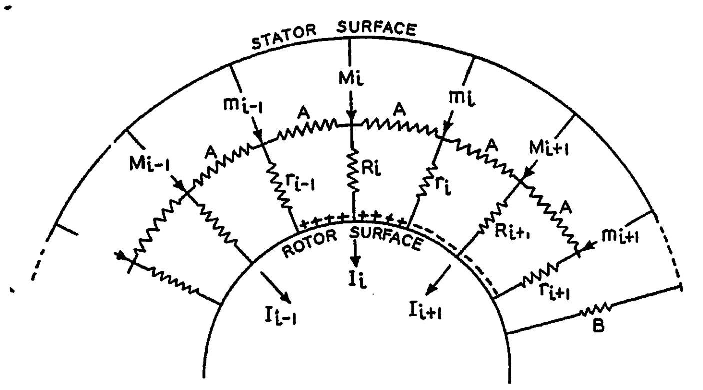


Figure 1. Diagrammatic representation of the machine

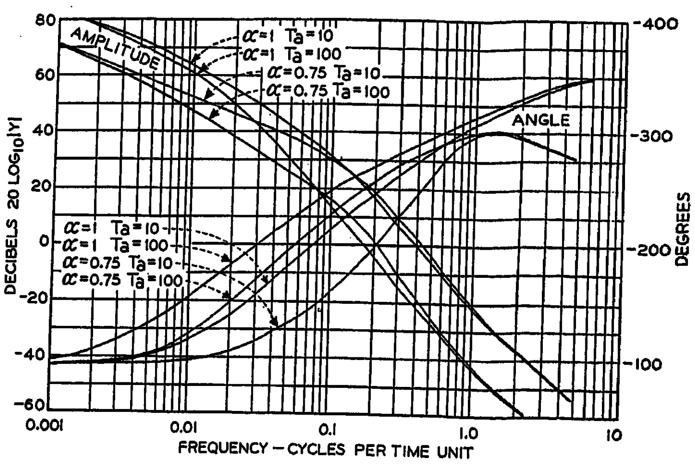


Figure 3. Transfer curves—magnitude and angle of the transfer function

pattern of magnetomotive force, an arbitrary magnetomotive force, M_i, is applied to the i'th main pole only and the fluxes are determined. By inspection of Figure 1 it is seen that all the leakage paths are short circuited except for the two paths adjacent to M_i. Similarly all the resistances which remain (except for R_i) are effectively in parallel and this parallel group is in series with R_i. The flux in pole i is then given by

$$F_i=\frac{M_i}{R_i+\frac{1}{\left[\Sigma_i\frac{1}{r_i}+\Sigma_i\frac{1}{R_i}-\frac{1}{R_i}+\frac{1}{B}\right]}} \tag{1}$$

The flux F_i returns through the other paths in inverse proportion to their reluctances so that the other fluxes are given by

$$F_{k\,(k\neq i)}=-\frac{F_i}{\left[\Sigma_i\frac{1}{r_i}+\Sigma_i\frac{1}{R_i}-\frac{1}{R_i}+\frac{1}{B}\right]+R_k}$$
$$=-\frac{M_i}{R_iR_k\left[\Sigma_i\frac{1}{r_i}+\Sigma_i\frac{1}{R_i}-\frac{1}{R_i}+\frac{1}{B}\right]+R_k} \tag{2}$$

and

$$_k=-\frac{M_i}{R_ir_k\left[\Sigma_i\frac{1}{r_i}+\Sigma_i\frac{1}{R_i}-\frac{1}{R_i}+\frac{1}{B}\right]+r_k} \tag{3}$$

If arbitrary magnetomotive forces now are applied to each main pole, the flux in pole i will be given by a term of the form (1) due to M_i plus terms of the form (2) (with the indices i and k interchanged) due to the other magnetomotive forces and the total effect can be obtained by suitable summation. In order to select the right term to use as the summation is performed, each equation is multiplied by a suitable selection symbol so that the addition becomes a routine algebraic procedure.

Thus equation 1 should be multiplied by a symbol, δ_{ik} defined by $\delta_{ik}=1$ for $i=k$ and $=0$ for $i\neq k$. This assures that this term will appear only once in the summation, namely when $i=k$. (This is recognized as the Kronecker delta symbol.) Similarly, equation 2 should be multiplied by a symbol which is written as η_{ik} and defined as $\eta_{ik}=0$ for $i=k$ and $=1$ for $i\neq k$. Hence it is seen that $\delta_{ik}+\eta_{ik}=1$ for all i's and k's. Using these symbols, our total flux be-becomes

$$F_i=\Sigma_k\left(\frac{M_k\delta_{ik}}{R_k+\frac{1}{\left[\Sigma_i\frac{1}{r_i}+\Sigma_i\frac{1}{R_i}+\frac{1}{B}-\frac{1}{R_k}\right]}}-\frac{M_kn_{ik}}{R_kR_i\left[\Sigma_i\frac{1}{r_i}+\Sigma_i\frac{1}{R_i}+\frac{1}{B}-\frac{1}{R_k}\right]+R_i}\right) \tag{4}$$

The quantity

$$\Sigma_i\frac{1}{r_i}+\Sigma_i\frac{1}{R_i}+\frac{1}{B}$$

has a fixed value, independent of the indices k or i and it is designated by C.

$$C=\Sigma_i\frac{1}{r_i}+\Sigma_i\frac{1}{R_i}+\frac{1}{B} \tag{4A}$$

Hence F_i becomes

$$F_i=\Sigma_k\left(\frac{M_k(CR_k-1)\delta_{ik}}{CR_k^2}-\frac{M_k\eta_{ik}}{CR_iR_k}\right) \tag{5}$$

POLE	1	2	3	4	5	6
ARM 1	$\frac{1}{3}$	1	$\frac{1}{3}$	$-\frac{2}{3}$	$-\frac{1}{3}$	$-\frac{2}{3}$
3	$-\frac{2}{3}$	$\frac{1}{3}$	$-\frac{2}{3}$	$\frac{2}{3}$	$-\frac{1}{3}$	$\frac{2}{3}$
5	$-\frac{1}{3}$	$-\frac{2}{3}$	$-\frac{1}{3}$	$\frac{2}{3}$	$\pm\frac{1}{3}$	$\frac{2}{3}$

6 POLE

POLE	1	2	3	4
ARM 1	$\frac{1}{2}$	$1-\frac{1}{2}$	$\frac{1}{2}$	$-\frac{1}{2}$
2		$\frac{1}{4}$	1	$-\frac{1}{4}$
3	$-\frac{1}{2}$	$\frac{1}{2}$	$-\frac{1}{2}$	$1+\frac{1}{2}$
4	1	$-\frac{1}{4}$		$\frac{1}{4}$

4 POLE

Figure 2. Winding matrices of the 4-pole and 6-pole structures

Then, adding and subtracting $M_k\delta_{ik}/$-$CR_iR_k)$ one obtains

$$F_i=\Sigma_k\left(\frac{M_k}{CR_k}\left(\frac{1}{R_i}+C-\frac{1}{R_k}\right)\delta_{ik}-\frac{M_k}{CR_iR_k}(\delta_{ik}+\eta_{ik})\right) \tag{5A}$$

Remembering that $\delta_{ik}+\eta_{ik}=1$ as well as the obvious property of δ_{ik} that $\Sigma_k\psi_k\delta_{ik}=\psi_i$ for any expression, ψ, the summation reduces immediately to

$$F_i=\frac{M_i}{R_i}-\frac{1}{CR_i}\Sigma_k\frac{M_k}{R_k} \tag{6}$$

Equation 6 should be extended to include commutating pole magnetomotive forces and proceeding in the same manner, it becomes

$$F_i=\frac{M_i}{R_i}-\frac{1}{CR_i}\Sigma_{kk}\frac{M_k}{R_k} \tag{7}$$

Here Σ_{kk} represents a summation for both the main and commutating poles. An analogous procedure for the commutating pole fluxes gives

$$f_i=\frac{m_i}{r_i}-\frac{1}{Cr_i}\Sigma_{kk}\frac{M_k}{R_k} \tag{8}$$

The summation term in equations 7 and 8 will ordinarily drop out. This is due to the fact that the machines normally dealt with have equal gaps, and the magnetomotive forces, though unequal, will (as seen later) add up to zero. Thus the flux in each pole may be obtained from the excitation on that pole and is independent of the other excitations. Moreover, if unequal air gaps give a nonzero summation, the quantity within the summation sign will no longer have any indices after being summed. Hence this summation needs to be calculated just once and will apply to all the poles in the machine.

Figure 1 also shows a rotor current in

that branch of the winding from arm i to arm $i+1$. The fluxes set up by this current may be determined in a manner analogous to what has just been done for stator excitations and then the effect of more complicated current patterns is determined by superposition.

The rotor current linked in any one of the loops in Figure 1 is given by $\rho i_i/2$. Here ρ is a quantity which ties in the per-unit systems between rotor and stator. This is numerically the same as the ratio of armature magnetomotive force at rated balanced load to unit main pole magnetomotive force. ρ will be of the order of magnitude of 1 for most machines.

Equations for the various loops now may be written in order to solve for the fluxes set up by this rotor current. Then an expression for the fluxes set up by a compound set of rotor currents may be obtained

$$F_i=\frac{\rho}{2R_i}(i_i+i_{i-1})-\frac{\rho}{2CR_i}\Sigma i_{kk}\left(\frac{2}{r_k}+\frac{1}{R_k}+\frac{1}{R_{k+1}}\right) \quad (9)$$

Similarly, for the commutating pole, the expression takes the form:

$$f_i=\frac{\rho}{r_i}i_i-\frac{\rho}{2Cr_i}\Sigma_k i_k\left(\frac{2}{r_k}+\frac{1}{R_k}+\frac{1}{R_{k+1}}\right) \quad (10)$$

As with equations 7 and 8, it is seen that for a machine with balanced air gaps, the summation terms will drop out.

Rotor Voltages

General expressions now may be obtained for rotor voltages due to speed and inductive effects under unbalanced conditions. One of the rotor coils is taken in an arbitrary position and its flux linkage expressed as a function of position and the flux in the stator poles. The coil voltage is then proportional to the total derivative of this expression; the position change giving the speed voltage while the stator flux change gives the induced voltage. By using the special time unit, rotor speed is eliminated from this expression and the equation reduces to

$$E_i=\frac{F_i-F_{i+1}}{2}-P\left(\frac{F_i+F_{i+1}}{2}\right) \quad (11)$$

where P is the differential operator with respect to the unit of time.

The voltage between any two nonadjacent terminals will be designated as E_{ij} where i and j refer to the two terminals between which voltage is measured. This value can be obtained by direct summation from equation 11, giving

$$E_{ij}=\sum_{i=1}^{i=j-1}E_i=\frac{F_i-F_j}{2}-P\left(\sum_i^j F_i-\left(\frac{F_i+F_j}{2}\right)\right) \quad (12)$$

Equation 12 is rather simple in form since it depends only on the total flux under each pole and not on the distribution of this flux along the pole face. A more careful analysis shows that arbitrary variation of air gap reluctance and magnetomotive force along the pole face will cause some error in the expression. However, this error affects only the transient term and for any variations that might be encountered, this error is found to be small enough to neglect.

Commutation

Commutation is a matter of prime importance on a large machine. In contrast to small generators, where little attention need be paid to this question, satisfactory commutation must be assured in order to have a successful device. The machine presents the problem of a very large current collecting surface traveling at high peripheral speed. Even minor imperfections will cause serious interruption of the contact surface between brush and commutator. Hence it is important to provide proper electrical conditions for commutation of any unbalanced combination of rotor currents.

To determine the required stator commutating windings, an arbitrary coil undergoing commutation is considered. A coil short circuited by brush i undergoes a change of current from i_{i-1} to i_i during its short-circuit period and there is a corresponding reactance voltage designated by $\gamma(i_i-i_{i-1})$. The factor γ includes inductance and time of commutation.

There also will be an appreciable mutual effect due to coils short circuited by the adjacent brushes on either side. Thus the over-all voltage can be written as

$$E_c=\gamma(i_i-i_{i-1})-\mu(i_{i-1}-i_{i-2})-\mu(i_{i+1}-i_i) \quad (13)$$

Here μ is similar to γ except that it accounts for the mutual effects. In order to have satisfactory commutation, it is necessary that this voltage be completely neutralized by the fluxes f_i and f_{i-1} of the two commutating poles through the fields of which the coil is passing. The desired opposing voltage is then proportional to $f_{i-1}-f_i$ and can be calculated by using equations 8 and 10 which give the commutating pole fluxes due to both the armature currents and the stator excitations. Application of these two equations yields

$$f_{i-1}-f_i=\rho\left(\frac{i_{i-1}}{r_{i-1}}-\frac{i_i}{r_i}\right)+\frac{m_{i-1}}{r_{i-1}}-\frac{m_i}{r_i}+\left(\frac{1}{r_i}-\frac{1}{r_{i-1}}\right)\left[\frac{\rho}{2C}\Sigma_k i_k\left(\frac{2}{r_k}+\frac{1}{R_k}+\frac{1}{R_{k+1}}\right)+\frac{1}{C}\Sigma_{kk}\frac{M_k}{R_k}\right] \quad (14)$$

This flux, $f_{i-1}-f_i$, must (except for an appropriate multiplying constant) be equal to equation 13 and the terms m_i, must be chosen so as to accomplish this.

It is seen that the term $\Sigma_{kk}M_k/R_k$ of equation 14 also includes main pole magnetomotive forces so that the commutating pole windings must correct for main pole excitations. This undesirable requirement can be eliminated if r_i is made equal to r_{i-1}, which is easily done by making all commutating gaps equal. (This still has put no restriction on main pole air gaps.) Equation 14 now reduces to

$$f_{i-1}-f_i=\frac{\rho}{r}(i_{i-1}-i_i)+\frac{1}{r}(m_{i-1}-m_i) \quad (15)$$

Equations 13 and 15 now are equated and at the same time a new set of currents, the external currents leaving each brush arm, are introduced. These currents, which are the only ones directly available for use in the stator windings are defined by

$$I_i=\frac{1}{2}(i_{i-1}-i_i) \quad (16)$$

The factor of 1/2 has been chosen so that with normal balanced load, I_i also will equal 1. This gives

$$\frac{1}{2r}(m_i-m_{i-1})=\frac{\rho}{r}I_i+\gamma I_i-\mu(I_{i-1}+I_{i+1}) \quad (17)$$

To determine the required pattern of commutating pole exciting windings it is assumed that each pole may need separate windings from each brush arm so that

$$m_i=A_iI_i+A_{i+1}I_{i+1}+A_{i+2}I_{i+2}+\ldots\ldots A_{i-1}I_{i-1} \quad (18)$$

Because of the symmetry of the machine, the sequence of windings on the next adjacent pole must be the same except for a change of one unit in the index of m and the I's, giving a similar expression for m_{i-1}. If all the currents were independent variables, one could take m_i-m_{i-1} and equate coefficients of the various I's to the corresponding terms on the right side of equation 17. However, use must be made of the fact that all the currents are not independent, for the sum of the currents leaving the rotor must equal zero. Using this relation, one of the currents is eliminated and the coefficients of the remaining currents are equated. It is possible to solve this set of $(N-1)$ equations in N unknowns if one more requirement is added and this requirement is that the sum of the absolute values of all the A's is a minimum, thus giving a minimum amount of copper in the machine. Proceeding on this basis, the final result obtained is

$$\left.\begin{aligned} A_i&=\frac{N}{2}D+2r\mu \\ A_{i+1}&=-\left(\frac{N}{2}-1\right)D-2r\mu \\ A_{i-1}&=\left(\frac{N}{2}-1\right)D \end{aligned}\right\} \qquad (19)$$

and for the others,

$$A_{i+k}=\left(k-\frac{N}{2}\right)D$$

where

$$D=\frac{2\rho+2r\gamma-4r\mu}{N}$$

It should be noted that these values are not in actual turns but in per-unit magnetomotive force for unit current. This may be compared with the amount of winding on a normal balanced machine and it is found that the ratio of the two is approximately $0.4N+0.2$.

This formula brings out a practical limitation to the number of independent poles to be used on a machine, since the amount of copper increases undesirably when the number of poles increases. However, as pointed out later, there is no need for a very large number of amplification stages, and hence the number of truly independent poles in the structure need not be very large. Thus an 8-pole machine might be built as two 4-pole machines and the commutating windings would then be designed for a 4-pole structure. A 6-pole machine cannot be further broken down and it would be necessary to treat it as a 6-pole structure.

Stator Winding Arrangements and Selection

The problem of selecting the most effective pattern of stator windings to accomplish a given purpose now may be considered. Various arrangements will be discussed and where possible windings will be made to serve multiple purposes. It is necessary to become somewhat specific in discussing these winding arrangements but the treatment will demonstrate the general technique to be used on any structure.

In a typical application, all the positive brush arms are connected together at one point, after first going through the required commutating coils and such main pole coils as are required. The resistances from brush arm to common junction should be equal on all arms so that the main load current will divide equally in the steady state. Similar connections are made for the negative brush arms.

Normally, the main pole air gaps will be made equal so that at steady state the currents will divide nearly equally and the machine will operate at greatest efficiency. However, the formulas will permit calculating the effects when the gaps are unequal.

Previous attempts to consider possible internal amplification stages have visualized patterns which were similar to standard machines. Thus an 8-pole machine might use every other pole as a 4-pole structure. This procedure is inapplicable to machines whose total number of poles divided by an even integer fails to yield an even integer for a quotient. An example is the 6-pole or 10-pole machine, for here one cannot pick out a normal 2-pole or 4-pole machine pattern, by using arms which are normally at the same potential. Therefore the present treatment also will consider circulating paths between arms which are not symmetrically spaced. This in turn will set up unsymmetrical current and flux patterns but these can be used to obtain amplification.

For clarity in illustration, a particular case of a 6-pole machine is taken. This will demonstrate all the features to be brought out and also require the use of unsymmetrical paths in the machine. Initially a control field is placed on two poles which have the same polarity for the output stage. Thus poles 1 and 3 are picked and excited so that $M_1=1$ and $M_3=-1$. With balanced air gaps the resultant fluxes are then very simply given by equation 7 as: $F_1=-F_3=1, F_k=0_{k\neq 1 \text{ or } 3}$. The steady-state generated voltages now are determined from equation 12 giving: $E_{13}=1$ and $E_{15}=1/2$. It is seen that there is no current flow from 5 but only from terminal 1 to terminal 3. This current may be used to excite the normal 6-pole machine or, if desired, to provide another internal stage of amplification. The first possibility is chosen here since this provides sufficient amplification for most practical cases. Hence windings are placed in the circuit 1–3 to neutralize the armature reaction effects set up by this current and in addition to excite the 6-pole structure. The armature reaction fluxes should not be cancelled completely since these fluxes may alter the machine characteristics in a desirable manner.

If the current from 3 to 1 is called I_{31}, the current in the short path through the armature from arm 1 to arm 3 is $2/3\ I_{31}$ and that in the long path is $1/3\ I_{31}$. Equation 9 is then applied, giving

$$F_1=F_3=\frac{1}{3}\rho I_{31};\quad F_2=\frac{4}{3}\rho I_{31};$$

$$F_4=F_5=F_6=-\frac{2}{3}\rho I_{31} \qquad (20)$$

To neutralize these armature reaction fluxes, the following compensating windings are placed in the 3 to 1 path

$$N_1=-\frac{1}{3}\alpha\rho;\quad N_2=-\frac{4}{3}\alpha\rho;\quad N_3=-\frac{1}{3}\alpha\rho;$$

$$N_4=N_5=N_6=\frac{2}{3}\alpha\rho \qquad (21)$$

Here the polarities are given for circulating current from arm 3 to arm 1. After specifying on which side of the common junction point to place the windings, the polarities may be specified in terms of our standard notation. α is a number which indicates how effectively the armature reaction is compensated for. It would be equal to 1 for 100 per cent compensation and will be some number close to 1 in practice. In addition to these windings, there are also windings (called the forcing windings) to excite all six poles as a normal machine. These need not be a set of separate windings but may be combined with the compensating windings.

For the moment, neglecting these windings and taking the windings already present, a decision is made on how to divide them on each side of the junction. These windings not only supply the excitations desired but also provide excitation when normal balanced load is supplied. To avoid these additional excitations, each coil must be split into two half coils, one on each side of the junction point, so that they neutralize each other for balanced load. A better procedure is to use these additional excitations and thus have the coils serve a double purpose. These excitations will be used for the self-excited "tuned series field" for the final 6-pole stage of the machine. This amounts to a positive feedback for this stage and will be used to make the steady-stage amplification infinite so that in the steady state the machine can regulate very accurately and the unbalanced currents of the preliminary stages are very small. By this procedure one also avoids splitting the compensating coils into two parts, thus saving many coil interconnections.

The first step in splitting these windings is to place them so that for normal load they provide excitation in the proper

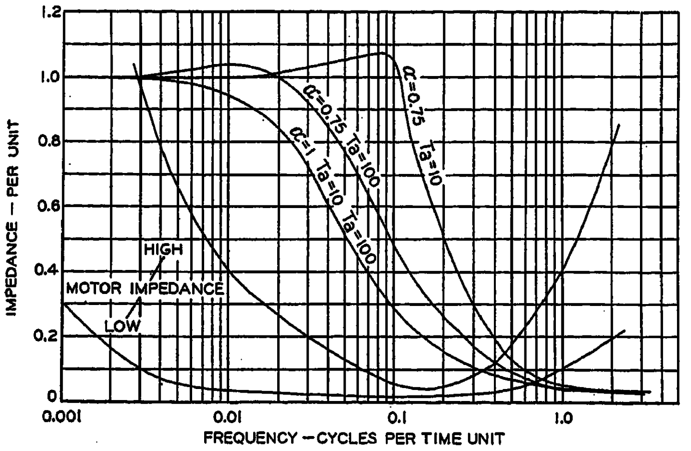


Figure 4. Minimum load impedance for stability

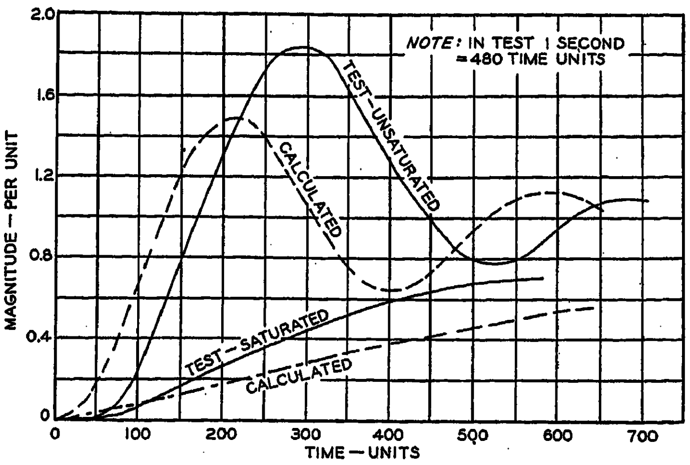


Figure 5. Transient response of machine—calculated and test values

direction for the series field excitation of the output stage. This can be done by inspection. In these two arms our forcing windings also must be placed. Rather than place them here as completely independent windings, an economy of coil connections may be effected by combining them with the existing coils. The total amount of excitation to be provided by the forcing windings is not critical. The more used, the more effective is the excitation of the final output stage. However, the use of too much excitation involves an excessive winding and corresponding copper loss. A suitable compromise is one unit of magnetomotive force per pole for unit circulating current.

The forcing winding has an equal number of turns on each pole and since the most desirable way to split these windings on each side of the junction point is not known, they are tentatively split into two parts; one in arm 1 of magnitude $a\alpha p$ and one in arm 3 of magnitude $b\alpha p$. Since αp is approximately equal to 1 or a bit larger, $a+b$ will be made roughly equal to 1. These quantities are added to what is already present on these poles and then expressions for the absolute values of the windings in these arms are written. Curves for these absolute values can be plotted and any horizontal line on these curves will pick out a pair of values for a and b which gives equal resistance in arms 1 and 3. By inspection, a convenient combination can be selected which does not give rise to a large number of odd coil sizes.

The result finally obtained is shown in the pattern of Figure 2 which is called the stator winding matrix. The total turns in each arm is now 10/3 and an equal amount is placed in arm 5 to balance the resistances. These extra turns are selected by inspection and are used to correct the excitations on all the poles so that they are proper for normal load. The winding matrix, made up of pure numbers, characterizes the machine independently of its size or rating and may be used to determine the performance of the machine.

Similar winding matrices may be developed for other variant types of this class of machine. In particular, a matrix for a 4-pole structure is of interest in order to get some data for comparison with machines that have already been built. The same techniques are used as illustrated above, and the matrix is built up in systematic fashion. This 4-pole matrix also is shown in Figure 2.

Differential Equations

It is desired to set up the differential equations of the machine and use them to determine some of the features of the machine behavior. Since the machine has a great many windings, the procedure is lengthy but can be carried out in a systematic manner.

In the case of the 6-pole structure, there are five degrees of freedom for the six arm currents (since the sum of these currents is zero) and an additional current for the control field. Thus the over-all current pattern will require six components and any six current patterns may be chosen as the basic currents provided they are all independent. Similarly, for any structure involving N poles, there are N independent current patterns.

The choice of the actual patterns to use is largely a matter of convenience. There are those patterns which are intentionally set up for purposes of amplification. For example, in the 6-pole machine there are the internal circulating current I_{31} and the control field current. In addition, the normal load current, I_L, and any three other patterns are used. For these the fluxes through the machine are determined and then the effects of these fluxes in both stator and rotor. Eventually all the currents will be eliminated except the control current and load current which are the two of interest.

In determining the voltages around the various loops equation 12 is used for that part of the voltage between rotor terminals. The voltage induced to any of the stator windings is proportional to the magnitude of the winding and the rate of flux change through the winding. These voltages can be obtained in a systematic fashion by writing the fluxes as a column vector of N elements (each element being the flux under one pole) and then multiplying from the left by the stator winding matrix. The result of this matrix product is a column vector whose elements represent the induced voltages in each of the arms. When combined with the voltages in the rotor windings and the resistance drops, the complete expressions for the differential equations are obtained as a set of self and mutual factors between the currents.

Invariance of Machine Behavior to Matrix Changes

The previous work discussed various arrangements of stator windings to produce the required flux patterns. The windings were altered freely to obtain the most economical arrangement and no attention was paid to possible changes in machine behavior.

Actually it can be shown that if two radically different windings set up the same flux patterns, they also will have the same induced voltages in all the loops and thus give rise to the same machine behavior. This can be done by taking the winding matrix and using it to express the induced voltage as a matrix product. Examining the elements of this matrix product for any two arrangements which give the same flux patterns, it is found that they must be the same. Hence, the simplest stator winding may be picked without impairing machine behavior.

Transfer Characteristics

In order to determine some of the features of the transient behavior of the machine the transfer characteristic (ratio of output current to control field voltage) must be determined for a range of frequencies. This is obtained directly from the differential equations and is represented by the ratio of two determinants. Evaluation of this expression is made practicable by expressing it graphically in terms of only a few parameters. This can be done since the per-unit notation gives approximate numerical values for many quantities such as rotor resistances which fall within a narrow range of values. The expression, when evaluated, is of the form $\psi_1/(\psi_2+Z\psi_3)$. Here ψ_1, ψ_2, ψ_3 are integral functions of the frequency and contain as parameters the control field time constant, T_a, and the amount of machine compensation, α. Z is the load impedance and it is seen that the transfer expression depends on type of load used.

The type of load most commonly encountered is one which at zero frequency has unit resistance drop (to balance the unit terminal voltage) and at other frequencies presents an inductive impedance. This load may therefore be written as $1+LP$. L will ordinarily be very large in the per-unit system since loads usually encountered will have moderately large time constants. For this case it can be shown that L may be removed as a separate factor with little inaccuracy and the transfer expression reduces to the form

$$\frac{I_L}{E_a}=\frac{H}{R_aL}Y(P,\alpha,T_a) \qquad (22)$$

where $Y(P,\alpha,T_a)$ is a function of frequency with α and T_a as parameters. H represents the magnitude and R_a the resistance of the control field winding. Curves for $Y(P,\alpha,T_a)$ are plotted for both amplitude and phase angle in Figure 3. These data are given for the matrix of the 4-pole structure and will be used for comparison with actual tests. A similar set of curves can be prepared for the 6-pole structure and when this is done it is found to be very similar in form and even in fair quantitative agreement over a large portion of the frequency range.

Limitations on Load Impedance

The properties of the transfer expression may be considered for any load, Z. Principal interest lies in determining what characteristics the load should have in order to form a stable combination with the generator without addition of any special stabilizing means. As already mentioned, the transfer admittance is of the form $(\psi_1/\psi_2+Z\psi_3)$. Hence it is desirable to determine what poles this expression has in the right half P plane. This is given by the corresponding zeros of the denominator.

Since $\psi_2+Z\psi_3=Z\psi_3(1+\psi_2/Z\psi_3)$; it is seen that if $|Z|>|\psi_2\psi_3|$, the plot of $(1+\psi_2/Z\psi_3)$ cannot loop the origin so that the zeros are given only by zeros of Z and ψ_3. Moreover, ψ_3 may be checked separately and it is found that it has no zeros. Hence a sufficient (though not necessary) condition for a stable load has been set up. This limiting value of Z is plotted as a set of curves over the frequency spectrum in Figure 4. Again the values for the 4-pole structure are used although the curves for the 6-pole structure are quite similar. Here it is seen that a serious restriction has not been imposed since $|Z|$ stays close to 1 and soon drops to zero. Any Z used will usually start at 1 and rise as the frequency increases, so that it will meet this condition.

There is one possible type of load which will fall below these curves over part of the frequency range. This is a load which contains very large capacitance elements which make their effect felt at low frequencies. Such a load is represented by the impedance of a motor.

The range of values which the motor impedance may take can be obtained by considering possible per-unit values of the motor constants and the differential equation of the motor. This has been done and curves plotted in Figure 4. It is seen that the motor impedance definitely lies below the required limit over part of the frequency range, so that instability would be expected. A more complete check verifies that this does give rise to an unstable combination.

Saturation

As soon as the point is reached where saturation in the iron becomes noticeable, it is found that the effect does not appear gradually, but occurs abruptly enough so that it may be described in terms of a ceiling voltage which the machine has reached. This behavior can be accounted for by examining the armature reaction magnetomotive forces due to internal circulating current and to output current. These currents combine to produce saturation under one control field pole and not the other. The loss in flux due to this saturation is such as to require more control field magnetomotive force. Since both these currents are quite large compared to the original control field current, the demagnetizing effect is large and accounts for the abrupt saturation. This effect will require a more liberally designed machine where high ceiling voltages are needed. It is possible to make approximate calculations for the behavior under saturation by assuming linear performance until the ceiling voltage is reached and calculating the further response to this fixed ceiling voltage.

Hysteresis

Hysteresis, although small enough so that the transient behavior is not greatly affected, will prevent the machine from providing perfect accuracy of regulation in the steady state. An approximate investigation of this error shows that the only magnetic path which contributes an important term to this error is the control field path. The other paths have sufficiently large currents and magnetomotive forces so that the hysteresis is small by comparison.

An approximate formula may then be set up for the hysteresis error as

$$\text{error}=\frac{\Delta M}{\dfrac{H}{R_a}K_o} \qquad (23)$$

Here ΔM is the width of the hysteresis loop and K_o is the amount of feedback from the error detecting device in whatever regulating system the generator is being used. As expected, the error is reduced by larger values of K_o since the error detecting device attempts to reduce whatever error appears.

Comparison With Test Results

The transfer curves for the 4-pole structure may be employed to determine transient behavior for comparison with test data.

The regulating generator as tested, was used in the voltage regulation of a turbo alternator whose field winding constitutes the load of the regulating generator. The output voltage (assumed proportional to alternator field current) is rectified and a fraction of it, K, is compared with a reference standard, E_o. The difference, if any, is applied to the control field which then corrects the voltage as needed. Thus $E_a=E_o-KI_L$ and there results for transfer expression with feedback present

$$I_L=\frac{E_o}{K}\cdot\frac{1}{\left(1+\dfrac{LR_a}{HYK}\right)} \qquad (24)$$

This frequency curve may then be transformed to the corresponding time response for various input voltages.

Figure 5 shows a calculated time response to unit step voltage change in the control field together with an actual test curve for this case. The agreement is seen to be fairly good. The fact that the error is still moderately large is to be expected from the nature of the calculation. Thus the data for this machine have been interpolated graphically from the curves. Similarly, the amount of feedback is not known exactly and since the system is not far away from the critical value of feedback, small changes in feedback will cause appreciable changes in the overshooting of the curves. Figure 5 also shows another test in which the voltage was raised close to saturation. The machine reached its ceiling voltage very quickly and most of the curve represents response to this ceiling voltage. Again there is fairly good agreement between test and calculation.

References

1. THE MULTISTAGE ROTOTROL, M. M. Liwschitz. *AIEE Transactions*, volume 66, 1947, pages 564–8.

2. THEORIE DU FONCTIONNEMENT DE LA METADYNE, J. M. Pestarini. *Revue Generale de L'Electricite* (Paris, France), volume 27, 1930, pages 355, 395 and volume 28, pages 227, 260.

3. NETWORK ANALYSIS AND FEEDBACK AMPLIFIER DESIGN (book), H. W. Bode. D. Van Nostrand Company, New York, N. Y., 1945.

4. MAIN EXCITER ROTOTROL EXCITATION FOR TURBINE GENERATORS, C. Lynn, C. E. Valentine. *AIEE Transactions*, volume 67, 1948, pages 535–39.

5. APPLICATION OF TENSORS TO THE ANALYSIS OF ELECTRICAL MACHINERY, G. Kron. *General Electric Review* (Schenectady, N. Y.), 1942.

6. PRINCIPLES OF SERVOMECHANISMS (book), G. S. Brown, D. P. Campbell. John Wiley and Sons, New York, N. Y., 1948.

No Discussion

Temperature Rise Values for D-C Machines—II

AN AIEE COMMITTEE REPORT

THE results of heat run tests made jointly by the Allis-Chalmers Manufacturing Company, Century Electric Company, General Electric Company, Reliance Electric and Engineering Company, and Westinghouse Electric Corporation on a motor exchange program involving motors at 1,750 rpm, 40 degrees centigrade open, from 1/2 to 50 horsepower, were reported in a previous paper.[1]

It was recommended that Table I of the American Standards Association C-50[2] be expanded, using values derived from the results of tests, to cover short-time rated motors and to include limiting values of temperature rise by the resistance method corresponding to existing values for the thermometer method for all windings. An expanded table was given as a conclusion and it was recommended that after due discussion and verification it be used to replace the present standard. Use of the resistance method for windings of less than 1-ohm resistance was considered only as an alternative test procedure of practical value in occasional instances, but not usable without determination and public recognition of temperature rise values corresponding to standard values for the thermometer method.

In publishing the previous paper the committee recognized the need for similar data on a sufficient number and variety of machines to make the results representative of d-c machines generally. An exchange motor test program on large machines was not practical, but the same companies offered from their engineering data files, test results on a large number of machines of all sizes. These results are reported in this paper giving a new expanded table with values suitable for a general standard covering integral-horsepower d-c machines.

It is recommended that this table supersede the one of the previous paper and that expansion of Table I of ASA Standard C-50 be made accordingly. For reasons of accuracy, reliability, and economy in the testing procedure of d-c machines, in general, the committee recommends the thermometer method as the preferred or standard test procedure. These reasons are elaborated with substantiating data later in the paper. In lieu of expansion of the ASA Standard, the committee recommends the use of the table given herein in cases where the alternative use of the resistance method is desirable.

Method

Although the test procedure used by the different manufacturers varied in details, the following procedure is sufficiently representative to show what was done by each.

Hot- and cold-resistance readings were taken on shunt field and armature windings.

For the field winding the hot resistance was determined by reading the field volts *V* and the amperes *A* just previous to shutdown taking the ratio *V*/*A*. This was done to avoid the error possible in correcting the measured resistance to the instant of shutdown. When measuring *V* and *A*, it is necessary that the true field volts and amperes are read excluding the current taken by the low-voltage relay usually connected across the field circuit.

For the armature winding the resistance is measured between two segments on the commutator. This measurement is taken when the armature is cold and again after the machine is operated under normal conditions of load and ventilation until the temperature of the stationary parts becomes constant. For the latter, the machine is shutdown quickly maintaining the load current until nearly stopped. Quickly after the machine stops the resistance is read between the same segments or at the most accessible spot on the commutator. If the same segments are not used, contact is made with two segments the same distance apart as for the cold reading. The resistance and the elapsed time from the start of the shutdown are recorded together at intervals of about one minute for 15 or 20 minutes. The exact position at which the hot resistance was read is marked on the commutator and after the machine has cooled to room temperature a cold-resistance check is made at this same position.

The resistance measurements are taken using a double bridge connected as shown in Figure 8. A special bridge for low resistance is used with a very sensitive external galvanometer. Current is fed into the armature through leads or through the brush rigging of the machine. Voltage drop is taken off the commutator segments. Care is exercised to allow the readings to be taken without effect from the equalizing winding and with negligible effect from the resistance of the commutator segments and risers to which the voltage contacts are connected. This means that the temperatures indicated by the changes in resistance are for the armature windings alone and are not affected by the temperature changes of the risers, commutator, and equalizer winding.

Temperature rise by thermometer

Paper 49-232, recommended by the AIEE Rotating Machinery Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 20, 1949; made available for printing September 16, 1949.

Prepared by the AIEE Subcommittee on D-C Machines of the Rotating Machinery Committee. Committee personnel: T. M. Linville, *Chairman*, Mrs. B. O. Buckland, W. E. Clancy, F. A. Compton, Jr., Lanier Greer, L. N. Grier, J. L. Hamilton, C. B. Hathaway, V. P. Hessler, V. B. Honsinger, W. R. Hough, P. Lebenbaum, C. Lynn, L. G. Opel, D. Ramadanoff, J. F. Sellers, B. H. Caldwell, Jr., W. H. Fifer, D. E. Fritz, and A. T. McClinton.

Table I. Present Table ASA-C50-1943

Item	The temperature rises in degrees centigrade in this table apply to the several classes of machines as given at the heads of the respective columns. Machine Part	Method of Temperature Determination to Be Employed	General Purpose Generators and Motors Class A Insulation Col. 1	Totally Enclosed, Totally Enclosed Fan-Cooled, Explosion-proof, Waterproof, Dust-tight, and Submersible Motors Class A Insulation Col. 2	Totally Enclosed, Totally Enclosed Fan-Cooled, Explosion-proof, Waterproof, Dust-tight, and Submersible Motors Class B Insulation Col. 3	Generators Having a Nominal Rating At End of 2-Hour Overload Class A Insulation Col. 4	Generators Having a Nominal Rating At End of 2-Hour Overload Class B Insulation Col. 5	Generators Having a 2-Hour 25 Per Cent Overload Rating At Continuous Load Class A Insulation Col. 6	Generators Having a 2-Hour 25 Per Cent Overload Rating At Continuous Load Class B Insulation Col. 7	Motors and Generators At End of Two-Hour Overload Class A Insulation Col. 8	Motors and Generators At End of Two-Hour Overload Class B Insulation Col. 9	Motors and Generators Other Than Col. 1–9 Inclusive Class A Insulation Col. 10	Motors and Generators Other Than Col. 1–9 Inclusive Class B Insulation Col. 11
1	Armature windings, and all windings other than 2 and 3	Thermometer See 2.054	40	55	75	55	75	40	60	55	75	50	70
2	Shunt field windings	Thermometer See 2.054	40*			55*	75*	40*	60*	55*	75*	50*	70*
		Resistance See 2.055	50*	60	80	65*	85*	50*	70*	65*	85*	60*	80*
3	Single-layer field windings with exposed uninsulated surfaces and bare copper windings	Thermometer See 2.054	50	65	85	65	85	50	70	65	85	60	80
4	Cores and mechanical parts in contact with or adjacent to insulation	Thermometer See 2.054	40			55	75	40	60	55	75	50	70
5	Commutators and collector rings**	Thermometer See 2.054	55†	65	85	65	85	55	70	65	85	65	85
6	Miscellaneous parts (such as brush holders, brushes, pole tips, etc.) may attain such temperatures as will not injure the machine in any respect.												

* The method of temperature determination to be used shall be optional with the manufacturer unless otherwise agreed upon. Only one method of temperature determination shall be required in any particular case.

** The class of insulation refers to insulation affected by the heat from the commutator, which insulation is employed in the construction of the commutator or is adjacent thereto.

† The limiting observable temperature rise for commutators of generators for electrolytic service shall be 50 degrees centigrade.

Proposed New Table

Item	The temperature rises in degrees centigrade in this table apply to the several classes of machines as given at the heads of the respective columns. Machine Part	Method of Temperature Determination	General Purpose Generators and Motors Class A Insulation Col. 1	General Purpose Generators and Motors Class B Insulation Col. 2	Totally Enclosed, Totally Enclosed Fan-cooled, Explosion-proof, Waterproof, Dust-tight, and Submersible Motors Continuous Rated Class A Insulation Col. 3	Totally Enclosed, Totally Enclosed Fan-cooled, Explosion-proof, Waterproof, Dust-tight, and Submersible Motors Continuous Rated Class B Insulation Col. 4	Motor and Generators Rated 30 Minutes to 2 Hours Inclusive Class A Insulation Col. 5	Motor and Generators Rated 30 Minutes to 2 Hours Inclusive Class B Insulation Col. 6	Motors and Generators Rated 15 Minutes or less Class A Insulation Col. 7	Motors and Generators Rated 15 Minutes or less Class B Insulation Col. 8	Generators Having a 2-Hour 25 Per Cent Overload Rating at Continuous Load Class A Insulation Col. 9	Generators Having a 2-Hour 25 Per Cent Overload Rating at Continuous Load Class B Insulation Col. 10	Generators Having A 2-Hour 25 Per Cent Overload Rating and Generators Having a Nominal Rating at End of 2-Hour Overload Class A Insulation Col. 11	Generators Having A 2-Hour 25 Per Cent Overload Rating and Generators Having a Nominal Rating at End of 2-Hour Overload Class B Insulation Col. 12	Special Purpose Drip-proof and All Motors and Generators Other Than Col. 1–12 Inclusive Continuous Rated Class A Insulation Col. 13	Special Purpose Drip-proof and All Motors and Generators Other Than Col. 1–12 Inclusive Continuous Rated Class B Insulation Col. 14
1	Armature windings and all windings other than 2 and 3	Resistance See 2.055	60		70	100	80	110	90	125	60	90	80	110	70	100
		Thermometer See 2.054	40		55	75	55	75	55	75	40	60	55	75	50	70
2	Shunt Field Windings	Thermometer See 2.054	40				55	75	55	75	40	60	55	75	50	70
		Resistance See 2.055	60		70	90	70	90	70	100	60	90	75	105	70	90
3	Single-layer field windings with exposed uninsulated surfaces and bare copper windings	Resistance See 2.055	60		70	100	80	110	90	120	60	90	80	110	70	90
		Thermometer See 2.054	50		65	85	65	85	65	85	50	70	65	85	60	80
4	Cores and mechanical parts in contact with or adjacent to insulation	Thermometer See 2.054	40				55	75	55	75	40	60	55	75	50	70
5	Commutators and collector rings	Thermometer See 2.054	55	75	65	85	65	85	65	85	55	70	65	85	65	85
		Pyrometer	65	85	75	100	75	100	75	100	65	85	75	100	75	100
6	Miscellaneous parts (such as brush holders, brushes, pole tips, and so forth) may attain such temperatures as will not injure the machine in any respect.															

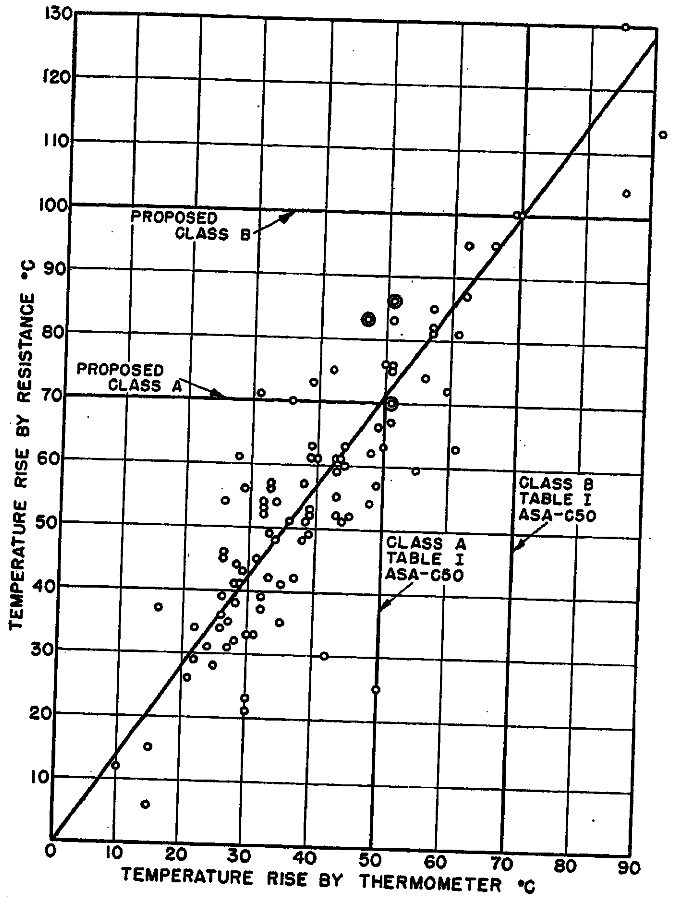


Figure 1. Armature temperature rise by resistance versus rise by thermometer. Composite curve, data from all participating companies

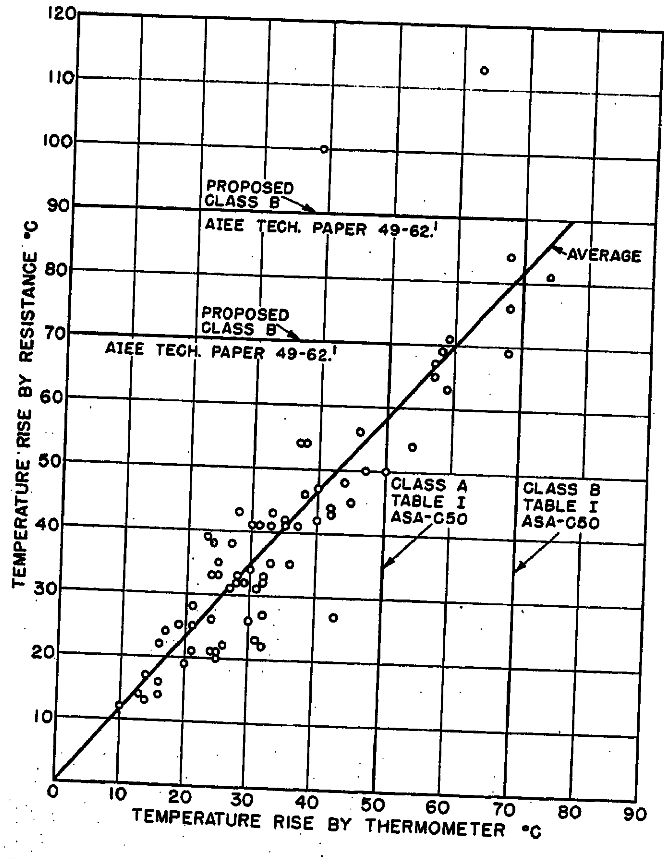


Figure 2. Shunt field temperature rise by resistance versus rise by thermometer. Composite curve, data from all participating companies

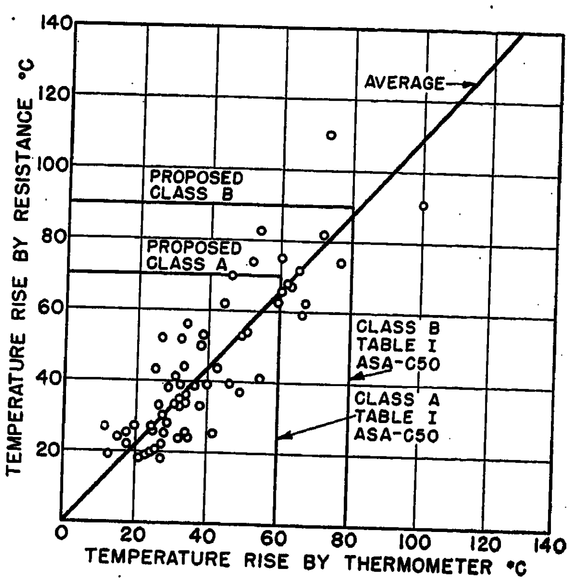


Figure 3. Commutating field temperature rise by resistance versus rise by thermometer. Data reported by company A

method was determined in accordance with AIEE Test Code 501.[3]

Plotting Data and Correcting to Instant of Shutdown

The hot-resistance readings minus the cold resistance are plotted on semilog graph paper against the elapsed time from the start of the shutdown. The resulting graph can be extended back to the instant when shutdown was begun showing approximately the temperature at this instant. In general the temperature decreases exponentially from the initial hot value to the final cold value. There are some relatively quick changes immediately after shutdown and the true curve

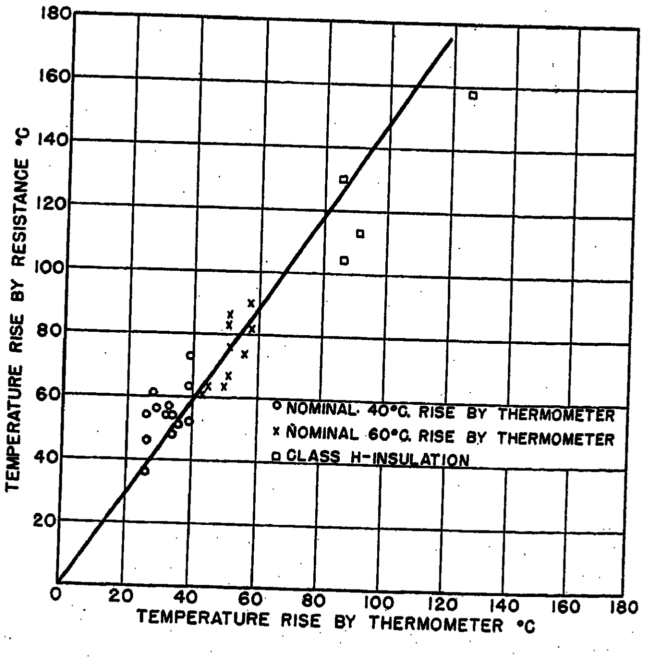
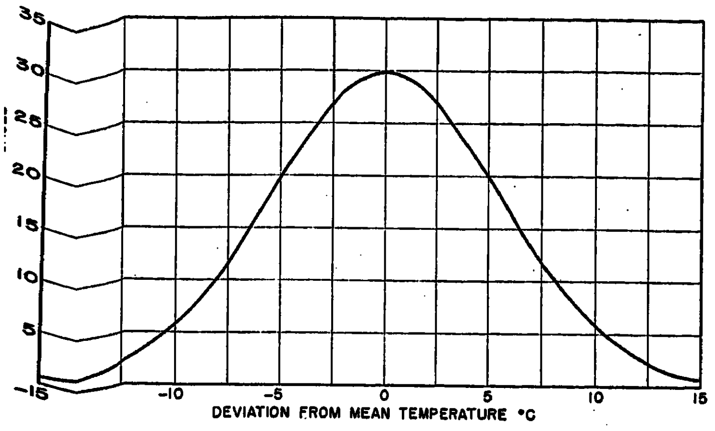
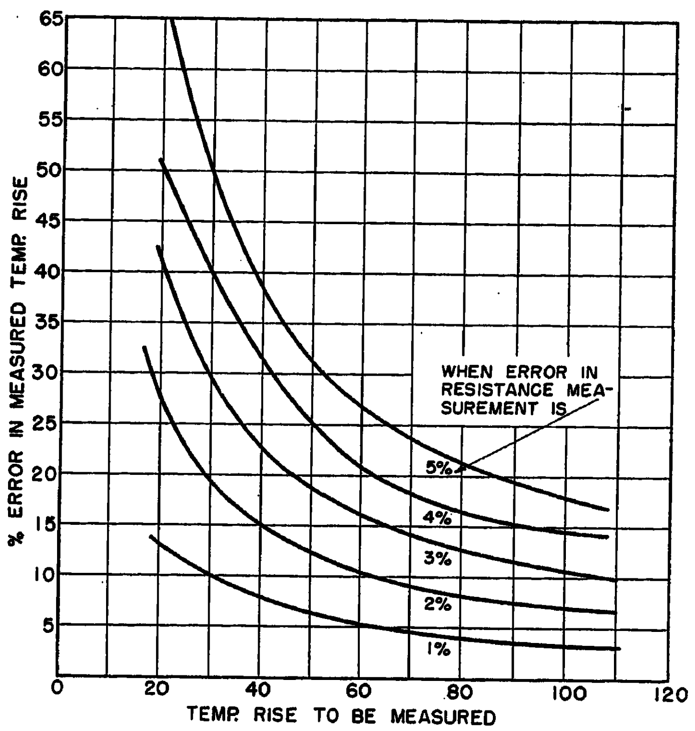


Figure 4. Typical plot of data covering armatures with mica insulation having nominal rise by thermometer of 40, 60, and 95 degrees centigrade



igure 5 (above). Probability curve for armature temperature rise by esistance method. Results of tests on 413 machines. Mean temperature rise = 76.5 degrees centigrade



igure 7 (right). Per cent error in measured temperature rise resulting om a given error in measured resistance as a function of the magnitude of the temperature rise to be measured

ntains several components with different exponentials. If only one longme component exists, the data will plot 1 semilog paper as a line which can be ntinued readily back to the initial inant of shutdown. It has been generally ceptable to plot the data on semilog per and to draw a line through the ints, extending this line back to the tial instant. This averages out the or in individual readings and, therefore, es a more accurate reading for temperare at the initial instant assuming the ick initial changes of temperature to be gligibly small.

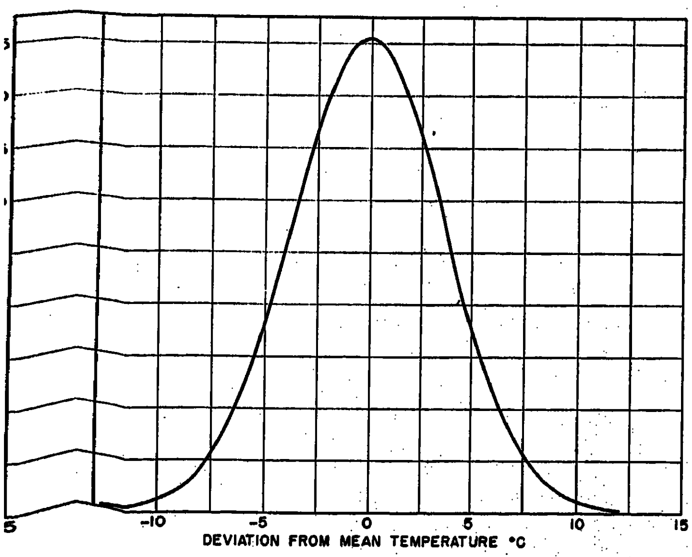
To obtain a correct line using semilog per and as a matter of fact whenever nilog paper is used, only the difference tween the temperature reading and the final cold temperature (room temperature) should be plotted for each instant of time. Otherwise a straight line cannot be drawn through the points and the temperature at the initial instant will not be correct. The resistance readings should never be plotted directly. The true conditions are

$$T_x = T_C + (T_H - T_C)\epsilon^{-\sigma t} \text{ (by assumption)}$$

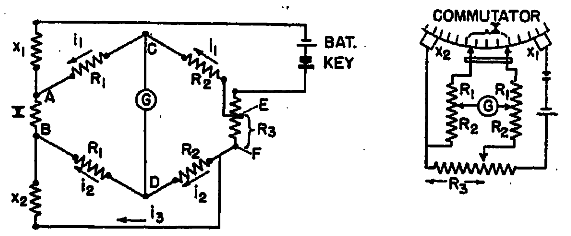
where

T_x = temperature at any instant
T_C = temperature cold
T_H = temperature hot
t = time in seconds
σ = reciprocal of thermal time constant

T_x plotted against time will be a curved line on either linear or semilog paper. A similar plot of $T_x - T_C$ on semilog graph



ıre 6. Probability curve for shunt field temperature rise by resistance hod. Results of tests on 420 machines. Mean temperature rise = 49.7 degrees centigrade



X = RESISTANCE TO BE MEASURED
x_1 = LEAD, BRUSH, AND PART WINDING RES.
x_3 = LEAD, BRUSH, AND PART WINDING RES.
R_1 = BRIDGE RATIO RES.
R_2 = BRIDGE RATIO RES.
R_3 = KNOWN VARIABLE RES.

$R_1 = KR_2$ WHERE K = BRIDGE RATIO SET BY SWITCH
WHEN BRIDGE IS BALANCED:
POINTS C AND D ARE AT SAME POTENTIAL.
$R_2 i_1 = R_3(i_2 + i_3) + R_2 i_2$
$R_1 i_2 + \mathrm{X}(i_2 + i_3) = R_1 i_1$
THEN:
$R_1 i_2 + \mathrm{X}(i_2 + i_3) = KR_3(i_1 + i_3) + KR_2 i_2$
$KR_2 i_2 + \mathrm{X}(i_2 + i_3) = KR_3(i_2 + i_3) + KR_2 i_2$
OR:
$\mathrm{X} = KR_3$

THE SAME CURRENT MUST FLOW THROUGH X AS FLOWS THROUGH R_3. THEREFORE, THERE MUST NOT BE A SHUNT AROUND R_3 UNLESS IT IS CONNECTED FROM E TO F AND THE COMBINED RESISTANCE USED AS THE VALUE OF R_3.

Figure 8. Double-bridge connections

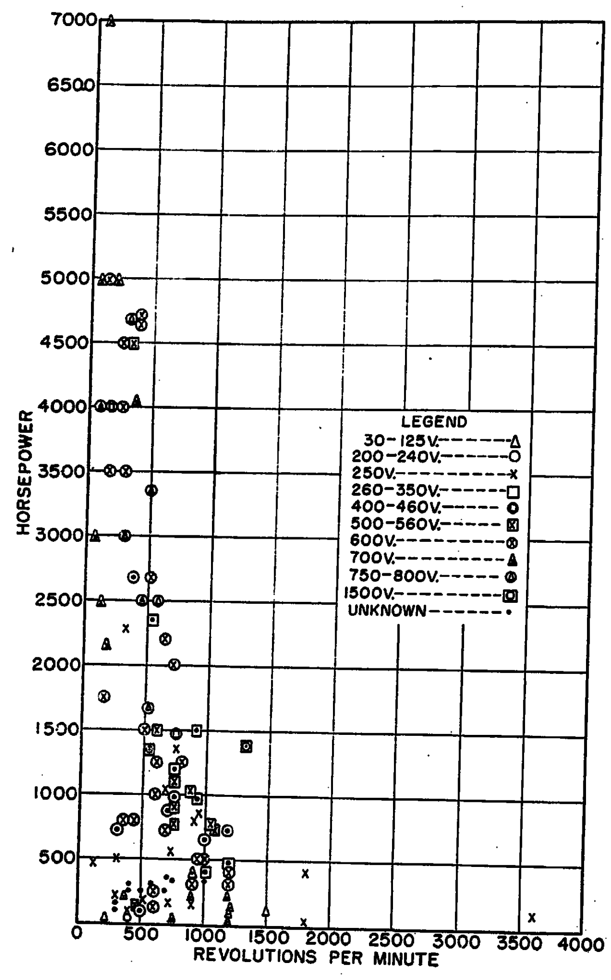


Figure 9. Horsepower, revolutions per minute, and voltage ratings of machines tested

paper will be a straight line. Furthermore

$$T_x - T_C = \frac{r - R_C}{R_C}(234.5 + T_C)$$

$$= (T_H - T_C)\epsilon^{-\sigma t}$$

where

r = the resistance at any instant corresponding to the temperature T_x

R_C = resistance cold at temperature T_C

Then

$$r - R_C = \frac{R_C(T_H - T_C)}{234.5 + T_C}\epsilon^{-\sigma t} = K\epsilon^{-\sigma t}$$

Therefore, if from each resistance reading the cold resistance is subtracted and the remainder plotted at each instant of time on semilog paper, a straight line may be drawn through the points and continued back to the initial instant of shutdown. Other ways of plotting the resistance-time data are likely to lead to an appreciable error resulting in an amplified error in the calculated temperature rise.

A straight line must not be taken for granted as curvature will be caused by a complex thermal time constant. For this reason there should be as little time as possible between shutdown and the first reading of hot resistance.

Results

Tests on 116 machines from about 4 horsepower to 7,000 horsepower and from about 100 to 1,200 rpm are reported. The horsepower, root-mean-square, and voltage of these machines are plotted in Figure 9 to show clearly the ratings of the machines tested. Results of tests are given in Figures 1 to 3 inclusive showing in each case the temperature rise by the resistance method plotted against rise by thermometer method. Lines are drawn to show the ratio of rise by resistance to rise by thermometer. In addition, lines are drawn to show the standard thermometer value and the corresponding resistance value for Table I. This corresponding value in each case is determined from the point of intersection of the line for the standard thermometer value with the line for the ratio. Where these values are lower than those given by the results of the earlier tests (see Table 1, reference 1) the former values are held. Values are rounded off to nearest preferred numbers in each instance. The average ratio of rise by resistance to rise by thermometer reported by company *A* was 100/74 for the armature winding (11 machines) and 100/84 for the shunt field winding (55 machines). Company *B* reported average ratios of 100/89 (5 machines) and 100/84 (7 machines), respectively. Company *C* reported on 72 machines tested at three separate factories showing 100/70 for armature windings for machines with either 40-degree nominal rise or 60-degree nominal rise by thermometer method. A typical plot of this data including four machines with class-*H* insulation is shown in Figure 4. The armature insulation for all of these machines is described as standard class *B* or better, the difference between the 40-degree and 60-degree machines being chiefly in the heat generated in the parts. The ratio for the shunt field winding reported by company *C* is 100/89 from tests on six machines. Company *D* reported 100/85 for armature windings as the average for 12 machines. The ratio for the commutating field was reported by company *A* only showing 100/91 for the average of 61 machines plotted in Figure 3.

It should be kept in mind that substantially all of the data was taken from the records of the companies and, therefore, are not the results of a preplanned test program with carefully followed uniform procedure. Hence, some of the data possibly have relative errors especially because of the correction of the results to the instant of shutdown. It follows that the data should be looked at in the aggregate. In the case of each company the range of results is broad as can be seen from the nature of the composite data of Figures 1, 2, and 3.

Values for short-time ratings are repeated from the previous paper.[1]

The combined reliability and accuracy of the resistance method is indicated by the spread of the data shown on Figures 1 to 4, inclusive. These figures, of course, also include the inaccuracies of the thermometer method and the variations produced by a wide variety of machines with structural differences. However, tests were made on 420 machines consisting of six groups of like machines all having a mean temperature rise by resistance of approximately 80 degrees centigrade. A statistical analysis was made of the results. The statistical data are plotted in Figures 5 and 6 showing the number of machines out of any total number of machines tested which can be expected to vary from the true result by any given percentage error. It is apparent that the resistance method is reliable in a single test within only an appreciable range of error which may be above or below the true result. Thus, a number of repeated heat run tests must be made for any single machine using the resistance method or a number of duplicate machines must each be tested. Therefore, the method is not generally appropriate or economical but may be useful when a number of like machines are being built. Factories making groups of like machines where special equipment and trained operators are justified may find the method practical, whereas for d-c machines in general the thermometer method is more suitable. Nevertheless, until proper values for the method are recognized, it is useful in no instances.

The necessity for especially accurate measurement when using the method is clearly shown by Figure 7.

Class *H* is omitted from Table I as the basic thermometer values are not yet established. When these are established the corresponding values for the resistance method will be given by multiplying the thermometer values by the same ratios as used to determine class-*A* and -*B* values.

Appendix

Recommended Test Procedure for Resistance Method for Field Windings

On field windings, read the true current in the field coils excluding low-voltage relay or other devices or current shunts connected across the field winding.

Read the voltage directly across the field circuit excluding rheostats and any series connected devices and cables.

Read the cold resistance and hot resistance with the same meters and the same connections.

Recommended Test Procedure for Resistance Method for Armature Windings

On armature windings measure resistance between commutator segments about 90 electrical degrees apart midway between brush arms.

Take cold and hot readings with same instruments, leads, and so forth.

After temperature of stator parts has become constant, shut machine down fast maintaining load current until nearly stopped. Start resistance readings as quickly as possible. Have contact points built into a yoke which holds them a fixed distance apart. Record resistance reading and the elapsed time from the start of the shutdown.

Record resistance and time at intervals of about two minutes until at least five readings are obtained.

Mark the exact position at which the hot resistance was taken and after the machine has cooled to room temperature measure the cold resistance with the contacts in the same position with the same instruments, leads, and so forth.

Use a double bridge with very sensitive external galvanometer connected as shown in Figure 8. Remember that the ratio of hot to cold resistance in the usual case is only 1.12 to 1.25 per cent and the temperature is proportional to the hot resistance minus the cold resistance so that any error in measuring resistance is multiplied 4-to-8 times when the readings are converted to temperature rise.

Be sure there is no shunt across the balancing (or slide wire) resistance of the double bridge, see Figure 8.

Subtract the cold resistance from each value of hot resistance. Plot the difference against time on graph paper with logarithmic scale for resistance and linear scale for time. Draw the best line through the points and determine the intersection at time zero. This is the differential resistance at zero time.

Divide this resistance by the cold resistance and multiply by the sum of 234.5 plus the ambient temperature in degrees centigrade. This gives the mean temperature rise of the armature winding.

References

1. Temperature Rise Values for D-C Machines. An AIEE Committee Report. *AIEE Transactions*, volume 68, part I, 1949, pages 206–18.

2. Rotating Electrical Machinery, C50. American Standards Association (New York, N.Y.), 1943.

3. Test Code for D-C Machines. *AIEE Standard Number 501*, 1941.

Discussion

A. T. McClinton (Naval Research Laboratory, Washington, D. C.): This report states that the change in resistance method of measuring temperature is unreliable and inaccurate, and thus preference is given the thermometer method. Unfortunately, little is said concerning the reasons for this inaccuracy nor are precautions given which should be taken in overcoming them. It must be recognized, however, that under certain conditions it is almost mandatory to employ the change in resistance method for measuring temperature, and therefore its limitation should be recognized and methods for improving the accuracy made available to everyone through this paper. Our own experiences at this Laboratory have shown that there are other sources of error in its use other than that introduced by the equation itself. If these are corrected, satisfactory results can be obtained.

On small machines it has been found highly desirable, and in certain cases necessary, to remove the brushes before making resistance measurements on the armature. An inspection of the schematic diagram of the armature circuit will reveal that under some conditions it is possible to have the armature circuit resistance paralleled by brush contact and lead resistance. Naturally the presence of these in the circuit will introduce error. It is recognized that in larger machines it will be impractical to remove the large number of brushes and therefore this precaution cannot be taken. However, it should also be noted that in the larger machines such a precaution is not necessary to provide the necessary accuracy.

A second source of error to be considered in making these measurements is the introduction of voltages in the galvanometer circuit through thermocouple action at the junction of the probes and commutator surface. Our experience has shown that probes, although supposedly made of identical material, when placed on the hot commutator will give voltages that are high enough in magnitude to seriously unbalance the measuring circuit. If we recognize the sensitivity required for the galvanometer circuit it becomes quite obvious that only small voltages introduced by thermocouple action can create a sizable error as well as erratic error in the measurement. It is believed that this source of error may overshadow any other that may enter into the measurement of temperature by change in resistance measurement, and that extreme care should be taken to assure that this thermocouple action does not exist.

M. A. Baker (General Electric Company, Fort Wayne, Ind.): One of the conclusions reached in this report was that the resistance method is not generally appropriate or economical for use on machines manufactured in small quantities. In the Fort Wayne Large Motor and Generator Division of the General Electric Company, we build a great variety of machines in small quantity manufacture, usually in lots of one or two. We agree thoroughly with the conclusion of this Subcommittee based on our own experience with temperature rise by resistance measurements over a period of about eight years. We look at the resistance method as a specialized tool by which we can get additional knowledge of our machines, but feel that the thermometer method should be maintained as the standard for guarantees because it is inherently more reliable with a limited number of tests. This was observed in the data presented in the counterpart of this report presented at the 1949 winter meeting.[1] In discussing this report the writer pointed out that in spite of the unusual care and laboratory equipment used in making these tests, the per cent variation between the two independent tests was greater for resistance rise than for the thermometer rise, on armatures and commutating fields. This should not be surprising when we realize, as was pointed out in this report, that errors made in measuring resistances are magnified many times when converted to temperature rise. On shunt fields where resistance is much higher, and where it can be determined prior to shut down, it is much easier to obtain accurate data, and rise by resistance is a practical test for main field coils.

The data presented in the report[1] shows that the ratio of thermometer rise to hot spot is just as definite as the ratio of resistance rise to hot spot, so with proper allowances set up, the thermometer method is a reliable measure of hot-spot temperature. In view of this, the simplicity of the test and its greater reliability (at least in

Table I. Proposed New Table

The temperature rises in degrees centigrade in this table apply to the several classes of machines as given at the heads of the respective columns Item Machine Part	Method of Temperature Determination	General Purpose Generators and Motors		Totally Enclosed, Totally Enclosed Fan-Cooled, Explosion-proof, Waterproof, Dust-tight, and Submersible Motors Continuous Rated		Motor and Generators Rated 30 Minutes to 2 Hours Inclusive		Motors and Generators Rated 15 Minutes or less		Generators Having a 2-Hour 25 Per Cent Overload Rating at Continuous Load		Generators Having A 2-Hour 25 Per Cent Overload Rating and Generators Having a Nominal Rating at End of 2-Hour Overload		Special Purpose Drip-proof and A Motors and Generators Other Than Col. 1–12 Inclusive Continuous Rated	
		Class A Insulation Col. 1	Class B Insulation Col. 2	Class A Insulation Col. 3	Class B Insulation Col. 4	Class A Insulation Col. 5	Class B Insulation Col. 6	Class A Insulation Col. 7	Class B Insulation Col. 8	Class A Insulation Col. 9	Class B Insulation Col. 10	Class A Insulation Col. 11	Class B Insulation Col. 12	Class A Insulation Col. 13	Class B Insulation Col. 14
1…Armature windings and all windings other than 2 and 3	Thermometer See 2.054	40		55	75	55	75	55	75	40	60	55	75	50	70
	Resistance See 2.055			70	100	80	110	90	125		90		110	70	100
2…Shunt field windings	Thermometer See 2.054	40				55	75	55	75	40	60	55	75	50	70
	Resistance See 2.055	60		70	100	80	110	90	125	60	90	80	110	70	100
3…Single-layer field windings with exposed uninsulated surfaces and bare copper windings	Thermometer See 2.054	50		65	85	65	85	65	85	50	70	65	85	60	80
	Resistance See 2.055			70	100	80	110	90	120		90		110	70	90
4…Cores and mechanical parts in contact with or adjacent to insulation	Thermometer See 2.054	40				55	75	55	75	40	60	55	75	50	70
5…Commutators and collector rings	Thermometer See 2.054	55	75	65	85	65	85	65	85	55	70	65	85	65	85
6…Miscellaneous parts (such as brush holders, brushes, pole tips, and so forth) may attain such temperatures as will not injure the machine in any respect.															

cases when only one or two runs are available) we in the Large Motor and Generator Division, Fort Wayne, heartily endorse the recommendation that the thermometer method be considered as the preferred or standard test procedure.

We have had a double standard (optional with manufacturer) for some time on main field temperature rating. Since rise by resistance is a practical test on main field coils we do not seriously object to this as long as proper limits are established. Based on our experience and data presented we concur with the proposed increased values of rise by resistance for shunt fields as long as the method of test is optional with the manufacturer. However, since resistance tests are not practical for production tests on low-resistance circuits, we recommend deleting the rise by resistance values for armatures and single layer field windings. Dual standards tend to cause trouble and misunderstanding, and we can see no benefits to be gained by leaving rise by resistance as an optional standard for general-purpose d-c machines. If rise by resistance is desirable on specialized lines of machines, a special standard should be prepared to cover these lines.

The temperature rise of any part of a machine should correspond to the insulation on that particular part. Most commutators are class-*B* insulated, and we recommend that class-*B* temperatures be allowed on all class-*B* commutators even when used in class-*A* insulated machines.

In concluding I would like to make a few remarks regarding the calculation of rise by resistance. I think this report overemphasizes the straight line relationship between the differential resistance and time. It has been our experience on a wide variety of d-c machines that the slope of the cooling curve during the first two or three minutes is appreciably different from the slope after this time. In fact, if we do not get at least two, or preferably three, points in the first two minutes, we do not consider the test satisfactory. The computation method given in this report gives the temperature rise above the ambient at which the cold resistance was measured. If the ambient has increased during the heat run, the results should be corrected by subtracting the difference between the final and initial ambient. If the final ambient is lower, the difference should be added to the calculated temperature rise

REFERENCE

1. TEMPERATURE RISE VALUES FOR D-C MACHINES, AIEE Committee Report. *AIEE Transactions*, volume 68, part I, 1949, pages 206–18.

B. H. Caldwell, Jr.: The AIEE Subcommittee on D-C Machines met immediately following the presentation and discussion of the report.

It was decided that the Proposed New Table should be modified. See Table I of the discussion.

The principal changes are:

Rise by Resistance for Armature Windings

Measurement of temperature rise by resistance for armature and single layer field windings is not recommended for d-c machines having a guarantee of 40-degree centigrade rise by thermometer for continuous load. This means that the temperature rise by resistance values will be omitted in columns 1, 9, and 11 for items 1 and 3.

It is for the low temperature rise that the percentage error in measured rise is greatest for a given error in the measured resistance, see Figure 7 of the paper. It is felt that the resistance method is not practical at this time for 40-degree centigrade rise machines. This change in the table removes most of the objections to use of the resistance method.

It was decided that the remaining resistance values should be included in the table as an alternate method.

Rise by Resistance for Shunt Windings

Since the shunt field is relatively a high-resistance winding, use of the resistance method meets with essentially unanimous approval. The table proposed in the report specified values of rise by resistance consistent with data obtained from the test program as being equivalent to the present limit as measured by thermometer method. This results in lower rise by resistance for the shunt field than for the armature. Since the shunt field winding is not subject to mechanical stress nor to as severe overloads as the armature winding, it is logical to establish the limits for the shunt field winding at least as high as for the armature winding. It is, therefore, recommended that the limits for temperature rise by resistance for the shunt field winding be made the same as for the armature winding.

Machines with Two or More Classes of Insulation

At least for the larger machines most modern commutators are insulated with class-*B* materials. A liberal interpretation of American Standards Association *C50*, 1943,[1] would seem to permit class-*B* temperature rise for such commutators even though they are connected to an otherwise class-*A* machine. It is recommended that this be made clear in the next revision of ASA *C50*. The same principle applies to other components having different classes of insulation.

In all cases it should be made clear that the temperature limits here proposed are intended to represent only the safe limit for satisfactory insulation life. It is quite possible that other considerations will impose a lower limit. For example, it would not in general be safe to attempt to operate an existing commutator, which was built for a temperature rise of 55 degrees centigrade, at a higher temperature rise, since adequate provision for the increased thermal expansion may not have been made in the original design and construction. In general other parts of a machine may have similar limitations.

It remains the manufacturer's responsibility to construct the machine so that not only is satisfactory insulation life obtained, but also that other factors which influence satisfactory operation are kept within safe limits. The user must recognize that insulation temperature is only one of the load limiting factors, and should consider all important factors before making a decision to operate a machine beyond its original rating.

The Proposed New Table as modified in this discussion is recommended as a basis for revision of Table A, ASA *C50*, 1943, page 10. Favorable action on this proposal by the Standards Committee is requested.

Reference

1. See reference 2 of the report.

Progress Report on AIEE Test Code for Electric Brushes

AN AIEE COMMITTEE REPORT

Synopsis: This report presents material which the Joint Subcommittee recommends for inclusion in a test code for electric brushes. It is presented to secure general discussion preliminary to formulation of a trial test code. The material presented generally represents the viewpoint and agreement of the AIEE Joint Subcommittee. Items which the Subcommittee agrees are desirable for inclusion in the test code but upon which further work must be done are indicated.

THE AIEE Committee on Electric Brushes, a joint subcommittee of the Power Rotating Machinery Committee and the Air Transportation Committee has for its major assignment the development of a test code for electric brushes. The personnel of the committee includes members of the technical group of the carbon section of the National Electrical Manufacturers Association (NEMA) so there has been continuous liaison with the electric brush manufacturers. It is the intention of both groups to bring their standards into agreement in those areas in which both groups are interested. The committee has adhered to the principle that an AIEE test code on electric brushes should define standard procedures for making tests and not be concerned with specifications concerning acceptable test results.

Much experimental work remains to be done before a complete and satisfactory test code on electric brushes can be written. The material of this progress report includes those items upon which there is general agreement and suggests additional items for which standard procedures should be prepared, but of which insufficient knowledge is available for standardization at the present time. The committee will procede with the study of these additional items.

Table of Contents

1.000 Purpose
2.000 Measurements on Material Specimens
  2.100 Sampling Schedule for Material Specimen Tests
  2.200 Material Specimen Test Procedure
    2.210 Specific Resistance
      2.211 Voltmeter-Ammeter Method (Standard Method)
      2.212 Kelvin Bridge Method (Alternate Method)
    2.220 Density
      2.221 Apparent Density
      2.222 Immersion Method (Alternate)
      2.223 Real Density—Oil Absorption Method
    2.230 Porosity
    2.240 Scleroscope Hardness
    2.250 Transverse Strength
    2.260 Percentage Ash
3.000 Tests on Brush Assemblies
  3.100 Shunt connection millivolt drop
  3.200 Vibration Test on Shunt Connection
4.000 Sliding Contact Operating Characteristics
  4.100 Test Equipment
    4.110 Slip Ring
    4.120 Brush Holders
    4.130 Test Brushes
    4.140 Equipment for Friction Measurement
    4.150 Equipment for Ring Surfacing
    4.160 Air Conditioning Equipment
  4.200 Preparation for Tests
    4.210 Preparation of Ring Surface
    4.220 Method of Seating Brushes
  4.300 Tests
    4.310 Contact Drop
    4.320 Coefficient of Friction
  4.400 Test Schedule
5.000 Life Tests
6.000 Commutating Ability Tests
7.000 Altitude Tests
  7.100 Standard Test Conditions
    7.110 Temperature
    7.120 Air Pressure

1.000 Purpose

It is the purpose of this brush test code to define procedures for measuring the physical properties of electric brush materials, for determining the quality of brush assemblies, and to test the performance of brushes as part of the sliding contact. Since some of the tests are empirical, and since the test results vary with many test conditions, it is desirable to standardize test procedures to reduce these variables to a minimum. Test data based on standard procedure should facilitate comparison of various brush grades and assist designers in the selection of the most satisfactory brush for the machine application and operating condition involved.

2.000 Measurements on Material Specimens

Tests of the physical properties of brush materials are made primarily for the purpose of checking manufacturing processes to obtain a uniform product since it is not yet possible to correlate closely the physical properties of brush materials with the operating characteristics of the brush.

Paper 49-234, recommended by the AIEE Rotating Machinery Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted November 15, 1948; made available for printing September 21, 1949.

Personnel of the AIEE Joint Subcommittee on Electric Brushes: V. P. Hessler, *Chairman;* W. H. Austry, H. R. Brown, W. E. Clancy, J. V. Dobson, H. M. Elsey, C. J. Herman, L. H. Hildebrandt, H. E. Keneipp, E. A. Lapham, T. M. Linville, D. Ramadanoff, Orrin Rutledge, S. D. Summers, A. L. Van Emden.

2.100 SAMPLING SCHEDULE FOR MATERIAL SPECIMEN TESTS

Table I

Number of Specimens in Lot	Number of Samples
1– 39	1
40–109	2
110–179	3
180–299	5
300–499	7
500–799	10

2.200 MATERIAL SPECIMEN TEST PROCEDURE

2.210 *Specific Resistance*

2.211 *Voltmeter-Ammeter Method* (*Standard Method*). Test specimens shall have, where practicable, a length of not less than four times the width or thickness. Direct current shall be passed through the specimen from end to end through contact plates which make contact across the entire area of both ends of the specimen. A good electrical contact with the carbon may be obtained with contact plates of silver-plated copper gauze, having a backing of 1/4-inch thick neoprene, or similar resilient material. The magnitude of the current shall be sufficient to give a satisfactory millivolt reading without overheating the specimen. A current of 2.5 amperes is satisfactory for a 1/2-inch square specimen. Sharp potential pointers shall be used, and the distance between the points shall not be less than either the width or thickness dimensions or greater than 50 per cent of the length of the specimen. The voltage or millivolt drop shall be taken by means of a potentiometer-type meter or a relatively high resistance voltmeter. The specimen should be tested at a temperature of 30 degrees centigrade, ±5 degrees centigrade.

The resistivity in ohm-inches shall be calculated by the use of the following equation:

$$\rho=\frac{Ewt}{Il}$$

where

ρ = resistivity in ohm-inches
E = voltage drop in volts
w = width of the specimen in inches
t = thickness of the specimen in inches
I = current in amperes
l = length between potential pointers in inches

2.212 *Kelvin Bridge Method* (*Alternate Method*). A Kelvin Bridge may be used to determine the resistance of the specimen instead of using the voltmeter-ammeter method. Care must be taken to avoid overheating the specimen. Follow the procedure outlined for the voltmeter-ammeter method and calculate the resistivity by the following equation:

$$\rho=\frac{Rwt}{l}$$

where R is the resistance in ohms measured between the potential pointers and the other quantities correspond with those of the voltmeter-ammeter method.

2.220 *Density*

Manufactured carbon is inherently a porous material and therefore both real and apparent density tests are useful for purposes of quality control.

2.221 *Apparent Density.* Finish the specimen to a shape such that the dimensions can be measured accurately with a micrometer. Weigh the specimen in grams to four significant figures. Calculate the volume in cubic centimeters and determine the apparent density by dividing the weight in grams by the volume in cubic centimeters.

2.222 *Immersion Method* (*Alternate*). Since this method of determining apparent density involves the determination of volume by water displacement, an irregularly shaped sample may be used. No preparation is necessary other than observing that the sample is clean, dry, and free from metal or other foreign material. Weigh the sample to four significant figures. Then coat the sample with thin grease or other waterproof masking material to prevent absorption of water. Suspend the sample in water from a balance with a number 40 thread. The sample must be completely immersed and all air bubbles must be removed from the surface of the sample. Weigh the sample to four significant figures while immersed. Calculate the apparent density as follows:

$$\text{Apparent density}=\frac{\text{Weight in air}}{\text{Weight in air—Weight in Water}}$$

2.223 *Real Density—Oil Absorption Method.* Real density is defined as the ratio of the weight of the specimen in grams to the real volume of the specimen material, that is, to the total volume less the volume of the interstices. The volume of the interstices is determined from the weight of oil of measured density which is absorbed by the specimen.

The following equipment is needed:

1. Micrometer.
2. Scale or balance with range of about 100 grams and sensitivity of 0.01 gram.
3. Hydrometer having a range of 0.800 to 0.900 at 20 degrees centigrade and graduated to 0.001.
4. Hydrometer cylinder 40 millimeters in diameter and 300 millimeters long.
5. Thermometer with range of zero to 100 degrees centigrade.
6. Vacuum flask of 1,000 cubic centimeter capacity.
7. Vacuum pump capable of producing a vacuum equivalent to 740 millimeters of mercury.
8. Water bath held below 20 degrees centigrade.
9. Petroleum oil grade *SAE-10, Texaco Regal B*, or equivalent.

Finish the specimen to a shape such that its dimensions can be determined accurately with a micrometer. Measure the specimen and calculate its total volume in cubic centimeters. Record this volume as V_t. Weigh the specimen in grams to four significant figures. Record this weight as W_1. Place the specimen in the vacuum flask and cover completely with oil. Place thermometer in the flask with its bulb immersed in the oil. Apply heat with care to heat the oil slowly to 70 degrees centigrade. While heat is being applied, connect the pump to the flask and increase the vacuum gradually to a value equivalent to about 740 millimeters of mercury. Care must be used to avoid undue foaming in applying the vacuum. Maintain the 70 degrees centigrade and vacuum until the specimen stops emitting bubbles. Three to five hours are usually required. Release the vacuum and cool the flask and contents to room temperature, and then to below 20 degrees centigrade in the water bath. Remove the flask from the water bath and allow the temperature to rise to 20 degrees centigrade. Remove the specimen from the oil, wipe dry, and weigh. Record this weight as W_2. Pour the oil from the flask into the hydrometer cylinder, cool to 20 degrees centigrade if the temperature has risen above this value and read its specific gravity. Record this figure as d.

Calculation of real density

Weight of oil absorbed $=W_0=W_2-W_1$
Volume of oil absorbed $=V_0=W_0/d$ cubic centimeters

$$\text{Real density}=\frac{W_1}{V_t-V_0}$$

If desired the total volume V_t may be determined as in the immersion method by weighing the specimen submerged immediately following the weighing to determine W_2. Record this weight as W_3. Then

$$V_t=W_2-W_3$$

and

$$\text{Real Density} = \frac{W_1}{W_2 - W_3 - V_0}$$

2.230 *Porosity*

The porosity of carbon is defined as the ratio of the volume of the interstices in a given specimen to the total volume of the specimen. The per cent porosity is calculated by the following equation where P represents the percentage porosity, RD the real density, and AD the apparent density:

$$P = \frac{(RD - AD)100}{AD}$$

2.240 *Scleroscope Hardness*

This measurement is made with the Shore, *Model C-2*, Scleroscope, equipped by the manufacturer with a special type of hammer for carbon. In making the tests, the instrument is mounted on a firm support and levelled by means of levelling screws so that the plumb rod is in the center of the ring. The anvil of the instrument must be kept clean. When round pieces are to be tested, a Vee block is used. All specimens must be surface ground before testing. Test pieces are placed in the instrument so that the hammer impact is applied in the direction of molding compression in the case of brush plate materials and perpendicular to the axis of extrusion for extruded materials. To make the test, the specimen is placed on the anvil or in the Vee block and the tube is lowered firmly against the specimen. Release the hammer and read the height of rebound. Take at least six readings at various spots on each side of a rectangular block and record the average as the scleroscope hardness. On round pieces take at least six readings at various places and record the average as the scleroscope hardness.

2.250 *Transverse Strength*

The transverse strength test is made by applying the breaking force in the direction of molding pressure in the case of specimens cut from plate stock and at right angles to the axis of extrusion in the case of extruded stock. For carbon, electrographitic, and graphite grades the specimen should be $1\frac{5}{8}$ inches long by 0.200 inch by 0.200 inch (+0.000 inch−0.002 inch). For metal graphite grades the specimen should be $1\frac{5}{8}$ inches long by 0.125 inch by 0.125 inch (+0.000 inch−0.002 inch). The direction in which the breaking force is to be applied must be clearly marked on the ends of the specimen at the time of machining.

To make the test on the 0.200 inch by 0.200 inch specimen, support it on two knife edges of 0.010 inch radius spaced 1.2 inches apart and apply the breaking force at a point midway between the two knife edges with a third knife edge of 0.010 inch radius. The force shall be applied at a steady rate not exceeding 500 pounds per minute. Note and record the total force in pounds at which the specimen ruptures.

To make the test on the 0.125 inch by 0.125 inch specimen, adjust the supporting knife edges to a spacing of 0.6 inch and apply the breaking force at a point midway between the supporting knife edges. The force shall be applied at a rate not exceeding 250 pounds per minute. Note and record the force in pounds at which the specimen ruptures.

The transverse strength is calculated with the following equation:

$$\text{Transverse strength} = \frac{3lF}{2Wt^2}$$

where

l = distance between supporting knife edges in inches
F = rupturing force in pounds
W = width of the specimen in inches
t = thickness of the specimen in inches

2.260 *Percentage Ash*

The basic procedure is to heat a representative sample of the material in an oxidizing atmosphere until complete incineration takes place. The remaining ash is then weighed and expressed as a per cent of the initial weight of the sample. Since the ash content of some electrographitic materials may be less than 0.1 per cent, special precautions must be taken to prevent errors in weight.

The apparatus should consist of a small muffle or tube furnace capable of maintaining a controlled temperature of 750 to 850 degrees centigrade and with an inlet for air or oxygen, an analytical balance, a dessicator if the ash is not to be weighed soon after removal from the furnace, and a platinum crucible of such size that the sample does not more than half fill the crucible. A glazed porcelain or equivalent crucible may be substituted for the platinum if a sample two or three times as large is used to minimize error due to change of weight of the crucible.

The sample should be cut from a representative section of plate or brush stock in such a manner that all surfaces of the sample are newly cut. The sample should contain 15 to 25 grams of material, but in any case the residue should not be less than five milligrams. The sample may be crushed to reduce the incineration time but extreme care must be used to exclude foreign matter.

Fire the crucible at red heat, cool and weigh. Place the sample in the crucible and weigh again. Place the crucible in the furnace and maintain the temperature between 750 and 850 degrees centigrade until the carbon is completely incinerated. Air or oxygen should be admitted to the furnace in such a manner that no ash is blown from the crucible. Remove crucible from furnace, cool to room temperature and weigh. If weighing is delayed, place crucible in desiccator.

$$\text{Percentage ash} = \frac{\text{Weight of residue}}{\text{Weight of sample}} \times 100$$

Sections 3.000 to 7.400 as shown in the Table of Contents are considered essential to a complete code and are being worked out. A trial test program is to be planned and carried out jointly with a committee of NEMA. It is hoped that the AIEE and NEMA committees will be combined officially to form a single operating group and that the final code can be issued for general trial within a year.

No Discussion

Differential Leakage of the Different Patterns of a Fractional Slot Winding

M. M. LIWSCHITZ
MEMBER AIEE

WHEN the conditions for balance are satisfied, a fractional-slot lap winding can be laid out in many different patterns all of which are balanced. The usual pattern is that for maximum distribution factor of the main wave. If this pattern is called the "basic," all other patterns can be derived from this one by cyclic shifts of coils from one phase to the other within the same repeatable group.[1] The distribution factor of the main wave of the derived patterns, that is, of the patterns with cyclic shift of coils, is always smaller than that of the basic layout. As regards the distribution factors of the harmonics of the derived patterns some are smaller, and some larger than those of the basic pattern.

The use of the patterns with cyclic shift of coils, which reduce the main wave, is justified, when it becomes necessary to reduce one or several electromotive force or magnetomotive force harmonics. However, it is generally assumed that the patterns with cyclic shift of coils have a smaller differential leakage than the basic pattern.

The aim of this paper is to show that this assumption is not correct and that the use of patterns with cyclic shift of coils, in order to reduce the differential leakage, cannot be justified.

Differential Leakage of Different Patterns

It has been shown in a previous paper[2] that the differential leakage of a 3-phase fractional-slot winding is given by the equation

$$X_d = \frac{4}{\pi^2} m f_1 N_{ph}^2 \frac{\tau l_e}{p g k_c k_s} 10^{-7} \times C \text{ ohms per phase} \qquad (1)$$

where

$$C = \frac{\beta^2}{4} \sum_{\nu \neq \beta/2} \left(\frac{k_{dpn}'}{\nu} \right)^2 \qquad \nu = 1,2,4,5,7\ldots \qquad (2)$$

when β is an even number, and

$$C = \beta^2 \sum_{\nu \neq \beta} \left(\frac{k_{dpn}'}{\nu} \right)^2 \qquad \nu = 1,5,7,11\ldots \qquad (3)$$

when β is an odd number.

The cyclic shift of coils does not introduce new harmonics nor does it change the pitch factors of the winding. It changes only the distribution factors k_{dn}'.

Therefore, for a comparison of the differential leakage of the different patterns, it is sufficient to compare their magnitudes of C.

A method of calculating the distribution factors for the basic pattern and for the patterns with cyclic shift of coils is given in Appendix 1. Here the results will be given for three 3-phase windings with $2^3/_8$, $1^{11}/_{13}$, and $2^5/_{11}$ slots per pole per phase.

Winding with $q = 2^3/_8$. The coil grouping of phase A for the basic layout is, in 8 poles:

3 2 2 2 2 2 3 3

The calculations were made for the basic layout and for three cyclic shifts of coils which yield the following coil grouping for phase A in 8 poles:

3 2 2 3 2 2 2 3 cyclic shift 1
3 2 3 3 2 2 3 2 cyclic shift 2
3 2 2 2 2 3 2 3 cyclic shift 3

The distribution factors of the harmonics k_{dn}', up to the slot harmonics of first order, are given in Table I. It can be seen from this table that all cyclic shifts of coils increase the distribution factors of the subharmonics while some of the distribution factors of the har-

Table I. Distribution Factors of a 3-phase Winding with $q = 2^3/_8$ for Different Layout-Patterns

ν	Basic Layout	Cyclic Shift 1	Cyclic Shift 2	Cyclic Shift 3	ν	Basic Layout	Cyclic Shift 1	Cyclic Shift 2	Cyclic Shift 3
1	0.0284	0.0773	0.0727	0.0585	32	0.0269	0.0500	0.0813	0.0970
2	0.0366	0.0868	0.0301	0.0410	34	0.0308	0.0743	0.0544	0.1145
4	0.9560	0.9480	0.9530	0.9250	35	0.0444	0.0742	0.0239	0.0383
5	0.0740	0.0234	0.3760	0.0417	37	0.1916	0.1320	0.1625	0.1260
7	0.0290	0.0776	0.6750	0.1002	38	0.0526	0.0460	0.0003	0.0554
8	0.0260	0.0142	0.0767	0.0316	40	0.0274	0.0637	0.0702	0.0992
10	0.0348	0.0728	0.0455	0.0478	41	0.0264	0.0334	0.0817	0.0577
11	0.0582	0.0391	0.0095	0.0537	43	0.0414	0.0676	0.0235	0.0264
13	0.0882	0.0372	0.0600	0.0317	44	0.0882	0.0372	0.0600	0.0317
14	0.0414	0.0676	0.0235	0.0264	46	0.0582	0.0391	0.0095	0.0537
16	0.0264	0.0334	0.0817	0.0577	47	0.0348	0.0728	0.0445	0.0478
17	0.0274	0.0637	0.0702	0.0992	49	0.0264	0.0142	0.0767	0.0316
19	0.0526	0.0460	0.0003	0.0554	50	0.0290	0.0776	0.0675	0.1002
20	0.1916	0.1320	0.1625	0.1260	52	0.0740	0.0234	0.0376	0.0417
22	0.0444	0.0742	0.0239	0.0383	53	0.9560	0.9480	0.9530	0.9250
23	0.0308	0.0743	0.0544	0.1145	55	0.0366	0.0868	0.0301	0.0410
25	0.0269	0.0500	0.0813	0.0970	56	0.0284	0.0773	0.0727	0.0858
26	0.0320	0.1395	0.1050	0.1890	58	0.0284	0.0773	0.0727	0.0858
28	0.1370	0.0918	0.1140	0.0602	59	0.0366	0.0868	0.0301	0.0410
29	0.1370	0.0918	0.1140	0.0602	61	0.9560	0.9480	0.9530	0.9250
31	0.0320	0.1395	0.1050	0.1890					

Paper 49-235, recommended by the AIEE Rotating Machinery Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted November 8, 1948; made available for printing September 1, 1949.

M. M. LIWSCHITZ is a professor with the Polytechnic Institute of Brooklyn, Brooklyn, N. Y., and also is connected with the Westinghouse Electric Corporation, East Pittsburgh, Pa., as consulting engineer.

The author gratefully acknowledges the assistance in calculating Tables I to IV by Mr. I. A. S. Bokhari, which were a part of his Master Thesis, worked out under the guidance of the author.

Table II. Quantity $C=16\sum_{\nu\neq 4}\left(\frac{k_{dpn'}}{\nu}\right)^2$ of a 3-phase Winding with $q=2^3/_8$ for Different Layout-Patterns

W/τ	Basic Layout	Cyclic Shift 1	Cyclic Shift 2	Cyclic Shift 3
Without Subharmonics				
0.983	0.0218	0.0176	0.0182	0.0176
0.842	0.0186	0.0165	0.0170	0.0176
0.701	0.0175	0.0155	0.0172	0.0172
With Subharmonics				
0.983	0.0263	0.0458	0.0320	0.0286
0.842	0.0224	0.0374	0.0278	0.0259
0.701	0.0199	0.0307	0.0244	0.0241

List of Symbols

$A_{n'}$ = magnitude of the electromotive force or magnetomotive force of a phase part
d = difference between two adjacent vectors of the slot star
D = distance between the axes of the phase parts
f_1 = line frequency
g = air gap
k_c = Carter factor
k_s = saturation factor
$k_{dn'}$ = distribution factor of the n'th harmonic
$k_{dpn'}$ = distribution times pitch factor of the n'th harmonic
l_e = effective core length
m = number of phases
n' = order of the harmonic with respect to a fundamental, the wave length of which is equal to $2p\tau$
N_{ph} = number of turns per phase
N = numerator of the fraction which fixes the number of slots per pole per phase = number of slots per phase in β poles
N' = number of slots in a phase part
p = number of pole pairs
P = smallest integer which makes d an integer
q = number of slots per pole per phase
W = coil width measured in units of τ
x_d = differential leakage
α_m = angle between two vectors of the slot star for the main wave
$\alpha_{n'}$ = angle between two vectors of the slot star for the n'th harmonic
β = denominator of the fraction which fixes the number of slots per pole per phase = number of poles in a repeatable winding part
$\theta_{n'}$ = angle between the axes of the phase parts for the n'th harmonic

monics of higher order than the main wave are smaller, and others larger than those of the basic layout.

Table II shows the quantity

$$C=16\sum_{\nu\neq 4}\left(\frac{k_{dpn'}}{\nu}\right)^2$$

that is, the relative differential leakage, for the four different layout patterns at three different coil pitches W/τ. It can be seen from this table that, with the subharmonics neglected, the differential leakage of the patterns with cyclic shift of coils is somewhat smaller than that of the basic layout and that, with the subharmonics taken into account, the differential leakage of the patterns with cyclic shift of coils is larger than that of the basic layout. It is true that the harmonics of low order and especially the subharmonics, are more damped by a squirrel-cage rotor or by eddy currents than the harmonics of higher order. However, very little gain, if any, with respect to the differential leakage can be expected from the cyclic shift of coils for the winding considered.

Table III shows the quantity C for a 3-phase winding with $q = 1^{11}/_{13}$ at three values of W/τ. The first column refers to the basic layout, the second column to a pattern with a cyclic shift. The coil grouping of phase A is in 13 poles:

2 2 1 2 1 2 2 2 2 2 2 2 2

for the basic layout and:

2 2 1 2 2 2 2 2 1 2 2 2 2

for the pattern with cyclic shift. For this winding, the differential leakage of the pattern with cyclic shift is larger than that of the basic layout even when the subharmonics are neglected.

Table IV shows the relative differential leakage C for a 3-phase winding with $q = 2^5/_{11}$ at three different values of W/τ. The first column refers to the basic layout, the second column to a pattern with a cyclic shift. The coil grouping of phase A in 11 poles is:

3 2 3 2 2 3 2 2 3 2 3

for the basic layout and

2 3 2 3 2 3 2 3 2 3 2

for the pattern with cyclic shift. It can also be seen from this table that no reduction of the differential leakage is to be expected from the pattern with cyclic shift of coils.

Appendix I. Determination of the Distribution Factors for the Basic Pattern and for the Patterns with Cyclic Shift of Coils

Basic pattern. In this case the N slots which belong to each phase in β poles lie in the slot star together and the distribution factor of the n'th harmonic is [3]

$$k_{dn'}=\frac{\sin N(\alpha_{n'}/2)}{N\sin(\alpha_{n'}/2)} \tag{4}$$

The vectors of the slot star follow the series 1, 1+d, 1+2d, 1+3d..., where

$$d=\frac{mNP+1}{\beta} \tag{5}$$

and P is the smallest integer which makes d an integer. With the quantities d and P and

$$\alpha_m=\frac{180°}{mN} \tag{6}$$

$$\alpha_{n'}=n'd\alpha_m\frac{\beta}{p}\text{ when }P\text{ is an even number}$$

$$\alpha_{n'}=n'd\alpha_m\frac{\beta}{p}+180°\text{ when }P\text{ is odd and }\beta\text{ is even}$$

$$\alpha_{n'}=n'(d\alpha_m+180°)\frac{\beta}{p}\text{ when }P\text{ is odd and }\beta\text{ is odd} \tag{7}$$

Patterns with cyclic shift of coils. In this case the N slots which belong to each phase in β poles do not lie together in the slot star but are divided into several parts. If the number of slots of a part is denoted by N', then the distribution factor of that part is

$$k_{dn'}=\frac{\sin N'(\alpha_{n'}/2)}{N'\sin(\alpha_{n'}/2)} \tag{8}$$

Assuming that the magnitude of the electromotive force or magnetomotive force of one coil side is equal to 1, the magnitude of the electromotive force or magnetomotive force of that part is

$$A_{n'}=\frac{\sin N'(\alpha_{n'}/2)}{\sin(\alpha_{n'}/2)} \tag{9}$$

If D is the distance between the axes of the parts, measured in angles $\alpha_{n'}$, then the angle between the vectors A_{n}' is

$$\theta_{n'}=D\times\alpha_{n'} \tag{10}$$

The resultant vector of the whole phase is

$$A_{n'}\text{res}=\sqrt{\Sigma(A_{n'}\cos\theta_{n'})^2+\Sigma(A_{n'}\sin\theta_{n'})^2} \tag{11}$$

and the resultant distribution factor of the phase

$$k_{dn'}=\frac{A_{n'}\text{res}}{\Sigma N'} \tag{12}$$

This procedure will be demonstrated on

Table III. Quantity $C=169\sum_{\nu\neq 13}\left(\frac{k_{dpn'}}{\nu}\right)^2$ of a 3-phase Winding with $q=1^{11}/_{13}$ for Different Layout-Patterns

W/τ	Basic Layout	Pattern with Cyclic Shift
0.903	0.0270	0.0285
0.723	0.0244	0.0285
0.542	0.0186	0.0265
0.903	0.0462	0.0492
0.723	0.0372	0.0424
0.542	0.0259	0.0340

the 3-phase winding with $q=2^3/_8$, considered previously. For this winding, $N=19$, $\beta=8$, and $d=50$ with $P=7$. The d-series yields for the basic pattern, the following slots for the three phases in a repeatable group (8 poles):

																		Phase
1	51	44	37	30	23	16	9	2	52	45	38	31	24	17	10	3	53	46	A
39	32	25	18	11	4	54	47	40	33	26	19	12	5	55	48	41	34	27	C
20	13	6	56	49	42	35	28	21	14	7	57	50	43	36	29	22	15	8	B

If a cyclic shift is made in such a manner that slot 15 of phase B is assigned to phase A, slot 53 of phase A is assigned to phase C, and slot 34 of phase C is assigned to phase B, then phase A consists of 3 parts

$N_0'=17$ (slots 1 to 3), $N_1'=1$ (slot 46)
$N_2'=1$ (slot 15)

The axis of the first part lies over slot 2. Taking this axis as reference and considering the angles in clockwise direction as being positive, the distance D_1 for slot 46 is equal to $+10$. The distance of slot 15 from the axis through slot 2 is $+47$ or -10. The smaller distance is to be chosen, because the larger distance will yield a smaller main wave, that is, coil 15 must be connected in a proper manner with the other coils of phase A. Thus, $D_2=-10$.

Table IV. Quantity $C=121\sum_{\nu\neq 11}\left(\frac{k_{d\nu n}'}{\nu}\right)^2$ of a 3-phase Winding with $q=2^5/_{11}$ for Different Layout-Patterns

W/τ	Basic Layout	Pattern with Cyclic Shift
0.950	0.0176	0.0158
0.815	0.0155	0.0155
0.679	0.0158	0.0152
0.950	0.0214	0.0502
0.815	0.0184	0.0416
0.679	0.0179	0.0332

References

1. Doubly Chorded or Doubly Shifted Fractional-Slot Lap and Wave Windings, M. M. Liwschitz. *AIEE Transactions*, volume 67, 1948, pages 684–86.

2. Differential Leakage of a Fractional-Slot Winding. M. M. Liwschitz. *AIEE Transactions* volume 65, 1946, pages 314–20.

3. Distribution Factors and Pitch Factors of the Harmonics of a Fractional-Slot Winding, M. M. Liwschitz. *AIEE Transactions*, volume 62, 1943, pages 664–66.

Discussion

P. L. Alger (General Electric Company, Schenectady, N. Y.): Dr. Liwschitz has added one more to the list of valuable papers he has contributed to the Institute. His present topic of the harmonic fields produced by irregular windings is one of peculiar fascination to the designer, but of little practical interest to the user. This is because every good designer makes sure that the harmonics of his machines are not large enough to be noticed by the customer.

However, speaking as one who has always been interested in design problems, I concur in Dr. Liwschitz' conclusions as now presented, and should like to add the following comments.

First, I know of no one who has stated a belief that the winding of maximum distribution factor is not the one of lowest reactance, as might be inferred from Dr. Liwschitz' statements. All the calculations of which I know have indicated that under the ideal assumptions made by Dr. Liwschitz the winding of minimum differential leakage reactance is the same as that for maximum distribution factor; but the conclusion has been reached because all of the examples studied have come out that way, rather than because of any satisfactory generalized proof.

Second, I question the accuracy of some of the harmonic field and reactance values given in the paper, for the reason that the method of summation of individual harmonics the author has employed is very difficult to check.

Gabriel Kron, in his very interesting book, Tensor Analysis of Networks,[1] has presented a method of calculation of these differential reactances that is entirely general, and, in my opinion, a good deal shorter than the method of summing all the individual harmonics, as presented by Dr. Liwschitz. In fact, the method of summing all the harmonic terms that he has used generally gives reactance values on the low side, because the series converges slowly, and one's patience is exhausted before he has calculated the terms out to a high enough order of harmonics. For example, the value of 3-phase differential leakage for the basic pattern of the $2^3/_8$ slot winding with 0.842 pitch calculated by Kron's exact method is 0.0267, instead of the 0.0224 value given by Dr. Liwschitz. Also, the fundamental distribution factor for this grouping is 0.95504, instead of 0.9560.

The actual reactance that will be observed in a particular machine departs from the ideal in two respects. First, the presence of slot openings, or of a large air gap, markedly reduces the higher harmonics as compared with the fundamental as shown in a recent Institute paper by H. R. West and me entitled The Air Gap Reactance of Polyphase Machines.[2] In the second place, the squirrel cage or other secondary winding of the machine has an admittance that is different for each harmonic field, and which is highly selective in the case of a few of the harmonics. For these reasons, the lowest reactance in practice may not coincide with the winding for maximum distribution factor.

The designer's wish to minimize noise and vibration leads him to prefer a winding arrangement that will minimize the fields with numbers of poles close to the fundamental. For example, in the case of the $2^3/_8$ slot per pole per phase winding considered by Dr. Liwschitz, the winding that gives minimum vibration has a coil grouping in phase A of

3 2 3 2 2 3 2 2.

This particular case is the only one of the five possible groupings of this winding that was omitted by Dr. Liwschitz from his analysis. For the least noise winding, the differential leakage is 0.0883, and the fundamental distribution factor is 0.9320. On the other hand, the worst grouping of this winding, from the point of view of vibration, is the cyclic shift 2, since Table I of the paper indicates that for this grouping the $\gamma=5$ field has a distribution factor of 0.376. In practice, this field, combined with the fundamental $\gamma=4$ field would give severe vibration.

Calculations made by my associates using the Kron method, for the case of a $2^4/_7$ slot per pole per phase winding, give differential leakage reactances of 0.0215, 0.0336, 0.0407, and 0.0675, for the four possible distributions of phase A, all for 0.907 pitch.

3 3 2 2 3 3 2,
3 3 2 2 2 3 3,
3 2 2 3 2 3 3, and
3 3 2 3 2 3 2.

The first of these has the best distribution factor and also the lowest ideal differential leakage. The last one has the lowest vibration but also the highest reactance. However, in many other cases, the lowest vibration winding has one of the lowest reactances as well.

References

1. Tensor Analysis of Networks (book), Gabriel Kron. John Wiley and Sons, New York, N. Y., chapter 12, 1939.

2. The Air Gap Reactance of Polyphase Machines, P. L. Alger, H. R. West. *AIEE Transactions*, volume 66, 1947, pages 1331–43.

J. S. Gault (University of Michigan, Ann Arbor, Mich.): In paragraph 3, column 3, of page 1129 of the paper, the three shift patterns are apparently chosen quite at random. The only one explained in detail is shift 2, which is obtained by substituting coil 15 for coil 53 in phase A, and so forth. Since this presents the simplest shift pattern, 1-17-1, and also since the value of K_{d4} is least affected, it would seem as though this should be called shift 1. In Table I of the paper, the value of K_{d2} for shift 2 is given as 0.0301. I computed 0.0388, which also would be the value for K_{d55} and K_{d59}.

Shift 1 is apparently obtained by substituting coil 25 for coil 44 in phase A, and so forth. This gives a pattern, 2-16- -1. I tried to check his value for K_{d4}, but computed 0.0839 instead of the value 0.0868 given.

Shift 3 might be obtained by substituting coils 25, 4, and 39 for coils 44, 23, and 1 respectively or by substituting 29, 36, 50, 8 and 15 for 10, 17, 31, 46 and 53 respectively. Either gives a shift pattern, 1-2-14-1- -1. For this I computed $K_{d4}=0.944$, instead of the value 0.925 given in the paper, but checked his figure of 0.041 for K_{d2}.

The author is to be commended for his brevity and the tabular display of data to prove his point. The sampling of six values in Table I, however, indicates that 50 per cent of the values given may only be correct to the first significant figure.

M. M. Liwschitz: I wish to thank Mr. Alger, who has done fundamental work on the differential leakage of electric machines, for his comments on my paper, and I was glad to learn that he concurs in my conclusions with respect to the differential leakage of the fractional-slot windings. However, I do not agree with some of his statements.

Mr. Alger mentions that all the calculations of which he knows "have indicated that under the ideal assumptions made by Dr. Liwschitz the winding of minimum differential leakage reactance is the same as that for maximum distribution factor." I have not seen any published calculations of differential leakage of fractional-slot windings except my own paper, Differential Leakage of a Fractional-Slot Winding,[1] in which the influence of the slot-openings as well as of the damping by a squirrel-cage rotor is considered. The paper by Mr. Alger and Mr. H. R. West, The Air Gap Reactance of Polyphase Windings,[2] does not treat the fractional-slot windings.

Mr. Alger questions the accuracy of some of my results "for the reason that the method of summation of individual harmonics the author has employed is very difficult to check... the series converges slowly and one's patience is exhausted before he has calculated the terms out to a high enough order of harmonics." Mr. Alger has overlooked equations 22 and 23 of my paper mentioned above. They give a simple formula for the sum of all slot harmonics so that only the harmonics of low order have to be considered individually. The consideration of the individual harmonics is necessary if the damping by a squirrel-cage rotor is to be considered. Mr. Alger did not try to do this in his paper mentioned above, otherwise he would be forced to consider the harmonics individually and not in lumps.

Mr. Alger apparently had in mind only the 120-cycle noise due to stator harmonics. It is different when one considers the noise produced by stator and rotor harmonics together, which is usually much more disturbing than the 120-cycle noise. There it may become necessary to reduce one specific stator harmonic, which yields an inadequate number of force pole-pairs, and a coil grouping which reduces to a certain degree several stator harmonics may not be satisfactory.

The coil grouping depends upon the individual case and there exists no coil grouping which is the best for all cases.

Mr. Gault has overlooked the practical aspect of the paper. The shift patterns chosen are not at random. They are all of the same importance, one pattern being the right one for one machine, the other pattern being the right one for another machine. The practical engineer reduces the distribution factor of the main wave, if it is necessary to avoid noise, and thus it depends upon the individual case to decide which pattern should be called the best and given the first place.

Mr. Gault overlooks further the fact that it is not the first or second or third significant figure which counts here but the decimal place of the significant figure. A harmonic, the first significant figure of which starts in the third decimal place, is of no importance no matter how large the significant figure is. The values of all tables of the paper have been calculated using the slide-rule because no greater accuracy is necessary for the practical engineer. It is impossible that two engineers using the slide-rule will get exactly the same results in complicated calculations of the kind made for the tables. I am not sure whether Mr. Gault or the graduate student mentioned in my paper has made errors in the third decimal place and in one case even in the second decimal place. It is not worthwhile to check this because it has no practical value. Table I contains about 160 figures. Mr. Gault made calculations for five of them. Overlooking the important fact that not the significant figures but the decimal places are of importance, he states that "50 per cent of the values given may only be correct to the first significant figure." A reader who is not familiar with the problem of harmonics may get the wrong impression that the paper is incorrect. Mr. Gault neither acknowledges the fact that his calculations are based on formulas derived for the first time in my paper nor does he comment on the substance of the paper.

REFERENCES

1. See reference 2 of the paper.

2. THE AIR GAP REACTANCE OF POLYPHASE MACHINES, P. L. Alger, H. R. West. *AIEE Transactions*, volume 66, 1947, pages 1331–43.

Motor Tests Evaluate Thermal Endurance of Class-H Insulation and Silicone Varnish

GEORGE GRANT, III
MEMBER AIEE

T. A. KAUPPI
ASSOCIATE AIEE

GRAHAM LEE MOSES
MEMBER AIEE

G. P. GIBSON
MEMBER AIEE

THE thermal endurance of silicone resins was first evaluated by laboratory tests. Subsequently, accelerated tests were made on insulation specimens and then a few isolated machines were tested.[1,2] It was recognized, however, that a comprehensive test program involving a number of machines operated at various temperatures would have to be completed before the life expectancy of this new class of insulation could be evaluated properly. In October 1943, Dow Corning Corporation set up a motor testing laboratory at Midland, Mich., where such a program was carried out in co-operation with the Westinghouse Electric Corporation. Progress reports have been made on part of the results.[3,4] This program is now complete enough to be summarized and reported as a completed project. It is believed that the results will prove of interest and value in the further application of class-H insulation.

Objectives

The motor test program was planned originally to determine the thermal endurance of silicone-insulated motors at several different temperatures. The objective was to obtain a scattering of thermal life end points over a sufficiently wide temperature range to permit extrapolation of life expectancy to lower temperatures. It was further desired to obtain a comparison between the behavior of class-B insulation and class-H insulation under the same test conditions. A further purpose was to attempt some correlation between laboratory tests on insulation components and the behavior of the components in motors.

The fundamental concept followed in this test program is that appreciable decrease in the insulation resistance of the windings after exposure to excessive humidity is a criterion of minimum insulation life. Ultimate or maximum life is taken to be the point at which the insulation deteriorates mechanically to such an extent that the motor fails to operate.

Apparatus Tested

The results reported in this paper include the original six induction motors with class-H insulation treated with *DC 993* silicone varnish, and two class-B induction motors of similar design. One traction motor with class-H insulation treated with *DC 993* silicone varnish also is included. All of the induction motors were wound on standard *284* frames and rated at 10 horsepower. A complete description of the motors tested is given in Appendix I.

Test Procedure

The test cycle started with a period of operation at the test temperature followed by cooling to room temperature and exposure of the windings to 100 per cent relative humidity for a 24-hour period. The period of running was determined by dividing the estimated life by 25 so that each motor would experience approximately the same number of cycles before failure. The precise test procedure is given in Appendix II.

Results

Typical data on three units have been plotted in Figures 1 through 8. Figures 1 through 6 show how aging alters the insulation resistance and dissipation factor as determined just before humidification, and after humidification. Per cent change in the winding resistance (at 25 degrees centigrade) as a function of aging also is shown on these graphs. Capacitance was not plotted since it showed no significant change as a result of aging.

In general the same pattern is obtained on each unit. The humidified insulation resistance and dissipation factor show little change for a period. Then suddenly insulation resistance decreases and dissipation factor increases indicating a major change in the condition of the insulation. This is the point at which moisture begins to penetrate the winding. It is considered to be a minimum life point. Coincident with the loss of moisture resistance, fine crazing of the resin became very noticeable.

Insulation resistance and dissipation factor are not affected by ambient conditions until the minimum life point has been reached. Thereafter, they become dependent on ambient conditions. This interdependence is indicated by data given in Figures 7 and 8. The insulation resistance cooling curves in Figure 7 show an increase in moisture absorption after aging at high temperature. Line A, taken before minimum life was reached, shows no moisture pickup on cooling. However, both B and C which were taken

---

Paper 49-237, recommended by the AIEE Rotating Machinery Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted March 10, 1949; made available for printing September 1, 1949.

George Grant, III, is an electrical engineer and T. A. Kauppi is Manager, Product Engineering, both with the Dow Corning Corporation, Midland, Mich. Graham Lee Moses is Manager, Development Insulation Section, Westinghouse Electric Corporation, East Pittsburgh, Pa., and G. P. Gibson is design engineer with Westinghouse Electric Corporation at Buffalo, N. Y.

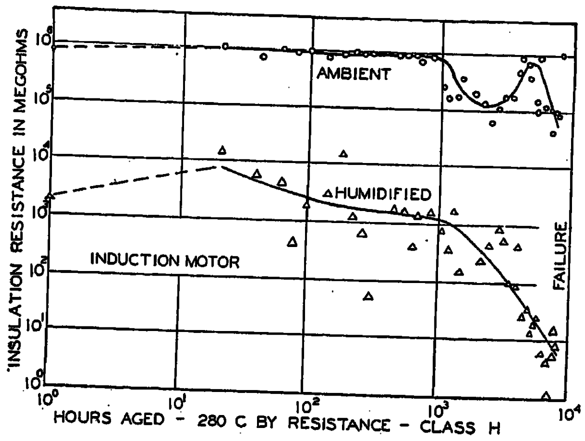


Figure 1 (left). Effect of aging at 280 degrees centigrade and periodic humidification on insulation resistance of class-H induction motor winding

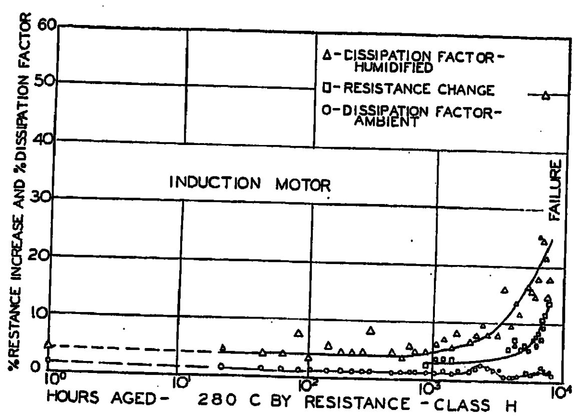


Figure 2 (above). Changes in dissipation factor of insulation and cold resistance of the winding as a function of hours aging at 280 degrees centigrade on class-H induction motor

after minimum life, show definite moisture pickup. Line B with less aging than C, shows much more pickup due to the greater humidity. The curves in Figure 8 show that a new unit is relatively unaffected by ambient humidity while a unit that has passed the minimum life point could almost be used as an indicator of humidity.

Insulation resistance readings on a motor under ambient conditions, therefore, gives no positive indication of the condition of the insulation unless ambient conditions are known. Under low humidity conditions, insulation resistance readings will be high regardless of the extent to which the windings have been aged. Under high humidity conditions, however, high-insulation resistance indicates that the minimum life point has not been reached.

Figure 2 also shows the change in cold winding resistance as a result of aging. Very little change is noted until the minimum life point has been reached. The cold copper resistance then tends to increase at an accelerating rate due to oxidation of the conductors.

This trend occurred in each of the four silicone units which have failed so far. Failure occurred during the heat run and was caused by the fusing of the conductors. This fusion was the result of localized reduction in the cross-sectional area of the wires due to oxidation of the copper. Oxidation started to take place when crazing of the insulation at minimum life permitted air to penetrate the voids to the hot copper. Thus, localized oxidation took place resulting in a local increase in resistance of sufficient magnitude to be reflected by an increase in cold winding resistance. With the load current flowing, the heating and consequently the temperature in these high-resistance spots increased. This increase in temperature accelerated the rate of oxidation and the rate of increase in resistance. This cumulative condition progressed until fusing of the conductor took place.

At the time of failure no fire was encountered on these four units. The windings were firm though the resin was brittle. Failure of the class-B units took place during the 5-minute no-load run after humidification. Failures were due to short circuits within the winding caused by moisture penetration. Both units caught fire. The windings were very loose at the failure point and there was little visible resin left on the winding.

Had much vibration been present, the class-B units probably would have failed much sooner because of the lack of resin bonding of the windings. However, since the silicone windings were still firmly bonded, they probably would not have failed much sooner under vibration conditions.

Discussion

As background for this discussion, the points at which minimum life and failure took place are listed in Table I. Except for two of the units for which the test procedure was altered, the number of cycles required to reach minimum life and to reach failure are very nearly the same from unit to unit. This indicates good reproducibility of results.

It is agreed generally that the thermal life of insulation is reduced by one-half for each increase of 8 to 12 degrees centigrade in temperature. Thus, if life is plotted logarithmically and temperature linearly, a straight line results. The minimum life points of the silicone units are so plotted in Figure 9. The two lowest points through which a straight line can be drawn are 950 hours at 280 degrees centigrade and 9,750 hours at 240 degrees centigrade. These points were taken to determine the slope of the life line. The line through these points is taken to be the minimum time to be expected to reach minimum life. It shows a rule of half life for increments of 11.6 degrees centigrade which, for practical purposes, may be considered to be 12 degrees centigrade. Similarly, a line parallel to this was drawn through the point (1,000 hours at 300 degrees centigrade) that produced the greatest spread. This line represents the maximum time to be expected to reach minimum life. These two lines represent the probable spread in minimum life.

After minimum life has been reached, failure would be determined largely by external factors and might be expected anywhere up to the points found in this testing. It is believed that there is little

Table I. Summary of Motor Test Results

Motor Number	Insulation Class	Temperature Degree Centigrade by Resistance	Minimum Life Hours	Minimum Life Cycles	Time to Breakdown Hours	Time to Breakdown Cycles	Type Motor	Figure Number
1	H	310	500	25	5,131	47	Induction	
2	H	300	1,000	8	5,179	45	Induction	
3	H	300	500	22	6,888	50	Induction	
4	H	285	800	26	1,610*	42*	Traction	5, 6
5	H	280	950	17	8,548	41	Induction	1, 2
6	H	240	10,750	28	13,950*	35*	Induction	
7	H	240	9,750	26	13,450*	34*	Induction	
8	B	275	20	9	1,056	33	Induction	3, 4
9	B	200	200	16	3,760	40	Induction	

* These motors were still operating in January 1949.

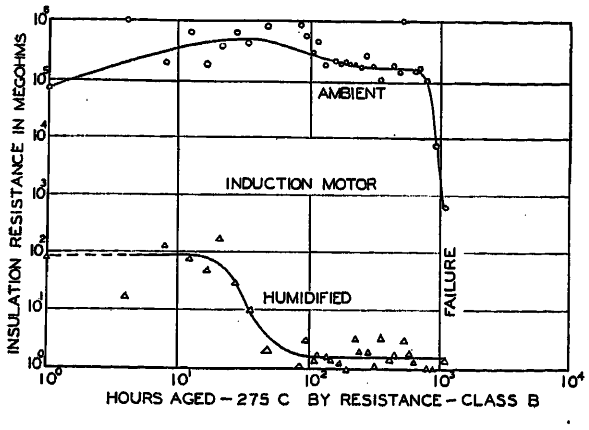


Figure 3 (left). Effect of aging at 275 degrees centigrade and periodic humidification on insulation resistance of class-B induction motor winding

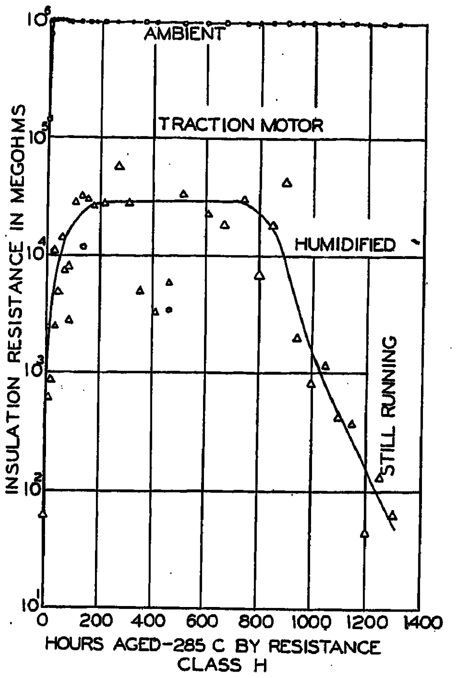


Figure 5. Dry and humidified insulation resistance data for class-H-insulated traction motor as a function of hours aged at 285 degrees centigrade

chance that units built like these will run much beyond the failure points in this testing. The actual failure points and the originally estimated life line for *DC 990A* also are plotted for reference.

Tinned or cadmium-plated wire probably would reduce or eliminate the oxidation of the copper conductors and increase the time to failure. However, chemical action, severe vibration, moisture, or other factors encountered by many motors in service, probably would cause failure to take place within the limits of minimum life and the plotted failure points.

At the beginning of the testing of the second and eighth units listed in Table 1, longer heat runs than those called for by dividing the estimated life by 25, were used. Hence, more aging was obtained during each cycle and fewer cycles were required to reach minimum life. Further, the relative minimum life of the second unit was double the average of the other silicone motors and determined the line for maximum time to minimum life as plotted in Figure 9. It therefore appears that the number of times a motor is cooled is a factor in these tests. More frequent cooling produces shorter minimum life because the thermal expansion differences which tend to damage the insulation are encountered more often.

Another minimum life line based on the method suggested by Dakin[5] was drawn in Figure 9. According to this theory the insulation life as a function of temperature can be considered as a first order chemical reaction. The logarithm of the life then is a function of the inverse absolute temperature. In the zone of temperatures used in this testing, little difference between the two life lines is noticeable. Outside this zone some divergence is noted and life as predicted by the inverse absolute temperature is always greater than that obtained by extrapolation based on the 12-degree centigrade rule. For general purposes the 11.6- or 12-degree centigrade rule should prove satisfactory and Dakin's method simply offers greater assurance in selecting safe operating temperatures.

The hottest-spot temperatures as measured by thermocouples were found to be not more than 10 degrees centigrade above the temperatures measured by resistance. Hence, 10 degrees centigrade were added to the temperature shown in Figure 9 and the resulting minimum life lines were plotted in Figure 10. Also plotted in Figure 10 are the minimum life points found on the class-B units and minimum life data on classes A and B as presented by other investigators.[6,7] It will be noted that all of the class-A and -B data are closely grouped and in a much lower temperature zone. A life of seven years (61,300 hours) has been used to represent a typical life for these classes of insulation at their respective permissible temperatures.[6] According to the 12-degree centigrade line for class H, a life of approximately ten times this could be expected at the 180-degree-centigrade hottest-spot temperature now set for class H. For a 7-year life, a temperature of 220 degrees centigrade should be permissible. Comparing the class-H line with the top class-B line at 180 degrees centigrade, it will be seen that class H should have 430 times the life of class B.

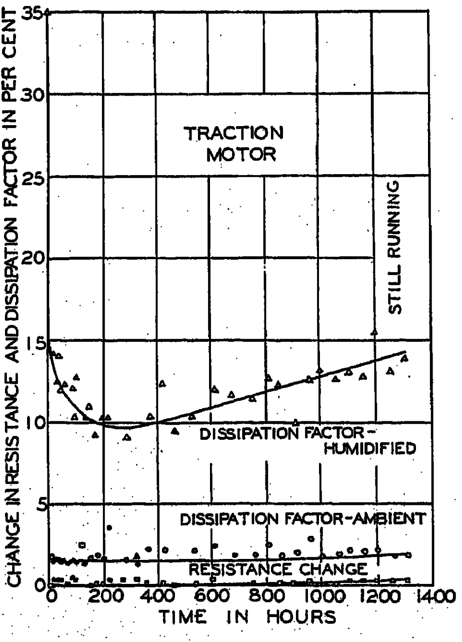


Figure 6. Effect of thermal aging at 285 degrees centigrade on dissipation factor and cold resistance of the winding of class-H-insulated traction motor

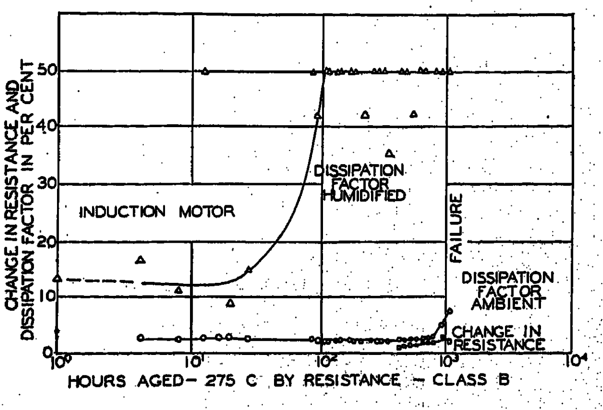


Figure 4 (left). Changes in dissipation factor of insulation and cold resistance of the winding as a function of hours aging at 275 degrees centigrade on class-B induction motor

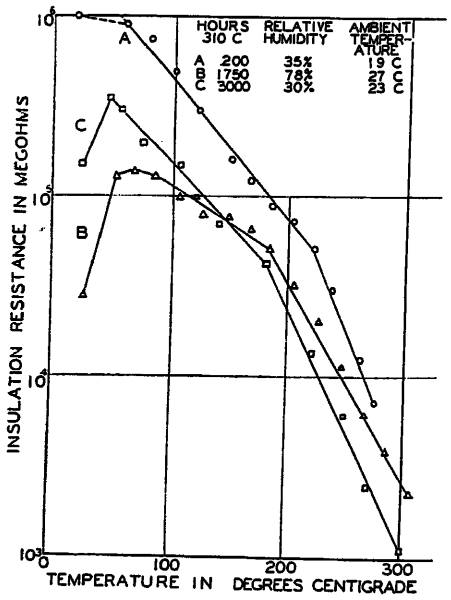


Figure 7. Effect of temperature and humidity on insulation resistance of class-H-insulated induction motor at various degrees of aging at 310 degrees centigrade

Conclusions

The conclusions reached concern thermal aging and moisture resistance of insulations. Therefore, other design considerations are disregarded in these conclusions.

A number of motors were tested at different temperatures, but there were not a sufficient number at any one test condition to permit statistical analysis of the results. However, the points reported can be considered to fall within the 95 per cent failure zone. Based on the knowledge that the test points are reasonable approximations, the following conclusions were reached:

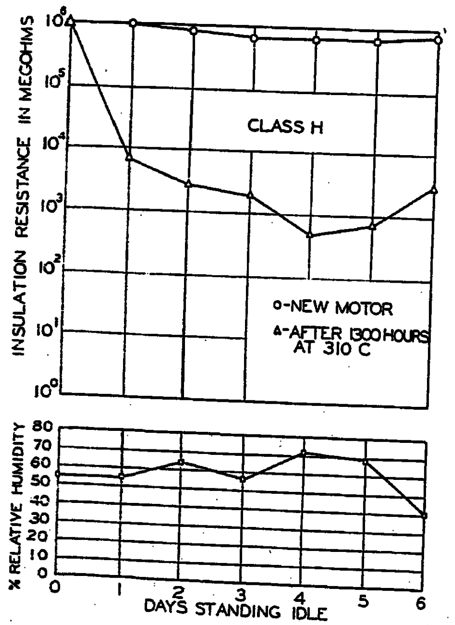


Figure 8. Change in insulation resistance of a class-H-insulated induction motor while standing idle-new and after aging 1,300 hours at 310 degrees centigrade

1. This method of testing is a satisfactory way to determine the minimum thermal life expectancy of insulation in an electric motor. It also determines the type of failure to be expected from thermal aging of different kinds of insulation.

2. The minimum life points as determined by this method and serious crazing of the resin occur after the same amount of aging.

3. Failure of the windings of silicone insulated motors has been found to occur before the bonding of the resin is seriously weakened and was due to copper conductor failure.

4. Silicone insulation presents less fire hazard than do other classes of insulation as shown by these tests and other independent flammability tests.

5. When motors of this size operate at temperatures near 280 degrees centigrade, the number of times they are cooled will affect the minimum life. More frequent thermal cycling may reduce the minimum life.

6. The minimum thermal life of class-H-insulated equipment using *DC 993* varnish is approximately twice that of equipment made with *DC 990A* varnish.[4]

7. The results of these tests indicate about a 100-degree-centigrade temperature advantage for class-H insulation compared with class-B insulation. If current concepts of thermal aging are correct, the class-H insulation could be expected to operate as much as several hundred times as long as class-B equipment, where thermal aging is the primary factor in establishing insulation life.

8. At least seven years of continuous operation at a hottest-spot temperature of 220 degrees centigrade will be required to reach the minimum thermal life of class-H-insulated equipment.

9. The thermal life of class-H silicone insulated motors closely follows a 12-degree-centigrade rule. It is possible that the life of class-H-insulated equipment, employing *DC 993* silicone resin, may follow the more optimistic rule

$$\log_e (\text{hours life}) = \frac{16{,}520}{\text{Absolute Temperature}} - 23$$

10. The maximum life of class-H insulation was not determined by these tests, since the ultimate end points were the results of copper conductor failure rather than mechanical disintegration of the resin.

11. In silicone varnished class-H motors, insulation resistance as a function of relative humidity can be used effectively in maintenance work. If the insulation resistance is not seriously affected by humidity, the motor may be presumed to be entirely satisfactory for continued use. If dependent on humidity, the minimum life point has been reached.

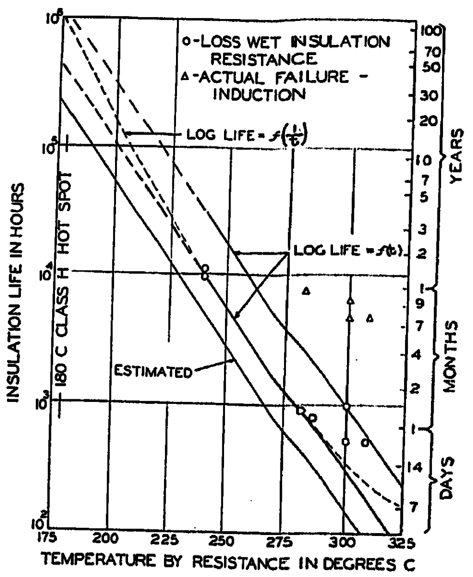


Figure 9. Thermal aging curve indicating the minimum life zone of class-H-insulated windings

Appendix I. Description of the Motors Tested

Induction Motors

The induction motors selected for test were rated at 10 horsepower, 60 cycles, 1,140 rpm, 440 volts. The frame (number *284*) was a totally-enclosed type and the end brackets were the wound-rotor type of open construction. Each bracket carried a 1-inch thick asbestos heat shield instead of the normal air shields. Matching heat shields were mounted on the shaft and rotated with it. With this and special bearing cooling construction a temperature of 450 degrees centigrade in the winding was required to

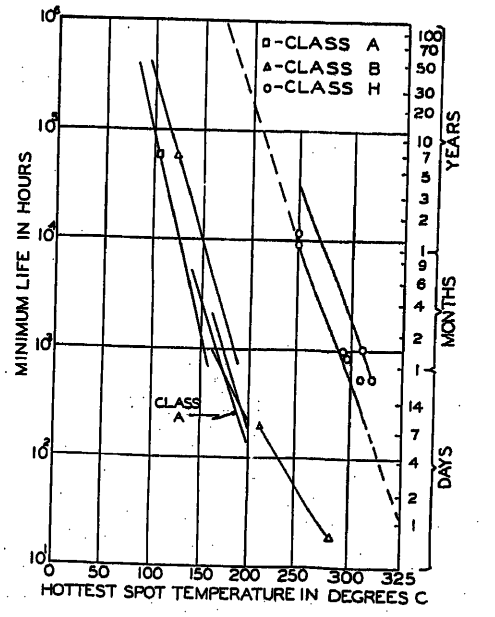


Figure 10. Composite thermal endurance curve (minimum life) for various classes of insulation as reported by several investigators

raise the bearing temperature to 100 degrees centigrade. Thus, high-temperature insulation tests were possible without complications arising from the bearings. Bearings are, of course, an essential part of a motor, and they must be considered in any prediction of over-all performance. However, insulation tests and bearing tests must be performed as independently as possible.

Thus, the motors were in effect thermally enclosed but not enclosed by the generally accepted National Electrical Manufacturers Association or AIEE definition. The space between the asbestos heat shields provided an opening for the moisture to leave the interior when the motor was dried.

The silicone class-H stators were wound with mush coils using double glass covered wire treated with silicone resin *DC 993*. A combination of glass cloth and mica 0.026-inch thick treated and bonded with *DC 993* resin was used for the slot cells and center wedges. Similar material 0.010-inch thick was used for phase insulation. Since suitable silicone materials were not available at the time these motors were built, an asbestos phenolic laminate served for the top wedges. Untreated glass tape was applied from iron to iron on the end turns. All connections were taped with a silicone bonded flexible glass and mica tape over which untreated glass tape was applied. After winding, the stators were prebaked then vacuum-and pressure-impregnated with *DC 993* silicone resin, followed by multiple dips in the resin. Each dip was followed by a bake at 250 degrees centigrade.

The class-B motors were identical to the silicone units in all details except that synthetic organic class-B varnishes were used in place of the silicone resin and appropriate baking temperatures were used.

Traction Motor

The railway motor was the same as those used on standardized PCC (President's Conference Car) streetcars (Westinghouse type *1432*). The general motor design was identical to the standard production models except that class-H silicone insulation was applied as follows:

Armature coil conductors were glass-covered wire treated with silicone varnish *DC 993*. Ground insulation consisted of $2^{1}/_{2}$ turns of glass-backed mica wrapper 0.008-inch thick. The glass cloth was silicone-varnish sized and a silicone bond was used. Binder tape was fiber-glass, sized with silicone varnish *DC 993*. The coils were impregnated with silicone varnish *DC 993*.

Main and commutating field coils were made of strap conductors, separated by a single thickness of glass-backed mica tape 0.007-inch thick (silicone bonded and sized). Ground insulation consisted of two half-lapped layers of glass-backed mica tape 0.007-inch thick (silicone bonded and sized). Binder tape was fiber-glass sized with *DC 993*. These coils were vacuum and pressure impregnated with *DC 993* at 70 per cent solids.

Insulation details for winding the armature were made of glass-backed mica and asbestos cloth, silicone sized and bonded. For slot wedges asbestos-melamine Micarta was employed. Coils were soldered in the commutator necks with high melting solder (304 degrees centigrade solidus temperature) for extra protection.

Both armature and stator were dipped twice in *DC 993* silicone varnish at 50 per cent solids.

The commutator was insulated with mica bonded with a high-temperature synthetic shellac substitute. The bars were made of silver-bearing hard-drawn copper to avoid softening during the high-temperature bake or during operation. During all baking cycles the commutator nut was loosened to relieve the vee binding from pressure strains.

Appendix II. Details of Test Procedure

Preliminary

1. A low-temperature standardizing run of 48 hours at 130 to 140 degrees centigrade was made to arrive at a dry condition. While cooling from this run, the insulation resistance, capacitance, and dissipation factor were taken at intervals of 20 degrees centigrade.

2. Upon reaching room temperature, the motor with the end bracket removed, was placed under humidification for a period of one week. Daily readings of insulation resistance, capacitance, and dissipation factor were made and a high-potential test of rated voltage 60 cycle was applied for five minutes between windings and frame.

3. Within five minutes after humidification the three quantities were again read and the high potential test again was applied. Within 15 minutes after humidification, the end bracket was replaced and the motor was run at full rated voltage, no load, for five minutes. Following this, insulation resistance, capacitance, and dissipation factor were measured and the standard high-potential test was repeated once more.

4. The motor was then put on a drying run in which the temperature rose to 100 degrees centigrade in the first $1^{1}/_{2}$ hours and to 130 to 140 degrees centigrade in six hours. This temperature was maintained for eight to twelve hours.

5. If the test temperature exceeded the baking temperature of 250 degrees centigrade, a break-in run was made. This consisted of taking the motor up to 250 degrees centigrade by overloading and then setting load so that the temperature increased at the rate of 10 degrees centigrade per hour till the test temperature was reached. The break-in time was evaluated on the basis of the 12-degree rule. This accumulated time was counted in on the time of the first heat run.

Heat Run

On the heat run, the test temperature was attained as quickly as possible by overloading. In the interests of test standardization, the duration of heat runs was determined from the estimated life. The estimated life was based on the premise that the life of silicone insulation *DC 990A* was 5,000 hours when operated at a temperature of 240 degrees centigrade. The estimated life at any other temperature was determined by the 12-degree rule using 5,000 hours at 240 degrees centigrade as the base. This calculated time was divided by 25 to determine the length of time these motors were to be operated at test temperatures during each cycle. For practical reasons, operating times at test temperature were not less then four hours. The first five cycles were run according to this plan, but in each succeeding group of five cycles time at test temperature was double that of the preceding group of five cycles.

Test Cycle

A complete test cycle began with a heat run as described. The machine was then shut down and during the cooling period, readings of insulation resistance, capacitance, and dissipation factor were taken at intervals of 20 degrees centigrade. Humidification and high-potential tests were then made.

These tests were followed by a no-load run, and a drying run as described in paragraphs 2, 3, and 4 in the preceding section entitled "Preliminary," except that the period of humidification was reduced to 24 hours. Such cycles of heat and humidity were repeated until the motor failed.

Traction Motor-Test Procedure

The traction motor, being a d-c machine, required some slight variations in the general procedure outlined before. It was planned originally that the insulation resistance, capacitance, and dissipation factor of the main field, commutating field, and armature should all be studied. However, it was soon found, because of the noninsulated lead ends, brush rigging, commutator, and other unavoidable bare copper, that the severe humidification caused insulation resistance, capacitance, and dissipation factor values of the commutating field and armature to reach magnitudes that could not be measured with the equipment at hand.

The complexity of the circuits precluded satisfactory guarding. Hence, the data here presented are those obtained from the main field having leads that extend outside the motor and are not affected by leakage from bare copper inside the motor.

Both the main field and commutating field were run at 285 degrees centigrade by resistance. This was done by shunting some of the line current around the main field. While the fields were at this temperature, the armature operated at 180 degrees centigrade and the commutator at 150 degrees centigrade.

The only other variations in procedure were in the 5-minute run and high-potential test. This motor, being series wound, would overspeed if unloaded with the full 300-volt direct current applied. Hence it was run at the maximum permissible voltage which was 125. Although this was considerably below the 300-volt rating, it was somewhat compensated for by the use of 500 volts of alternating current for the high-potential tests. Before the high-potential test and the no-load run the excess water was blown off the commutator, vee rings, and brushholders.

Methods of Measurement

1. Temperature by resistance was calculated from the value of resistance ob-

tained by projecting to zero time the resistance curve of the windings while cooling.

2. Insulation resistance values are 1-minute readings obtained with a 500-volt d-c megohm bridge. They are corrected to a temperature of 25 degrees centigrade from temperature-insulation resistance data.

3. Capacitance values were measured between windings and frame by means of a 60-cycle low-voltage bridge. They are corrected to 25 degrees centigrade.

4. Dissipation factor of the insulation was evaluated on the capacitance bridge and was obtained concurrently with the capacitance.

References

1. ORGANO-SILICON COMPOUNDS FOR INSULATING ELECTRIC MACHINES, T. A. Kauppi, G. L. Moses. *Electrical Engineering* (*AIEE Transactions*) volume 64, March 1945, pages 90–93.

2. THE APPLICATION OF SILICONE RESINS TO INSULATION FOR ELECTRIC MACHINERY, J. DeKlep, L. R. Hill, G. L. Moses. *Electrical Engineering* (*AIEE Transactions*) volume 64, March 1945, pages 94–98.

3. SILICONE INSULATION PROVED BY TEST, T. A. Kauppi, George Grant, G. L. Moses, R. P. Horrell. *Westinghouse Engineer* (East Pittsburgh, Pa.) September 1945, pages 135–40.

4. INVESTIGATION OF SILICONE INSULATION ON HIGH-TEMPERATURE RAILWAY MOTOR, George Grant, III, T. A. Kauppi, Graham Lee Moses. *AIEE Transactions*, volume 66, 1947, pages 305–11.

5. ELECTRICAL INSULATION DETERIORATION TREATED AS A CHEMICAL RATE PHENOMENON, Thomas W. Dakin. *AIEE Transactions*, volume 67, 1948, pages 113–22.

6. SYNTHETIC INSULATION AND THE 10-DEGREE RULE, G. L. Moses. *Westinghouse Engineer* (East Pittsburgh, Pa.) July 1945.

7. TEMPERATURE-AGING TESTS ON CLASS-A-INSULATED FRACTIONAL-HORSEPOWER MOTOR STATORS, J. A. Scott, B. H. Thompson. *Electrical Engineering* (*AIEE Transactions*) volume 61, July 1942, pages 499–501.

8. PREDICTING INSULATION PERFORMANCE BY STATISTICAL ANALYSIS, Graham Lee Moses. *Electrical Manufacturing* (New York, N. Y.) volume 43, April 1949, pages 108–12.

No Discussion

General Theory of Multiple Cage Induction Motors

SHELDON S. L. CHANG
ASSOCIATE AIEE

IN designing the rotor lamination of a multiple cage motor, a designer usually feels the double difficulties of too many variables and too many demands. The slots can be of any shape, any multiplicity, and the same lamination may be used for motors of different speed and horsepower to give, under all circumstances, the best possible performances which nobody knows. In this paper, an attempt is made to simplify this problem:

1. By deriving a quantitative relationship between torque at all values of slip and current at all values of slip with no restriction whatsoever on the rotor construction.
2. By defining a basic leakage factor $k_{r\infty}$ which is a quantitative measure of the "circumstance" which limits the performance of the motor in general.
3. By determining the combinations of full load slip, power factor, and starting kilovolt-amperes per horsepower which can be obtained without reduced accelerating torque for motors with a given value of $k_{r\infty}$.
4. By showing a design method which gives the slot areas and over-all slot constants, and the apparent rotor impedance at various slip frequencies for any selected possible combination of slip, power factor, and starting current.

The analysis is based upon three assumptions:

1. The saturation effect on the leakage reactance is negligible.
2. The stator resistance is negligible.
3. The iron loss does not have any appreciable effect on torque and current.

Of these three assumptions, the third is always true. The first assumption is common in most machinery analysis. The second assumption is justified by the following considerations:

1. In larger induction motors of 30 horsepower and above, the ratio of stator resistance to leakage reactance is usually so low that the stator resistance is smaller in magnitude compared to the change in leakage reactance due to saturation effect.
2. By neglecting these two effects, the possible performance derived in this paper fixes the upper limit for induction motors.
3. The combined effect of these two factors introduces an approximately constant voltage drop. This voltage drop can be corrected in actual motor design without much difficulty.

Derivation of General Equations Between Torque and Current

The conventional equivalent circuit of an induction motor is in stator terms. The frequency and voltage are always the line frequency and voltage. Both the primary and secondary reactances are assumed constant and the conductivity of rotor material is multiplied by the slip.

The equivalent circuit in rotor terms is just as true and easy to prove. In such an equivalent circuit, the line frequency and voltage are multiplied by the slip.

The inductances instead of the reactances are now constant. The conductivity of the rotor material is constant while that of the stator material is divided by s.

Under the assumption of negligible stator resistance, this equivalent circuit is exactly the same circuit of the motor under locked rotor condition while a voltage of variable frequency is applied to its terminals, since throughout the circuit, the inductances and conductivity are constant. With stator resistance neglected, an equivalence is established between a running motor at constant frequency and voltage and a locked motor tested under a test frequency and voltage in proportion to slip.

Suppose $Y = G-jB$ is the admittance per phase of the locked motor measured at angular frequency ω, where $\omega = \omega_1 \times s$, ω_1 is the line frequency. The phase current I of the motor running at a slip s is

$$I=sYV \tag{1}$$

The stator input watts per phase is

$$W=sV^2G \tag{2}$$

Under the assumed conditions, the total stator input is equal to the total secondary input which is the torque in synchronous watts.

Therefore

$$T=snV^2G \tag{3}$$

The total reactance volt amperes is

$$U=snV^2B \tag{4}$$

The performance of the motor, such as power factor, speed at different load, starting torque, starting current, and accelerating characteristic are completely described by T and U as functions of the slip. From equations 3 and 4, we see that T and U are not completely independent but they are the imaginary and real components of a single function F, namely

$$F=U+jT \tag{5}$$

From equations 3, 4, and 5

$$F=\frac{nV^2}{\omega_1}\times j\omega Y \tag{6}$$

F is called the performance function, because once F is known, the complete performance of the motor is known.

We have shown that the torque in synchronous watts and the total reactive volt amperes are respectively the imaginary and real components of a complex function F. This function F is a constant times $j\omega$ times an admittance function Y of a stationary electric circuit, since a

---

Paper 49-238, recommended by the AIEE Rotating Machinery Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted March 28, 1949; made available for printing September 2, 1949.

SHELDON S. L. CHANG is with Robbins and Myers, Inc., Springfield, Ohio.

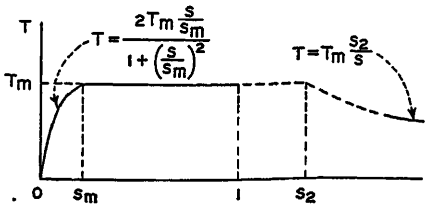


Figure 1. The speed torque curve of a constant torque motor

motor with rotor locked is not different in anyway from an ordinary electric circuit. Network theorems show that the real and imaginary components of F do not vary with frequency independently, and that they are tied together by the following equations[1]

$$\int_{-\infty}^{\infty} T du = \frac{\pi}{2}(U_\infty - U_0) \tag{7}$$

$$U_c - U_0 = -\frac{2\omega_c^2}{\pi}\int_0^\infty \frac{\frac{T}{\omega}-\frac{T_c}{\omega_c}}{\omega^2-\omega_c^2} d\omega \tag{8}$$

$$T_c = \frac{2\omega_c}{\pi}\int_0^\infty \frac{U-U_c}{\omega^2-\omega_c^2} d\omega \tag{9}$$

In equations 7 through 9, $U=\ln s$, U_c and T_c are values of U and T at a slip frequency ω_c, U_0, and U_∞ are the total reactive voltamperes at $s=0$ and $s=\infty$ respectively.

Equation 7 states that the total torque area is equal to $\pi/2$ times the difference between the reactive volt amperes at infinite slip frequency and zero slip frequency. Once these two values are fixed, any variation in the shape and conductivity of rotor cages shifts the location of torque on the speed axis but does not change the total torque area. At $s=0$

$$U_0 = n\frac{V^2}{X_0} \tag{10}$$

where X_0 is the total reactance which is the sum of the primary leakage reactance, and magnetizing reactance. At infinite slip frequency, the rotor bars are equivalent to short circuits, and

$$U_\infty = n\frac{V^2}{X_\infty} \tag{11}$$

where X_∞ is the total leakage reactance at infinite slip frequency. Since at high slip frequency all the induced current is concentrated at the rotor bar surface, the total leakage reactance X_∞ does not include any rotor slot leakage permeance except that of the slot opening only. If the skin effect of the outer cage is neglected in calculating the performance, the value of X_∞ should include the leakage permeance of the outer cage correspondingly. In any case, X_∞ is quite independent of the shape of the rotor slot, or the inner cage.

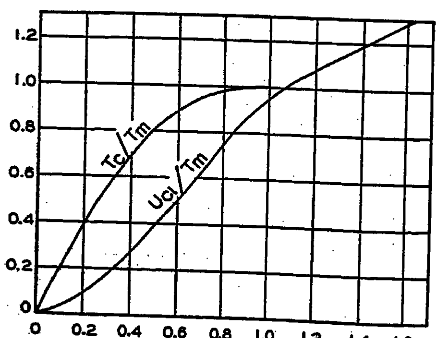


Figure 2. Reactive current of a constant torque motor

Substituting equations 10 and 11 in equation 7, it becomes

$$\int_{-\infty}^{\infty} T du = \frac{\pi}{2}\left(\frac{nV^2}{X_\infty}-\frac{nV^2}{X_0}\right) = \frac{\pi}{2}\frac{nV^2}{X_\infty}k_{r\infty} \tag{12}$$

An equation for U_0 can be derived from equation 12 as follows

$$U_0 = \frac{nV^2}{X_0} = \frac{nV^2}{X_\infty}\frac{X_\infty}{X_0} = \frac{nV^2}{X_\infty}(1-k_{r\infty}) \tag{13}$$

Combining equations 12 and 13, a relation between the no load current and torque area is obtained; it is

$$U_0 = \frac{2}{\pi}\frac{1-k_{r\infty}}{k_{r\infty}}\int_{-\infty}^{\infty} T du \tag{14}$$

These equations give the necessary conditions which the torque and current of induction motors must follow. They are very general conditions in that most arbitrarily assigned torque curves do not lead to a motor that can be actually built. However, most commercially desirable speed torque curves can be approximated with that of an actual motor. This point will be illustrated in an example later.

Constant Torque Motors

To illustrate the application of equations 8 and 12 to actual motor design, a motor with the speed torque curve of Figure 1 is studied. This particular speed torque curve is chosen because it is simple and is satisfactory for general application. For slip less than one, we are interested in keeping the torque at its maximum value. At very high slip frequencies the torque is inversely proportional to the slip if the skin effect of the outermost cage is neglected. There is a transitional region between the constant torque characteristic and the declining torque characteristic. We do not know what the torque curve looks like in this region, and it would make the design job simpler if we leave this region flexible. However, in calculating the current curve for a slip equal to one or less, very slight error is introduced in assuming the dotted curve which is an extension of the two torque curves which meet at a slip s_2. $s_2>1$ and can be arbitrarily chosen, but it has to be large enough to allow an adequate transition region.

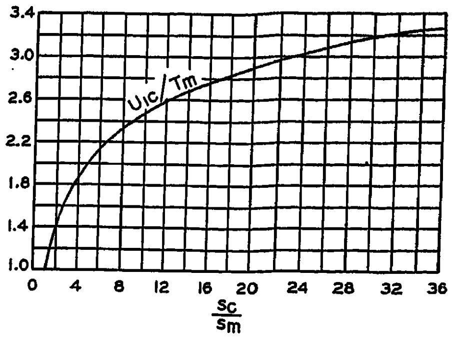


Figure 3. Reactive current of a constant torque motor

Since the skin effect of the outermost cage is neglected, its leakage reactance should be included in X_∞.

For deep bar motors, the skin effect is not negligible, and the motor torque is inversely proportional to the square root of slip for large values of slip. X_∞ does not include any of the rotor slot leakage except at the slot opening.

For the torque curve of Figure 1, the corresponding reactive power can be calculated from equation 8. First, equation 8 is written in terms of the slip s and s_c instead of the angular frequency. $s=\omega/\omega_1$, $s_c=\omega_c/\omega_1$.

Equation 8 becomes

$$U_c - U_0 = -\frac{2s_c^2}{\pi}\int_0^\infty \frac{\frac{T}{s}-\frac{T_c}{s_c}}{s^2-s_c^2} ds \tag{15}$$

Since the values of T and T_c are described by different functions in different ranges

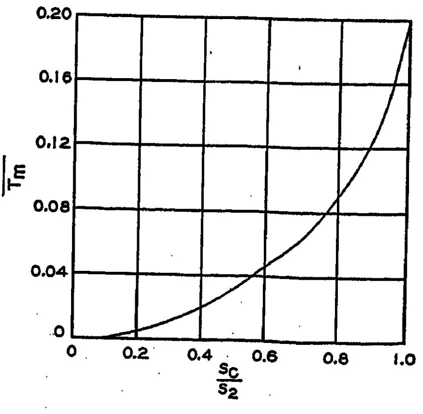


Figure 4. Added reactive current due to the declining torque characteristics at high slip frequency

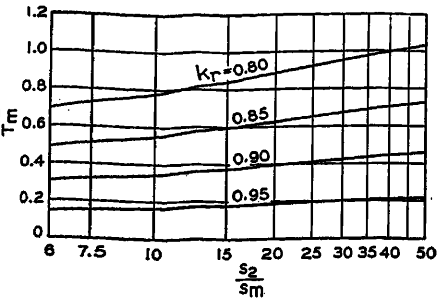


Figure 5. No load current as a function of the range of maximum torque

of s and s_c, it is necessary to separate the integral into three parts and integrate separately. Finally we obtain

$$U_c = U_0 + U_{c1} + U_{c2} \tag{16}$$

where, for

$s_c < s_m$:

$$U_{c1} = \frac{s_c^2 T_m}{s_c^2 + s_m^2} + \frac{2T_m}{\pi} \frac{s_c s_m}{s_c^2 + s_m^2} \ln \frac{s_m + s_c}{s_m - s_c} + \frac{T_m}{\pi} \ln \left[1 - \left(\frac{s_c}{s_m}\right)^2\right] \tag{17}$$

for

$s_m < s_c < s_2$:

$$U_{c1} = \frac{s_c^2 T_m}{s_c^2 + s_m^2} + \frac{2T_m}{\pi} \ln \left(1 + \frac{s_c}{s_m}\right) - \frac{T_m}{\pi} \frac{(s_c - s_m)^2}{s_c^2 + s_m^2} \ln \frac{s_c + s_m}{s_c - s_m} \tag{18}$$

and for all values of $s_c < s_2$:

$$U_{c2} = \frac{2T_m}{\pi} \left[1 - \frac{s_2}{2s_c} \ln \left(\frac{s_2 + s_c}{s_2 - s_c}\right) - \frac{1}{2} \ln \left(1 - \frac{s_c^2}{s_2^2}\right)\right] \tag{19}$$

for $s_c = s_2$:

$$U_{c2} = \frac{2T_m}{\pi} (1 - \ln 2) \tag{20}$$

The no load volt amperes U_0 can be evaluated from equation 14 which gives

$$U_0 = \frac{2T_m}{\pi} \frac{1 - k_{r\infty}}{k_{r\infty}} \left(2.57 + \ln \frac{s_2}{s_m}\right) \tag{21}$$

Equations 17–21 are plotted into curves in Figures 2, 3, 4, 5. These curves, together with equation 16, allow the current at any value of slip to be readily calculated.

Example

As an example to illustrate the application of these curves, suppose a motor has the following constants and requirements: $k_{r\infty} = 0.95$; full load slip $s_c = 0.02$; $s_2 = 1.50$; and maximum torque = starting torque = 2 times full load torque.

From Figure 2, we see that for a load point of $T_c = 0.5\ T_m$; $s_c = 0.268 s_m$; $U_{c1} = 0.13\ T_m$.

Then calculate

$s_m = 0.02 \div 0.268 = 0.0746$

$\frac{s_2}{s_m} = 20.1 \qquad \frac{s_c}{s_2} = 0.0133$

From Figures 4 and 5, we have

$U_{c2} = 0.000019 T_m$

$U_0 = 0.187 T_m$

From equation 16

$U_c = (0.187 + 0.130 + 0.000019) T_m = 0.317 T_m$

The full load power factor is

$$P.F. = \frac{T_c}{\sqrt{T_c^2 + U_c^2}} = \frac{0.5}{\sqrt{0.5^2 + 0.317^2}} = 84.5 \text{ per cent}$$

For the locked rotor condition

$s_c = 1, \qquad \frac{s_c}{s_m} = 13.4, \qquad \frac{s_c}{s_2} = 0.667$

From Figures 3 and 4 we have

$U_{c1} = 2.65 T_m \qquad U_{c2} = 0.054\ T_m$

$U_c = (0.187 + 2.65 + 0.054) T_m = 2.89\ T_m$

The starting kilovolt-amperes per horsepower is

$$\sqrt{1 + 2.89^2} \times 2 \times 0.746 \times \frac{1}{0.98} = 4.66$$

Curves of Power Factor and Starting Kilovolt-amperes Per Horsepower

The above example can be repeated for different values of $k_{r\infty}$ and full load slip, with fixed values of s_2 and of maximum torque to full load torque ratio. The result is plotted in Figures 6 and 7. These curves give the values of full load power factor and starting current for given values of full load slip and $k_{r\infty}$. Conversely, knowing $k_{r\infty}$ and starting current, the minimum full load slip without low accelerating torque can also be determined.

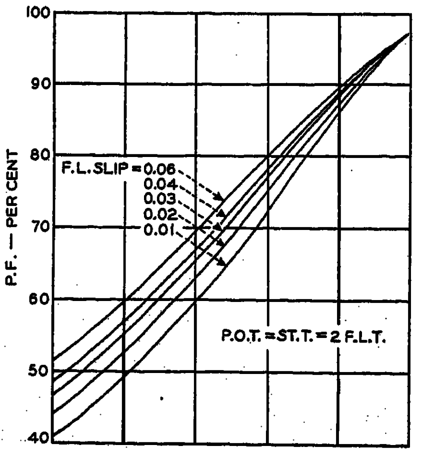


Figure 6. Power factor of a constant torque motor

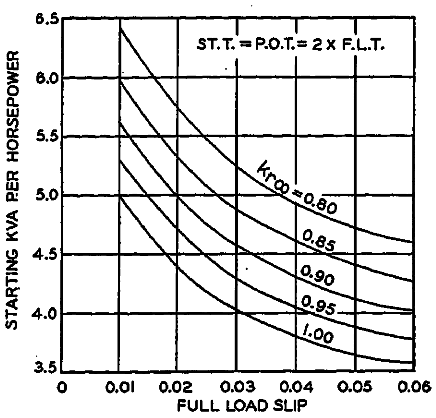


Figure 7. Starting current of a constant torque motor

The significance of these curves lies in that $k_{r\infty}$ is determined as soon as the stator slots, air gap, and the number of rotor slots of the external cage are given. These curves allow a pair of values of full load slip and starting current to be chosen which represent the best compromise under the given conditions. Then the inner cage can be designed accordingly.

Because of the saturation effect of the leakage reactance and the stator resistance, the starting current values of actual motors are about 10 to 20 per cent higher than the curve values. They nevertheless give a fairly close idea of what the starting currents are like for various values of full load slip and leakage factor

Leakage Reactance and Effective Rotor Resistance as a Function of Slip Frequency

The foregoing analysis explores the possible performances of a multiple cage induction motor. No attention is paid to the rotor itself. In the following, the same method will be employed to determine the rotor constants which would give the desired performance.

It is generally assumed that the magnetizing current can be represented by a reactance directly across the voltage source. In that case, the rotor current can be obtained by subtracting U_0 from U. However, such a method is subject to error, as the actual magnetizing current varies with the slip. A more rigorous analysis arrives at essentially the same result with slight modification on the motor constants.

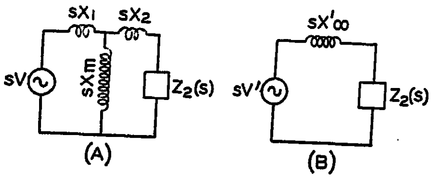


Figure 8. Equivalent circuits of multiple-cage induction motor

The equivalent circuit of an induction motor in terms of rotor frequency is shown in Figure 8(A). X_1 is the stator leakage reactance, X_2 is the rotor leakage reactance without including any leakage flux below the outermost cage. X_m is the magnetizing reactance. Z_2 is a function of the slip and

$$\lim_{s\to\infty}\left(\frac{Z_2}{s}\right)=0$$

The reactances X_∞ and X_0 are

$$X_\infty=X_1+\frac{X_2X_m}{X_2+X_m}$$

$$X_0=X_1+X_m$$

The admittance Y is the admittance looking into the circuit from the voltage source.

As far as the rotor current is concerned, the circuit of Figure 8(A) is equivalent to that of Figure 8(B) as a result of Thevenin's Theorem. In Figure 8(B)

$$X_\infty'=X_2+\frac{X_1X_m}{X_1+X_m}$$

$$V'=V\frac{X_m}{X_0}$$

Let Y' be the admittance looking into the circuit from the voltage source V' and $F'=jsnV'^2Y'$. The same analysis can be applied to show the relationship between the torque and the reactive components of the rotor current. The same set of equations is obtained except that in the new equations

$$U_0'=0 \qquad {}_{r\infty}'=1$$

and $X^{\infty\prime}$, V' take the places of X_∞, V. From the values of U' and T', the effective total leakage reactance X_e and rotor resistance R_e can be calculated. Since these values are stated in terms of line frequency, they are related to Y' by the following equation

$$R_e+jsX_e=\frac{1}{Y'} \tag{22}$$

By definition of F', it can be easily verified that

$$R_e=\frac{nV'^2T's}{U'^2+T'^2} \tag{23}$$

$$X_e=\frac{nV'^2U'}{U'^2+T'^2} \tag{24}$$

The d-c rotor resistance R_d and the total leakage reactance at no load X_d are approximately

$$X_d=\frac{nV'^2}{2T_m} \qquad R_d=X_ds_m \tag{25}$$

Substituting these expressions in equations 23 and 24 we obtain

$$\frac{R_e}{R_d}=\frac{2\left(\frac{T'}{T_m}\right)\frac{s}{s_m}}{\left(\frac{U'}{T_m}\right)^2+\left(\frac{T'}{T_m}\right)^2} \tag{26}$$

$$\frac{X_e}{X_d}=\frac{2\left(\frac{U'}{T_m}\right)}{\left(\frac{U'}{T_m}\right)^2+\left(\frac{T'}{T_m}\right)^2} \tag{27}$$

Since $U_0 = 0$ and U_{c2} is negligible for values of $s_c<0.5s_2$, equation 16 shows that U'/T_m is approximately U_{c1}/T_m over the working range of $s_c<1$. The values of U'/T_m and T'/T_m are read from Figure 2 and 3 as a function of s/s_m. The values of R_e/R_d and X_e/X_d are calculated and plotted in Figure 9.

Since $k_{r\infty}' = 1$, equation 12 becomes

$$\frac{nV'^2}{2X_\infty'}=\frac{1}{\pi}\int_{-\infty}^{\infty}Tdu=\frac{T_m}{\pi}\left(2.57+\ln\frac{s_2}{s_m}\right) \tag{28}$$

Substituting equation 25 in 28 we obtain

$$\frac{X_d}{X_\infty'}=\frac{1}{\pi}\left(2.57+\ln\frac{s_2}{s_m}\right) \tag{29}$$

Let R_∞ be the resistance of the outermost cage. At very high slip frequency, the effective rotor resistance is equal to R_∞ and the rotor current is equal to V'/X_∞', therefore the torque is

$$T=n\left(\frac{V'}{X_\infty'}\right)^2\frac{R_\infty}{s} \tag{30}$$

From Figure 1, for large values of s

$$T=T_m\frac{s_2}{s} \tag{31}$$

Eliminating s and T between equations 30 and 31, we obtain

$$\frac{R_\infty}{X_\infty'}=\frac{s_2T_mX_\infty'}{nV'^2}=\frac{X_\infty'}{X_d}\cdot\frac{s_2}{2} \tag{32}$$

Equations 29 and 32 allow the values of X_d and R_∞ to be determined from X_∞'. s_m is determined by the full load slip and pull out torque to full load torque ratio. s_2 should be large enough to allow the transition from the constant torque characteristics to the declining torque characteristics. However, too large a value of s_2 would increase the no load current and reduce the power factor, as shown in Figure 6. Usually a design can be made for $s_2 = 2$. For $s_2 = 1.5$, the transition region would extend to $s<1$ and reduce the starting torque slightly. However, in actual motors, the saturation effect on the leakage reactance usually brings up the starting torque to more than compensate this reduction.

A Direct Design Procedure

The analysis given above allows a multiple cage motor to be designed without very much trial and error. Usually the rotor lamination design is the most difficult and important. After the stator lamination, air gap, and the number of slots of the outermost cage are assumed, we can proceed in accordance with the following procedure:

1. Calculate X_1, X_2, X_m, X_∞, X_∞', X_0, $k_{r\infty}$ for the worst case. This is usually the motor with a maximum number of poles and shortest stack.

2. Choose a satisfactory performance point from Figures 6 and 7 or any other similar curves. If $k_{r\infty}$ is too low to obtain a satisfactory performance, means for increasing X_0 or reducing X_∞ must be used to improve $k_{r\infty}$.

3. After a satisfactory selection is made, determine

$\frac{X_d}{X_\infty'}$ from equation 29

$\frac{R_\infty}{X_\infty'}$ from equation 32

$$\frac{R_d}{X_\infty'}=\frac{X_d}{X_\infty'}s_m$$

These ratios determine the over-all slot constant, the slot area of the outermost cage, and the total slot area respectively.

4. Approximate the actual R_e and X_e to

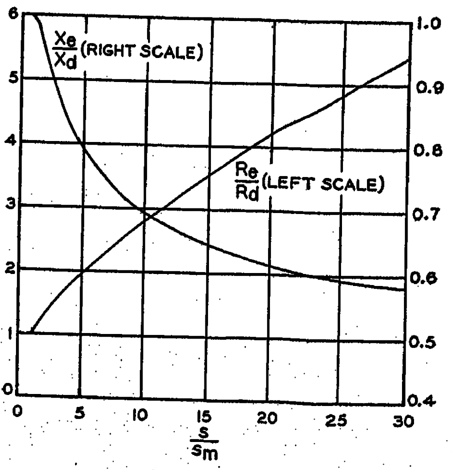


Figure 9. Effective resistance and leakage reactance of a constant torque motor as functions of slip frequency

the ideal values of Figure 9 by changing the shape and multiplicity of the inner cages while keeping the total area and slot constant to the predetermined values.

Conclusion

In the foregoing analysis, we have established the following facts about induction motors with multiple cage constructions:

1. By varying the shape and number of rotor slots, we can control either the torque or the reactive current as a function of slip. However, as soon as one of the two is fixed for all values of slip, the other component is automatically determined, see equation 8.
2. While in single-cage motors the total leakage reactance is determined by the maximum torque, a corresponding leakage reactance, in the more general case, is fixed by the torque area, equation 12. Therefore, in designing multiple cage induction motors, the torque area takes the same place as the maximum torque takes in single-phase motors.
3. Figures 6 and 7 show the starting torque, current, and full load power factor as a function of full load slip and $k_{r\infty}$. As the value of $k_{r\infty}$ does not depend on the inner cage construction, these curves fix the best performances that can be expected from multiple cage motors without bad accelerating characteristics. It also establishes $k_{r\infty}$, the leakage factor, as the most important single factor which determines the motor performance.
4. A direct design approach is shown which gives the values of slot areas and motors constants at different values of slip for any chosen, possible, performance.
5. Last, and perhaps most important of all, is that this general analysis tells where to look for improvements on cage construction. For example, most double-cage motors have trouble with poor power factor.

It would appear that the inner cage is the place to look for such improvements. However, since the basic leakage factor $k_{r\infty}$ is independent of the inner cage construction, any improvement on the power factor by modifying the inner cage is necessarily accompanied by undesirable effects on the full load slip, or starting and accelerating characteristics. The only effective way to improve motor performance in general is to increase the leakage factor $k_{r\infty}$. It can be accomplished by increasing the number of stator slots, or the number of slots in the outer cage, or any other effective measures. If the leakage factor is improved, and the necessary change is made on the inner cage to restore the desired speed torque characteristic, the improvement on full load power factor follows as a natural consequence.

List of Symbols

B = alternating current susceptance per phase of a locked motor

F = performance function, defined as $U+jT$

F' = performance function, defined as $U'+jT'$

G = alternating current conductance per phase of a locked motor

I = phase current of a running motor at line frequency and voltage

$k_{r\infty}$ = basic leakage factor, defined as $1-\dfrac{X_\infty}{X_0}$

n = number of phases

R_d = rotor resistance at very low slip

R_e = effective rotor resistance

s = slip

s_c = slip at calculated point

s_m = smallest slip, to which maximum torque is maintained

s_2 = slip at which the constant torque curve and the declining torque curve meet

T, T' = torque in synchronous watts

U = total reactive volt-ampere input to stator

U' = total reactive voltampere input to rotor

V = line voltage

V' = air gap voltage at no load

X_0 = total reactance per phase $= X_1+X_m$

X_1 = stator leakage reactance

X_2 = rotor leakage reactance, which does not include any slot leakage below the outermost cage

X_m = magnetizing reactance

X_∞ = basic total leakage reactance, defined as $X_1+\dfrac{X_2X_m}{X_2+X_m}$

X_∞' = basic total leakage reactance, defined as $X_2+\dfrac{X_1X_m}{X_1+X_m}$

X_d = total leakage reactance at no load

X_e = effective total leakage reactance

Y = admittance per phase of a locked motor. Y is a function of frequency. $Y=G-jB$

Z_2 = rotor impedance below the outermost cage, with the conductance of the outermost cage included

ω = slip frequency

ω_c = slip frequency of the calculated point

T_c, T_c', U_c, U_c' are T, T', U, U' at calculated point respectively

References

1. Network Analysis and Feedback Amplifier Design (book), H. W. Bode. D. Van Nostrand Company, New York, N. Y., 1945, pages 276–336.
2. Heat Losses in the Conductors of A-C Machines, W. V. Lyon. *AIEE Transactions*, volume 40, part II, 1921, pages 398–410.
3. Development of Low Starting Current Induction Motor, P. L. Alger. *General Electric Review* (Schenectady, N. Y.), volume 28, 1925, pages 499–508.
4. Theory of the Double Squirrel-Cage Motor, H. Lund. *Archiv fuer Elektrotechnik* (Berlin, Germany), volume 15, 1925, pages 121–37.
5. Boucherot Motors, H. Lund. *AEG Progress* (London, England), volume 2, May 1926, pages 87–91.
6. Polyphase Induction Motors with Double Squirrel-Cage Windings, C. W. Kincaid, D. F. Alexander. *Electric Journal* (East Pittsburgh, Pa.), volume 23, 1926, pages 15–19.
7. Squirrel Cage Induction Motors for High Starting Torque and Low Starting Current, M. Liwschitz. *Siemens-Schuckert Review* (Berlin, Germany), volume 2, 1926, pages 60–66, 87–95.
8. Double Squirrel Cage Motor, R. Baffrey. *Elektrotechnik und Maschinenbau* (Vienna, Austria), volume 46, 1928, pages 749–54.
9. Squirrel Cage Induction Motors, D. B. Hoseason. *Institution of Electrical Engineers, Journal* (London, England), volume 66, 1928, pages 410–42.
10. Line-Start Induction Motors, C. J. Koch. *AIEE Transactions*, volume 48, part I, 1929, pages 232–35.
11. Circle Diagram of Boucherot Motor, M. Krondl. *Elektrotechnik und Maschinenbau* (Vienna, Austria), volume 49, 1931, pages 161–67.
12. Modern Polyphase Induction Motors (book), F. Punga, O. Raydt, translated by H. M. Hobart. Sir Isaac Pitman & Sons, London, 1933.
13. Influence of Mutual Inductance between Starting and Operating of a Double Squirrel Cage Motor, W. Moenning. *Electrotechnik und Maschinenbau* (Vienna, Austria), volume 57, 1939, pages 450–53.
14. Practical Design of Double Squirrel Cage Motors, D. Oerlel, G. Jekelfalussy. *Engineers' Digest* (New York, N. Y.), volume 4, 1947, pages 253–56.

No Discussion

Saturation Effect of Leakage Reactance

SHELDON S. L. CHANG
ASSOCIATE AIEE

T. C. LLOYD
MEMBER AIEE

IT HAS long been an observed fact that the leakage reactance of an induction motor is not a constant, but varies with the saturation. Locked rotor tests at full voltage reveal reactances which are too low for correct running performance. On the other hand, calculated values of leakage reactance, useful for running performance, need adjustment downward before they can be used for calculating locked rotor conditions accurately. The need for this adjustment in polyphase induction motors is especially acute with attention centered through National Electrical Manufacturers Association and other Standards, upon starting current limitations and starting torque requirements.

It is the purpose of this paper to discuss the nature of the saturated leakage reactance and to show a practicable though somewhat empirical method of obtaining useful results.

What Causes Saturation?

The leakage reactance of an induction motor is considered to consist of the components discussed briefly in the following:

End Leakage

This is usually not saturable because the flux paths are non-magnetic for a relatively great distance.

Slot Leakage

This flux system can be separated into two parts, the first of which includes the yoke and the sides of the teeth. At standstill the flux density in this part is about equal to, or less, than the running densities and hence this major portion of the slot leakage path is not saturable.

The part of the slot leakage flux which travels across the tooth tip shows saturation and calculations must take into account the areas of the tips.

Skew Leakage

This component causes saturation effects because of the excessive magnetizing currents at the ends of the motor. Under locked rotor conditions, the stator and rotor currents at the center of the motor are about equal and opposite. At the two ends there is a phase shift in these currents of $\theta_k/2$. In this case, θ_k is the angle of skew in electrical radians.

The vector sum of the stator and rotor currents at the two ends is approximately equal to $I_s\theta_k/2$. This current would magnetize the motor at the two ends. Approximately

$$I_s=\frac{V}{X}$$

and

$$\theta_k=\frac{\pi k}{S}$$

Then the magnetizing current at the two ends is approximately

$$I_{me}=\frac{V}{X}\frac{\pi k}{2S}$$

where

S=number of stator slots per pole
k=rotor skew in stator slot pitch
V=volts per leg
X=leakage reactance

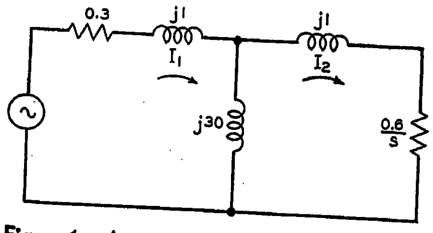


Figure 1. An equivalent circuit for one phase of the polyphase motor

Consider a typical example of a motor with nine slots per pole and a leakage reactance of 5 per cent of the magnetizing reactance, X_m. Then

$$I_{me}=\frac{V}{X_m}\times 20\times\frac{\pi k}{18}$$
$$=3.5k\frac{V}{X_m}$$

Now with a rotor bar skew of one stator tooth pitch, the "end" magnetizing current is 3.5 times the normal magnetizing current. When it fails to produce the proportional air gap flux, the skew leakage reactance is effectively saturated. This effect is greater if $k>1$; but it is not so dependent upon the number of slots per pole as the equation indicates, because the ratio X/X_m is usually reduced with the increase in the number of slots.

Zigzag and Belt Leakage

These are both of the same general nature, the former being caused by tooth harmonics. Belt leakage involves harmonics of a lower order. Since the stator and rotor harmonic magnetomotive forces usually do not cancel each other as the fundamental magnetomotive forces do, the resultant harmonic magnetomotive forces reach very high values under locked rotor conditions. As they saturate the tooth faces and fail to induce a proportional air gap harmonic flux, the zigzag and belt leakage reactances show the effects of saturation.

Example. Assume a typical squirrel cage induction motor has an equivalent circuit as shown in Figure 1. Assume that stator and rotor slots, under locked rotor conditions, are lined up as shown in Figure 2, which also indicates the stator coil positions.

In Figure 1 we know

$$\frac{I_2}{I_1}=\frac{j30}{j31+0.6}\text{ or }0.967\underline{/1.1}\text{ degrees}$$

Paper 49-240, recommended by the AIEE Rotating Machinery Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17-21, 1949. Manuscript submitted July 20, 1949; made available for printing September 22, 1949.

Sheldon S. L. Chang and T. C. Lloyd are with Robbins and Myers, Inc., Springfield, Ohio.

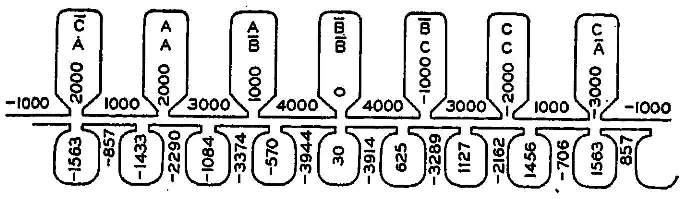


Figure 2 (above). Comparative values of the magnetomotive forces effective at the various tooth and slot positions

The bar notations over A, B, C indicate reversed polarity of the coil sides of the respective phases

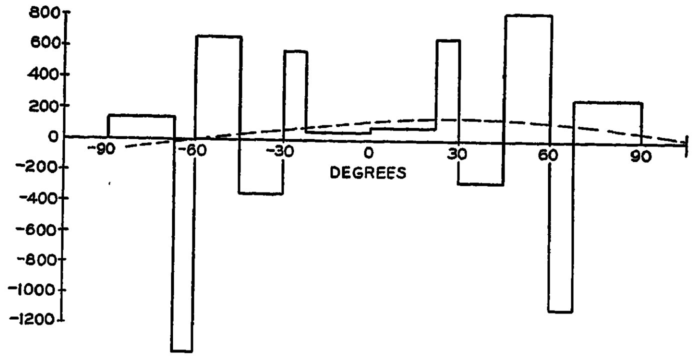


Figure 3 (right). The magnetomotive forces in the air gap

Consider the instant when phase A = 1,000-ampere conductors per coil side; phase $B = 0$; and phase C = 1,000-ampere conductors per coil side

The instantaneous ampere conductors in each stator slot are shown by the numbers in the slots. The magnetomotive force of the teeth also are shown by the numbers on the teeth of Figure 2.

The maximum rotor conductor current is

$$M \times K_w \times \frac{S \times 2}{S''}\,\frac{I_2}{I_1}$$

where

M = peak stator ampere conductors per coil side
K_w = the winding factor
S = stator slots per pole
S'' = rotor slots per pole

Then for the example

$$\frac{1{,}000}{0.866} \times 0.933 \times \frac{6 \times 2}{8} \times 0.967 = 1{,}616 \times 0.967 = 1{,}563 \text{ amperes (1 conductor)}$$

Because of the phase difference between the stator and rotor currents, the rotor bar in the center has a current of 1,563 sin 1.1 degrees, and the currents in the other slots are calculated by adding $\pm n \times 22.5$ degrees to the angle 1.1 degrees. The rotor bar currents are shown as numbers in the rotor slots. The magnetomotive forces of the rotor teeth are calculated and shown on the rotor teeth, Figure 2.

The sum of the stator and rotor magnetomotive force's gives the net magnetomotive force in the air gap. This is plotted in Figure 3. While it exhibits a variable pattern, its magnitude should be compared to the fundamental component of the air gap magnetomotive force. Such a comparison is quite difficult from examination of the actual magnetomotive force of Figure 3, but can be made by calculating the magnetizing current and then the peak value of the ampere-turns. From the circuit we know

$$\frac{I_m}{I_1} = \frac{j1+0.6}{j31+0.6} = \frac{1.168\underline{/59} \text{ degrees}}{31\underline{/88.9} \text{ degrees}} \text{ or } 0.0376\underline{/-29.9} \text{ degrees}$$

The peak value of the fundamental component of the air gap magnetomotive force is

$$\frac{1{,}000}{0.866} \times 0.0376 \times 2 \times 0.933 \times 1.5 \times \frac{4}{\pi} = 154 \text{ ampere-turns}$$

This is plotted in Figure 3 as the dotted line.

At running load, the maximum value of the air gap magnetomotive force is about 250 ampere turns. Figure 3 shows that the maximum value of the harmonic magnetomotive force at locked rotor condition is about five times this value. Since the stator and rotor tooth faces are saturated to a reasonable degree with normal magnetizing current, this large harmonic magnetomotive force evidently cannot induce a harmonic flux in proportion. Consequently the zigzag leakage reactance is reduced at locked rotor and pull out conditions.

Generally the zigzag leakage permeance and its degree of saturation can both be reduced by increasing the number of stator and rotor slots. However, between motors of different speeds with the same punchings, there is no conspicuous difference in the saturation factor. This fact is justifiable theoretically because with the total number of slots and ampere conductors per slot kept constant, both the harmonic magnetomotive force and the fundamental magnetizing magnetomotive force increase in proportion with the number of poles.

A Semiempirical Method of Determining Saturated Leakage Reactance

Though the physical picture of the saturation effect on leakage reactance is not difficult to see, to calculate it rigorously is nearly impossible. It would be necessary to solve nonlinear partial differential equations for the magnetic circuit. The boundary conditions are more complicated than anything that has ever been solved in mathematical physics. In addition, it would be necessary to solve scores of these differential equations for one motor, corresponding to different stator and rotor slot positions and different stator and rotor currents.

A semiempirical method will be shown to obtain a simple and useful solution.

The leakage permeance will be separated into two parts. The unsaturable leakage permeance consists of the end leakage and the main portion of the slot leakage. The saturable leakage permeance consists of the tooth tip, zigzag, skew, and belt leakage permeances. Suppose we plot the leakage flux due to these saturable permeances as a function of winding current. At low values of current, the flux increases with current linearly, with a slope determined by the unsaturated value of the saturable leakage permeance. At high values of current, the flux levels off and approaches a maximum value ϕ_m which can be determined in terms of the dimensions and multiplicity of the stator and rotor slots. The following flux values are in lines per pole (peak value):

ϕ = actual value of the saturable leakage flux
ϕ_m = maximum value of the saturable leakage flux
ϕ_0 = the value of the saturable leakage flux if it were not saturable

The actual space distribution of the saturable portion of the leakage flux depends to a great extent upon the primary winding current and the relative position between the rotor and the stator. Its effect is to produce a reactive voltage drop in the stator winding, either by direct linking with the stator winding or by reflecting into the stator through the rotor circuit. The sum total of this effect can be expressed as the equivalence of a sinusoidally distributed flux ϕ linking the stator alone. This equivalence is useful in determining the maximum value ϕ_M from actual lamination dimensions.

Evidently ϕ_o is proportional to the winding current. If we plot ϕ/ϕ_m against ϕ_o/ϕ_m instead of ϕ against winding current, a universal saturation curve would be obtained. At low values of ϕ_o/ϕ_m the curve is a straight line with unity slope

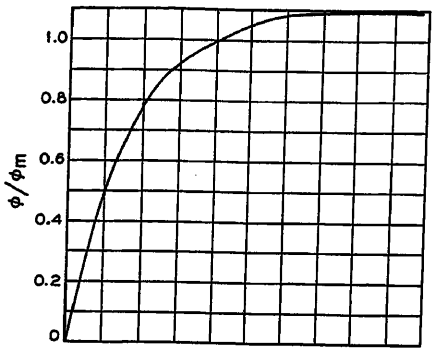


Figure 4. A universal saturation curve for determining the saturable leakage reactance

because ϕ and ϕ_0 are then equal. At high values of ϕ_0/ϕ_m; ϕ/ϕ_m approaches unity, as shown in Figure 4. Theoretically the curves for different motors should agree only at the high and low ranges of ϕ_0/ϕ_m, however, the curve of Figure 4 was found to be applicable to all the 31 motors analyzed.

One difficulty which prevents the making of a direct determination of this curve is that of separating the saturable components from the unsaturable components of leakage reactance. To make an analysis of a motor having slot shapes which are easy to calculate is unreliable, because any empirical result has to be based on the data of a large number of normal and representative samples. The data obtained on a particularly shaped sample motor may deviate materially from that of average motors.

Let $\mathcal{P}_2$ and $\mathcal{P}_2'$, denote the unsaturated and saturated values of the saturable leakage permeance. Then

$$\mathcal{P}_2'=\mathcal{P}_2\times\frac{\phi}{\phi_m}\div\frac{\phi_0}{\phi_m} \tag{1}$$

This semiempirical universal saturation curve method of determining the saturable leakage reactance can be applied readily to any design procedure an engineering office happens to be using. It is necessary to determine the proportional constant between ϕ_0 and I, and the maximum saturated leakage flux ϕ_m. These values have to be determined in line with the traditional method of calculating leakage reactance. It is believed that improvements on existing methods should prove more useful than attempting an entirely new approach.

In an earlier paper[1], the writers have explained a method used in calculating leakage reactance of squirrel cage induction motors. In this analysis, the skew leakage reactance was not calculated, but the slot, zigzag, and end leakage reactances were multiplied by experience factors. Generally, with standard rotor skew of one stator slot, the skew leakage is not only small, but also roughly in proportion to the other components of leakage permeances. With skew leakage absorbed in other components, the saturable permeance consists of the zigzag leakage and the tooth tip leakage only. It is: (See reference for additional symbols.)

$$\mathcal{P}_2=\frac{9.7D}{S_1S_2\Delta}\left(\frac{Tc'+Tc''}{\lambda'+\lambda''}-0.5\right)^2(0.8_p+0.2)L+$$
$$4.1\left(\frac{d'}{e'S_1}+\frac{d''}{e''S_2}\right)(0.625_p+0.375)L \tag{2}$$

The unsaturable permeance consists of the slot and end leakages. It is

$$\mathcal{P}_1=4.1\left(\frac{K_{sc}'}{S_1}+\frac{K_{sc}''}{S_2}\right)(0.625_p+0.375)L+$$
$$\frac{\lambda}{q}K_e(1.25p-0.25)$$

$$K_{sc}'=K_s'-\frac{d'}{e'} \qquad K_{sc}''=K_s''-\frac{d''}{e''} \tag{3}$$

The constants given in the foregoing equations are larger than those in the earlier paper, because the unsaturated values are larger than the compromise values which the original equations are intended for. The saturable leakage inductance per phase is $C^2n\mathcal{P}_2$. ϕ_0 is obtained by multiplying the inductance by the peak value of stator current and then divided by the effective turns per phase. It is

$$\phi_0=C^2n\mathcal{P}_2\times\sqrt{2}I\div\frac{CKw}{2}$$
$$=2\sqrt{2}\left(\frac{n\mathcal{P}_2}{K^2_w}\right)(CIK_w) \tag{4}$$

The maximum flux ϕ_m consists of the maximum zigzag and tooth tip flux. The maximum zigzag flux can be determined by the overlap method. Consider the zigzag flux that links the rotor slot S in Figure 5. Its value is determined by the smaller one of the two overlapping areas on the left and right of S. The relative position repeats itself as slot S moves one stator tooth pitch. The average value of the smaller overlapping area is

$$A_2=\frac{2L}{\lambda'}\int_0^{\frac{1}{2}(t'-e'')}xdx=L\;\frac{(t'-e'')^2}{4\lambda'}$$
$$=\frac{L\;\lambda'-(e'+e'')^2}{4\lambda'} \tag{5}$$

Similiarly the zigzag flux linking a stator slot has an average value of the smaller overlapping area of

$$A_1=\frac{2L}{\lambda''}\int_0^{\frac{1}{2}(t''-e')}xdx=L\;\frac{[\lambda''-(e'=e'')]^2}{4\lambda''} \tag{6}$$

Adding these two components, we find the average limiting area for the zigzag flux to be

$$A_{zz}=\frac{L}{4}\left[\lambda'-2(e'+e'')+\frac{(e'+e'')^2}{\lambda'}+\right.$$
$$\left.\lambda'-2(e'+e'')+\frac{(e'+e'')^2}{\lambda}\right]$$
$$=\frac{L}{4}\left\{\frac{[\lambda'+\lambda''-2(e'+e'')]^2}{\lambda'+\lambda''}+\right.$$
$$\left.(e'+e'')^2\left[\frac{1}{\lambda'}+\frac{1}{\lambda''}-\frac{4}{\lambda'+\lambda''}\right]\right\}$$
$$=\frac{L}{4}\left\{\frac{[\lambda'+\lambda''-2(e'+e'')]^2}{\lambda'+\lambda''}+\right.$$
$$\left.\frac{(e'+e'')^2(\lambda'-\lambda'')^2}{\lambda'\lambda''(\lambda'+\lambda'')}\right\} \tag{7}$$

In an average motor the second term is negligible compared to the first term. Therefore

$$A_{zz}=\frac{L}{4(\lambda'+\lambda'')}(\lambda'+\lambda''-2e'-2e'')^2 \tag{8}$$

The tooth tips furnish another passage for the saturable leakage flux. Its area is $(d'+d'')L$. The maximum saturable leakage flux per pole should be proportional to the total area. The proportionality constant is determined from test data to be 160,000. Therefore

$$\phi_m=160{,}000L\left[\frac{(\lambda'+\lambda''-2e'-2e'')^2}{4(\lambda'+\lambda'')}+\right.$$
$$\left.d'+d''\right] \tag{9}$$

Besides neglecting the second term in equation 7, another source of error is due to the fact that A_1 and A_2 have considerable portion in common. However, it is quite difficult to separate this common portion. Equation 9 is derived to determine how the maximum flux depends on the various slot dimensions. The errors are absorbed partially in the experimental constant 160,000.

The above example illustrates how the universal saturation curve method can be merged into an established design method. From equations 4 and 9, the values of ϕ_0 and ϕ_m can be calculated. Then the value of ϕ/ϕ_m is determined from Figure 4. The saturated value of the

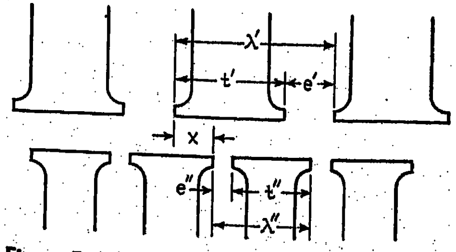


Figure 5. An analysis of the zigzag leakage flux

zigzag and tooth tip permeance then is calculated from equation 1. The total leakage permeance is $\mathcal{P}_1+\mathcal{P}_2'$.

The test data on 31 motors were taken to check the method. There are 61 values of leakage reactance in different degrees of saturation. The calculated leakage reactance agrees with test within 5 per cent in 39 cases, between 5 per cent to 10 per cent in 21 cases. The deviation is 11 per cent in one case. However, most of the deviation is introduced in calculating the value of unsaturated permeances $\mathcal{P}_1$ and $\mathcal{P}_2$. The agreements of saturation factors are all within 5 per cent, with but one exception of 6 per cent. The ratios of $(\mathcal{P}_1+\mathcal{P}_2')/(\mathcal{P}_1+\mathcal{P}_2)$ are in the range of 0.616 to 0.951.

Reference

1. REACTANCES OF SQUIRREL-CAGE INDUCTION MOTORS, T. C. Lloyd, Victor F. Giusti, S. S. L. Chang. *AIEE Transactions*, volume 66, 1947, pages 1349–55.

No Discussion

The Torques of the Synchronous Tie—A Steady-State Analysis

L. A. FINZI
ASSOCIATE AIEE

H. M. McCONNELL
ASSOCIATE AIEE

IN A previous paper[1] methods have been given to determine the current loci and thus the performance of synchronous ties consisting of two identical polyphase induction machines with interconnected rotors and with additional secondary impedance networks. The stator currents of both induction machines are functions of their common slip s and of the phase angle δ between voltages induced in corresponding rotor windings. It was found that the vectors representing these currents at any given slip describe a circular locus (called delta-circle) when δ varies; these circles are constructed easily from the two loci obtained for the stator currents by varying s and keeping $\delta=0$ and $\delta=\pi$ respectively. A torque axis can be drawn for each delta-circle.

In this paper the operation of synchronized drives of this kind is examined further in terms of the torque requirements which occur in practical applications.

Motor Torques and Synchronizing Torques

The torques impressed upon the rotors of the machines of the tie by their revolving air gap fields are functions of s and δ. As derived in the Appendix for any given slip these torques can be written in the form

$$\tau_{\text{leader}}=\tau_0+(\tau_\pi-\tau_0)\sin^2\frac{\delta}{2}+\tau_k\sin\delta \quad (1)$$

$$\tau_{\text{lagger}}=\tau_0+(\tau_\pi-\tau_0)\sin^2\frac{\delta}{2}-\tau_k\sin\delta \quad (2)$$

Torques acting in the direction of field rotation are positive. Consistently with the notations used in reference 1 the subscript "leader" applies to the machine whose induced rotor voltages lead in time the corresponding rotor voltages of the other machine, for which the subscript "lagger" is used. As should be expected, 1 and 2 are of the same type as those for the power flow in a general impedance circuit with two voltage sources.[2] τ_0 and τ_π are the torques acting upon either rotor at $\delta=0$ or respectively at $\delta=\pi$. At any other angle τ_{leader} and τ_{lagger} differ, their difference being largest (and equal to $2\tau_k$) at $\delta=\pi/2$.

The notations introduced are illustrated in Figure 1. Analytical expressions also are derived in the Appendix for τ_0, τ_π, and τ_k in terms of the design data of the tie and of the slip in equations 11, 12, and 13. Equations 1 and 2 can be rewritten in the form

$$\tau_{\text{leader}}=\tau_m+\tau_s \quad (3)$$

and

$$\tau_{\text{lagger}}=\tau_m-\tau_s \quad (4)$$

where

$$\tau_m=\text{motor torque}=\frac{1}{2}(\tau_{\text{leader}}+\tau_{\text{lagger}})=\tau_0+(\tau_\pi-\tau_0)\sin^2\frac{\delta}{2} \quad (5)$$

and

$$\tau_s=\text{synchronizing torque}=\frac{1}{2}(\tau_{\text{leader}}-\tau_{\text{lagger}})=\tau_k\sin\delta \quad (6)$$

as also illustrated in Figure 1, for $\delta=65$ degrees.

When $s>0$, τ_{leader} is also positive, thus positive power is transferred through the air gap of the leader into its rotor, and the a-c line supplies this power and the stator losses. Moreover if $\tau_m>\tau_s$, τ_{lagger} is also positive, so that positive power is transmitted through the air gap of the second machine into its rotor and again the sum of this power and of the stator losses is supplied from the line. On the other hand if $\tau_m<\tau_s$, power is transmitted from the rotor of the lagger into its stator and given back to the line after deduction of the stator losses. In this case the machine called lagger, seen from its stator terminals, behaves as an electric generator. If the slip is negative, the situation reverses as one or both machines act as induction generators; operation of the tie in the negative slip region however is practically unlikely, as will be discussed later.

Assuming as unit power the rated mechanical output of one machine and as unit torque this power divided by rated rotor speed, it also is seen that $2\tau_m$ represents the total per-unit power transmitted from both stators through the air gaps. This power is positive for positive slips and negative for $s<0$. The over-all per-unit mechanical power delivered by both induction machines (if friction and windage are considered part of the mechanical loads) is $2\tau_m(1-s)$ while $2\tau_m s$ represents the losses in the resistances of the rotor windings and external rotor network.

Values of τ_0, τ_π, and τ_k, obtained from equations 11, 12, and 13, are plotted in per-unit in Figure 2 as functions of s for a synchronous tie consisting of two 10-horsepower 1,120-rpm 60-cycle 3-phase induction motors. Assuming as unit voltage the rated stator phase voltage and as unit current the rated mechanical output per phase in watts divided by rated stator phase voltage, the impedances of each machine are in per-unit: stator resistance $R_1=0.033$; stator leakage reactance (at rated frequency) $X_1=0.124$; rotor resistance $R_2=0.05$; rotor leakage reactance $X_2=0.124$ (both reduced to primary terms.) Moreover, for a representation of the magnetizing branches as

---

Paper 49-241, recommended by the AIEE Rotating Machinery Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 19, 1949; made available for printing September 14, 1949.

L. A. FINZI and H. M. MCCONNELL are both with the Carnegie Institute of Technology, Pittsburgh, Pa.

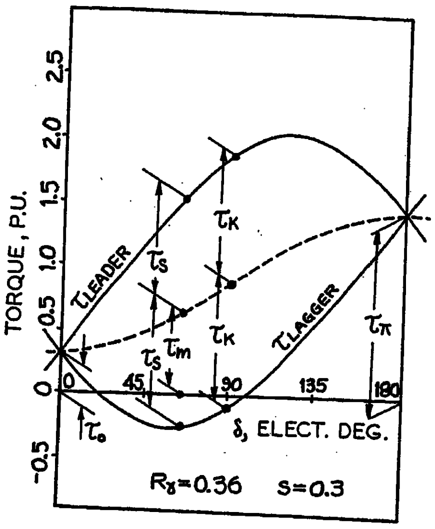


Figure 1. Example of torque-angle characteristics at constant speed

used in the top of Figure 8, Appendix, it is $R_0=19.40$ and $X_0=2.15$. Figure 2(A) gives curves calculated for a simple tie with direct rotor connection without external impedances in full lines, while the dotted lines refer to the addition of shunt inductances ($X_{\gamma L}=0.36$ per phase). Figure 2(B) refers to additional shunt resistors ($R_\gamma=0.36$ in full lines and $R_\gamma=0.09$ in dotted lines). In all these cases τ_π is represented by the same curve, since the presence of shunt impedances of any kind is not felt in the operation at $\delta=\pi$. In fact this curve coincides with the torque-slip characteristic of a machine operating singly with short-circuited slip rings. It also may be noted that in Figure 2(A) for the case of $R_\gamma=\infty$, τ_0 is always zero since for $\delta=0$ no currents flow in the rotor at any slip, while for finite values of R_γ the curve expressing τ_0 in Figure 2(B) can be obtained from the τ_π curve simply by changes of the slip scale; in fact at $\delta=0$ each machine behaves as a conventional induction machine having an external rotor rheostat with phase resistance $2R_\gamma$.

The use of shunt capacitors seems less advisable in view of limitations imposed by the danger of excessive magnetic saturation for the machines or by the size of capacitors needed, although combinations of capacitors with other network elements may be of interest. More elaborate rotor networks including series and shunt branches in symmetrical T in each phase also may be investigated by means of equations 11, 12, and 13.

Operation of the Synchronous Tie Under Load

The general arrangement of a synchronized drive is shown in Figure 3. The two remote stations A and B each consist of a driver D (prime mover or motor of any kind) driving a mechanical load L and coupled to one of the induction machines of the synchronous tie.

For any given situation it is presumed that the torque-speed characteristics of loads and drivers are known; thus the resultant mechanical torque, obtained by algebraic summation of the torques of driver and load, is a known function of speed for each station. (Some obvious care is needed to co-ordinate this information with the previous analysis[1] in which units of power and torque have been established on the basis of the name plate data of the induction machines and in which definite sign conventions already have been set for speed and torque in terms of the revolving air-gap fields). Figure 4(A) indicates with self-explanatory subscripts the torque-speed characteristics of drivers and loads as well as the resultant torques T_a and T_b of the two mechanical systems, plotted as functions of speed or slip for a hypothetical case in which "operation with the field" is intended; here driver torques act in the same direction as positive air-gap torques of the induction machines, and therefore are positive quantities. It also is conceivable and common, however, to operate the synchronous tie with fields revolving in the direction opposite to the torques of the drivers; for this type of "operation against the field" the resultant torque-speed characteristics of the mechanical systems have been redrawn in Figure 4(B) in order to avoid conflicts of sign conventions.

If the synchronous tie were removed, each station would operate independently at some speed n_a and n_b respectively, for which the equilibrium conditions $T_a=0$ and $T_b=0$ are satisfied. It is the task of the machines M_a and M_b of the tie to supply synchronizing torques, acting in opposite directions at the two stations, so as to obtain operation synchronized at a common speed. At the same time, however, M_a and M_b also supply motor torques acting in the same direction at both stations. In steady-state synchronized operation the speed (or the slip s) and the angle δ adjust themselves so as to satisfy simultaneously for each station the condition that the sum of all torques be zero, the summation including now the torques of the mechanical system and the air-gap torque as well. These equilibrium equations can be written

$$T_{\text{leader}}+\tau_{\text{leader}}=0 \qquad (7)$$

and

$$T_{\text{lagger}}+\tau_{\text{lagger}}=0 \qquad (8)$$

where τ_{leader} and τ_{lagger} are given by equations 1 and 2 respectively while T_{leader} and T_{lagger} are the resultant torques of the mechanical systems at the station whose machine is respectively the electrical leader or lagger. It may be noticed that the rotor of the station with algebraically larger mechanical torque is more advanced in the direction of positive field rotation; thus for $s>0$ its induced phase voltages lag in time behind the voltages induced in the corresponding rotor phase windings of the other machine; this consideration establishes to which station the subscript leader or lagger applies. (In the example of Figure 4(A) station A is the leader, while it is the lagger in Figure 4(B).)

The equilibrium conditions can also be written as

$$T_m+\tau_m=0 \qquad (9)$$

and

$$T_s+\tau_s=0 \qquad (10)$$

where τ_m and τ_s are given by equations 5 and 6, while the further notations $T_m=1/2\,(T_{\text{leader}}+T_{\text{lagger}})$ and $T_s=1/2(T_{\text{leader}}-$

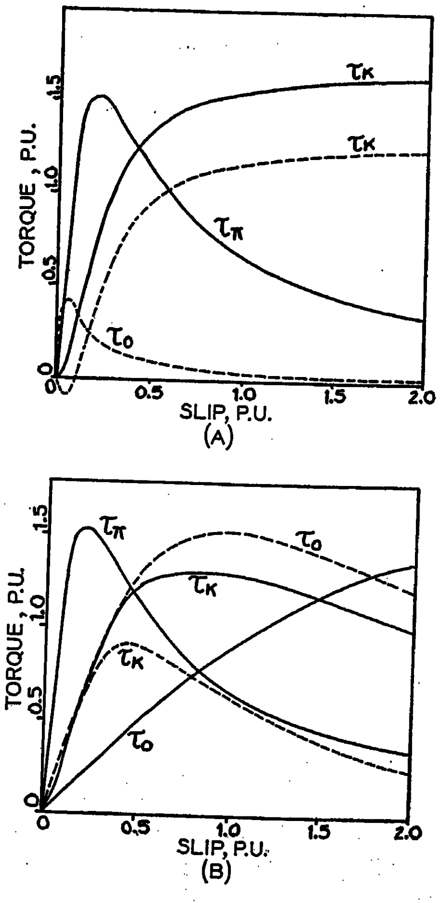


Figure 2. Plots of functions τ_0, τ_π, τ_k

A ——— simple tie without networks
- - - - shunt reactors $X_{\gamma L}=0.36$

B ——— shunt resistors $R_\gamma=0.36$
- - - - shunt resistors $R_\gamma=0.09$

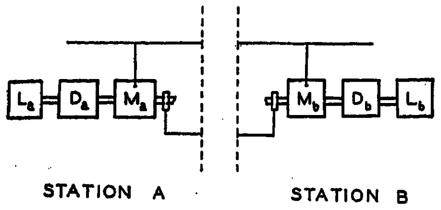


Figure 3. General type of synchronized drive

T_{lagger}) are introduced now. (Thus in the example of Figure 4(A) $T_s=1/2\,(T_a-T_b)$ while in Figure 4(B) $T_s=1/2\,(T_b-T_a)$.) The physical meaning of equations 9 and 10 is evident: if condition 9 is not met, both stations are going to accelerate or decelerate; on the other hand the relative angular displacement of the two shafts is going to increase or decrease if condition 10 is not satisfied.

A direct determination of s and δ from equation 7 and 8 or 9 and 10 is hardly possible by algebraic means, even if the torques of drivers and loads are assigned in the form of simple functions of the speed. A convenient procedure is suggested by the chart in Figure 5 in which families of curves $\tau_m=$constant and $\tau_s=$constant are drawn in terms of the two independent variables s and δ; the synchronous tie for which this chart applies consists of the two induction machines described previously with additional resistors $R_\gamma=0.09$ in shunt to the rotor interconnections. For the mechanical situation represented by the graphs of Figure 4(A), $-T_m$ and $-T_s$, which are known functions of s, have been plotted on the chart, by scanning the families of curves $\tau_m=$constant and $\tau_s=$constant. Thus, the plot of $-T_m$ gives values of s and δ satisfying equation 9, and likewise the plot of $-T_s$ satisfies equation 10. Hence the slip s and the rotor voltage angle δ for synchronized operation are determined by the intersection of the two plots. The situation represented in Figure 4(B) could be examined similarly, extending the chart to values of higher positive slips and plotting on it the functions $-T_m$ and $-T_s$ obtained in Figure 4(B) for the assumption of operation against the field.

Different situations arising from conceivable changes of drivers' or loads' characteristics can be investigated likewise and limits of steady-state stability can be determined, the synchronized operation becoming impossible when the plots of $-T_m$ and $-T_s$ no longer cross on the chart. A number of somewhat less general situations, for which these graphical procedures simplify or even become unnecessary, will be discussed later.

If factors of design are changed, for example by variation of the chosen impedances of the rotor network, the chart obviously needs redrawing. But the calculations required are easy, at least for commonly simple types of networks, if one assigns values for s and solves equations 5 and 6 for δ. Moreover, some preliminary insight on the influence of changes of parameters is gained if one considers, for example, that the intersections of the family of curves $\tau_m=$constant with the horizontal at $\delta=\pi$ do not vary for any changes of impedances in the shunt branch. The intersections of the same family of curves with the horizontal at $\delta=0$ also are found by linear readjustments of the slip scale, if in a simply resistive shunt network R_γ is changed. The family $\tau_s=$constant is symmetrical about the horizontal at $\delta=\pi/2$ and has kernels corresponding to maximum values of τ_s which are obtained at $\delta=\pi/2$ and at a slip which maximized τ_k. For the simply resistive shunt network considered, there is only one such kernel in the region of positive slips, which wanders towards higher slips if R_γ increases and goes to $s=$infinity in the limiting case of $R_\gamma=$infinity.

For the sake of completion some remarks can be made on the performance of the drive at negative slips. Since for $s<0$, τ_m is negative and over-all electric power is delivered through the air gaps to the a-c line at the expense of the mechanical systems, operation of this kind is not generally wanted. This situation may arise accidentally, however, when a reversal of the load torques unduly accelerates systems originally operating with the field. In this case, while both stations speed up into the region of negative slips, the machine with the largest acceleration gradually becomes more advanced in the direction of the revolving field; this however is slower than the rotors so that the machine considered is the electric leader. For s negative, τ_k remains generally positive and therefore, for $0<\delta<\pi$, $\tau_{\text{leader}}>\tau_{\text{lagger}}$; that is, the more advanced machine is pushed further ahead by its air-gap torque and the angle of the rotors increases to 180 electrical degrees and beyond; when this new condition is reached the machine considered becomes the electric lagger and synchronizing action in regenerative operation can take place. However, the transition through $s=0$ implies a temporary loss of synchronizing action and the two stations most likely will fall out of step at this point. Similar considerations apply to a tie operating at $s>0$ for which, however, τ_k over a region of slips happens to be negative, as is the case at small values of s for the inductive shunt network of Figure 2(A).

Types of Applications

The Load Divider

In many practical instances the synchronous tie is required to act as a load divider or equalizer, helping the driver of the more heavily loaded station and passing a share of its burden to the driver of the less loaded station. In such cases there is often no intention or need to obtain any over-all contribution of power from the a-c line supplying the stators of the machines of the tie which is therefore designed primarily to develop adequate

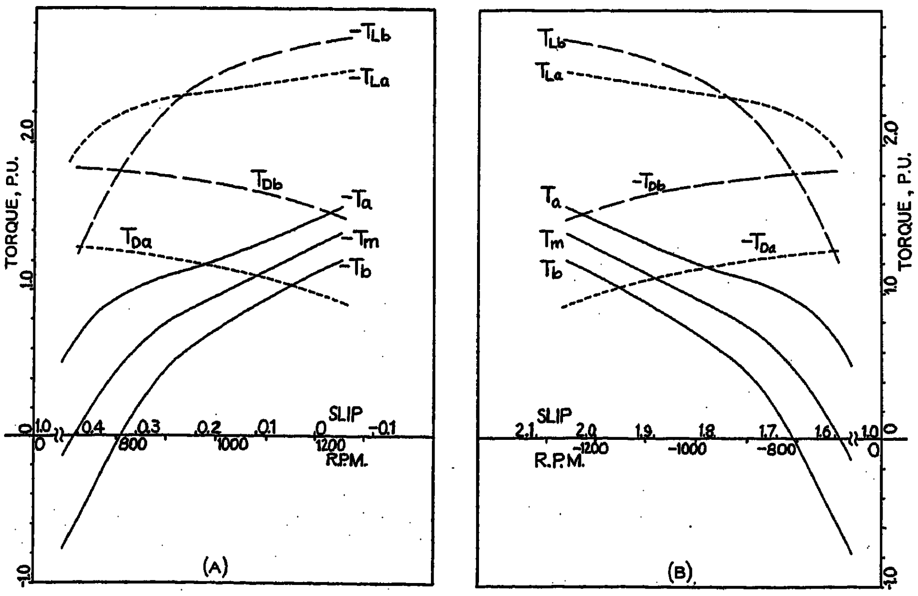


Figure 4. Examples of assigned torque-speed characteristics

A—operation with the field

B—operation against the field

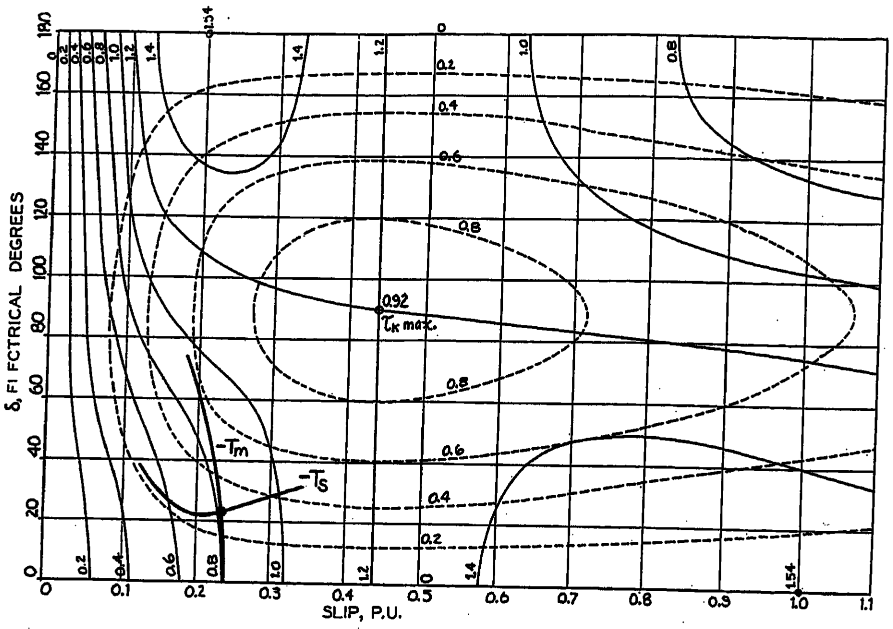


Figure 5. Chart for the determination of s and δ

synchronizing torque. Hence straight rotor connections, without external rotor networks, are commonly used. Then $\tau_0=0$, and τ_m can be assumed negligible if δ is not large. Hence in first approximation the synchronized speed of the stations is obtained directly from equation 7 as the speed for which $T_m=0$, that is, $T_a=-T_b$; thus τ_k is determined and finally δ results from equations 6 and 10.

Operation either with or against the field is practical and the latter often is preferred, in view of the larger values obtainable for τ_k, which make possible the equalization of load differences over a wider range. It also is seen that for operation against the field τ_k does not vary greatly for varying s; it is this consideration that makes it possible to some extent to elaborate on the steady-state stability of the system[3] in terms of the variable δ only.

Somewhat larger values of τ_k may be obtainable for rotation against the field if shunt capacitors are used, but tentative calculations for the machines in the example indicate then the close risk of excessive saturation.

The Follow-up Drive

In applications of this type only one station (for example, station A in Figure 3) has a driver. If the tie has no external networks, the main burden of supplying the mechanical power of both loads rests upon this driver. For operation with the field and $T_a>T_b$ the station B is retarded and M_b is the electrical leader. Operation against the field also is practical (and has the same advantage of larger τ_k mentioned in the section the Load Divider), but for the same mechanical situation, M_b is the electric lagger. Again external networks may be used if motor torques are needed to satisfy equation 9; for operation with the field then some of the burden is shifted from the driver upon the a-c line.

A simple analysis applies if the driver is provided with a governor fixing its speed at a constant value. In this case the angle δ results immediately from the equilibrium condition for station B, that is from equation 7 for operation with the field and from equation 8 for operation against the field, while the governor intervenes to satisfy the equilibrium condition of station A in any event. The angle at which pull-out occurs is then $\delta={}_{\tan}{}^{-1}(-\tau_k/\tau_\pi-\tau_0)$ for rotation with the field since this angle maximizes τ_{leador}, while it is $\delta={}_{\tan}{}^{-1}(\tau_k/\tau_\pi-\tau_0)$ for rotation against the field since this angle maximizes τ_{lagger}. Further simplifications occur if the tie has no mutuals and if the stator resistance and magnetizing current are neglected, since the problem reduces then to the analysis of the power flow between two voltage sources connected through a series impedance. The pull-out torque of station B is given then by equation 12, chapter 1, of reference 2, the leader being the sending end.

The Self-Synchronous Drive

This type of application is characterized by the absence of either driver. The induction machines are required not only to provide synchronizing torques keeping both shafts in step, but also to deliver motor torques corresponding to the total power requirements of the loads. Thus only operation with the field is intended (although reversal of speed and operation at $s>1$ may occur for excessive loads) and external rotor networks are needed. Shunt resistors are used commonly, although suggestions have been made recently[4] to substitute these resistors by reactances with the intent of reducing rotor losses; actually the question of network design is more comprehensive as it involves basic compromises between the motor and synchronizing actions obtainable. In any event for given τ_m the rotor losses are expressed by $2\tau_m s$ regardless of the type of rotor circuit used. For the example considered in Figure 2(A) it appears that inductive shunt reactances for certain sets of τ_m and τ_s values give lower s values than the shunt resistors $R_y=0.09$ of Figure 2(B) and therefore lower rotor losses, but the over-all disadvantages of smaller τ_k and the likely loss of synchronism at $s=0.1$ are evident. On the other hand capacitive reactances do not seem practical in view of the low values of rotor frequencies.

At any given speed the pull-out occurs for the value $\delta_{\max}=\tan^{-1}(\tau_k/\tau_\pi-\tau_0)$ which maximizes the smaller of the two air-gap torques, that is τ_{laggor}; some information may be gained calculating the expression $\Delta=(\tau_s/\tau_m)$ at $\delta=\delta_{\max}$, which has been called "ratio of inequality" by some authors.[5,6,7] But Δ is a function of the slip, which here is not a preassigned variable, and the immediate usefulness of this concept seems to be limited. In fact the simultaneous determination of both s and δ for the self-synchronous drive for any load situation results from the solution of equations 7 and 8 or 9 and 10 as discussed already in general.

It may be of some practical interest to evaluate the upper and lower values of T_b which limit the range of possible synchronized operation, at positive slip, for any given value of T_a. These limits are plotted, in Figure 6, which has been ob-

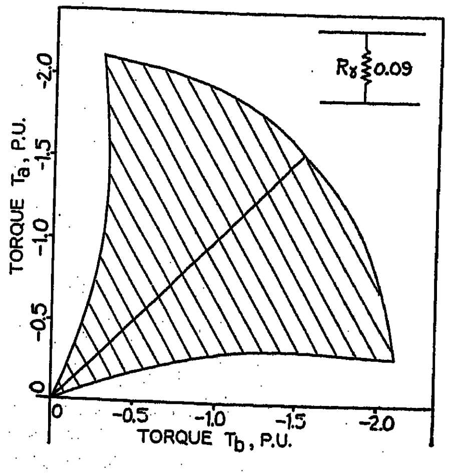


Figure 6. Limits of operation of a self-synchronous tie

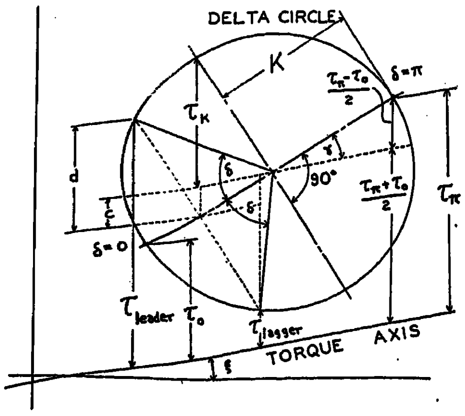


Figure 7 (left). Delta-circle and torque axis

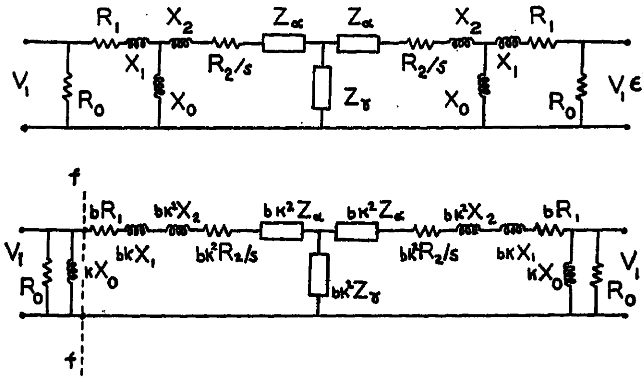


Figure 8 (right). Equivalent circuits of the synchronous tie

tained from the chart of Figure 5 by searching for the largest values of τ_s which are compatible with preassigned values of τ_m. Synchronized operation is possible for points within shaded area.

Conclusions

The torques of the induction machines of the synchronous tie have been obtained in forms which evidence the synchronizing and motor action of the drive, and expressions have been derived for the calculations of these torques at any speed and angular displacement in terms of design data.

The operation of the tie has been analyzed for general mechanical situations and methods have been suggested for the solution of the equilibrium equations of the system. Thus speed and angular displacement are obtained and currents, losses, and so forth, also can be evaluated with the procedures developed already in reference 1. Typical synchronized drives of this type have been discussed in particular.

The analysis presented is limited to the steady-state operation; the use of the derived torque expressions in differential equations describing mechanical transients may be acceptable as approximations for those transients in which speed and angular readjustments are sufficiently slow. In such cases some information concerning the influence of design of machines and rotor networks on the mechanical transient response may be gained in elementary fashion. More comprehensive analyses are available in the literature.[8,9]

Appendix

The air-gap torques of the two induction machines can be obtained graphically, for constant s and varying δ, from the delta-circle described by the vectors of the stator currents, by use of its torque axis.[1] As seen in Figure 7, simple geometrical considerations yield for these torques

$$\tau_{\text{leader}}=(\tau_\pi+\tau_0)/2-c+d$$

$$\tau_{\text{lagger}}=(\tau_\pi+\tau_0)/2-c-d$$

where

$$c=K\frac{\sin\gamma}{\cos\xi}\cos\delta,\ d=K\frac{\cos\gamma}{\cos\xi}\sin\delta$$

Also, by the law of sines

$$K\cos\gamma/\cos\xi=\tau_k,\ K\sin\gamma/\cos\xi=(\tau_\pi-\tau_0)/2$$

Hence

$$\tau_{\text{leader}}=\tau_0+(\tau_\pi-\tau_0)\sin^2\delta/2+\tau_k\sin\delta \quad (1)$$

$$\tau_{\text{lagger}}=\tau_0+(\tau_\pi-\tau_0)\sin^2\delta/2-\tau_k\sin\delta \quad (2)$$

τ_0, τ_π, and τ_k depend on the parameters of the equivalent circuit of the synchronous tie and on the slip. In order to derive usable expressions in analytical form, it has been found convenient to modify the original equivalent circuit transferring the magnetizing admittances at the stator terminals and correcting for this the remaining impedances of the circuit as indicated in the two steps of Figure 8. This transformation, which does not affect the general properties of the loci discussed in reference 1, has been suggested independently by Professor J. F. H. Douglas of Marquette University and by Professor G. F. Tracy of the University of Toronto in their unpublished lectures on induction machines. It implies the multiplication of the stator reactances by a factor $k=1+x_1/x_0$ and the rotor circuit impedances by k^2 and also the further multiplication of all impedances, with exception of those of the magnetizing branches by a factor $b=\epsilon^{j\theta}$ where $\theta=2R_1/kx_0$.

Then, by usual procedures of circuit analysis it is found, in per-units,

$$\tau_0=V_1^2(1-s_r)\frac{R^0-R_1}{(R^0)^2+(X^0)^2} \quad (11)$$

$$\tau_\pi=V_1^2(1-s_r)\frac{R^\pi-R_1}{(R^\pi)^2+(X^\pi)^2} \quad (12)$$

$$\tau_k=V_1^2(1-s_r)\left(\frac{1}{2}\frac{X^\pi}{(R^\pi)^2+(X^\pi)^2}-\right.$$
$$\frac{1}{2}\frac{X^0}{(R^0)^2+(X^0)^2}-$$
$$\left.R_1\frac{X^\pi R^0-X^0R^\pi}{[(R^0)^2+(X^0)^2][(R^\pi)^2+(X^\pi)^2]}\right) \quad (13)$$

Here V_1 is the per-unit stator phase voltage and s_r is the slip corresponding to the rated name plate speed of the induction machines, the factor $(1-s_r)$ appearing throughout in view of the choice of unit torque made in the text. R^0 and X^0 and respectively R^π and X^π are the resistances and the reactances of the input impedance at the terminals ff of the modified equivalent circuit for operation at respectively $\delta=0$ and $\delta=\pi$, having set $b=1$. For instance if, for the sake of generality, it is assumed that the external T rotor network of the original equivalent circuit consists of series impedances $Z_\alpha=R_\alpha/s+jX_{\alpha L}-jX_{\alpha c}/s^2$ and of a shunt impedance $Z_\gamma=R_\gamma/s+jX_{\gamma L}-jX_{\gamma c}/s^2$ (where $X_{\alpha L}$ and $X_{\gamma L}$ are inductive reactances and $X_{\alpha c}$ and $X_{\gamma c}$ are capacitive reactances, all at line frequency and reduced to primary terms) it is

$$R^0=R_1+k^2(R_2+R_\alpha+2R_\gamma)/s,$$

$$X^0=kX_1+k^2(X_2+X_{\alpha L}-X_{\alpha c}/s^2+2X_{\gamma L}-$$
$$2X_{\gamma c}/s^2)$$

$$R^\pi=R_1+k^2(R_2+R_\alpha)/s$$

$$X^\pi=kX_1+k^2(X_2+X_{\alpha L}-X_{\alpha c}/s^2)$$

as seen also applying the modification discussed above to the circuits of Figures 3(B) and 3(A) of reference 1. In practical cases the external rotor networks are of a less general nature and the equations 11, 12, and 13 are easier to calculate. Considerable simplifications are obtained if the machines of the tie are such as to make acceptable the simplifying assumptions $R_1=0$ and $X_0=\infty$. For instance, for the comparatively simple, but practically very important, case of a synchronous tie whose external rotor network consists of shunt resistors only, it is then

$$\tau_0=V_1^2(1-s_r)\frac{(R_2+2R_\gamma)/s}{[(R_2+2R_\gamma)/s]^2+(X_1+X_2)^2}$$

$$\tau_\pi=V_1^2(1-s_r)\frac{R_2/s}{(R_2/s)^2+(X_1+X_2)^2}$$

$$\tau_k=V_1^2(1-s_r)\frac{X_1+X_2}{2}\times$$
$$\left(\frac{1}{(R_2/s)^2+(X_1+X_2)^2}-\right.$$
$$\left.\frac{1}{[(R_2+2R\ \ )/s]^2+(X_1+X_2)^2}\right)$$

which simplifies even further, obviously, in the case of straight rotor connections ($R_\gamma=\infty$).

References

1. The Geometric Loci of the Synchronous Tie, L. A. Finzi, L. C. Wellard. *AIEE Transactions*, volume 68, part I, 1949, pages 168–171.

2. Power System Stability (book), S. B. Crary John Wiley and Sons, Inc., New York, N. Y., volume 1, 1945, chapter 1.

3. Limits of Stability of Synchronized Drives with Induction Machines, E. Scharstein. *Archiv Fuer Elektrotechnik* (Berlin, Germany), volume 28, 1934, page 770 (in German).

4. Induction Motors for Synchronized Drives, E. L. Schwarz-Kast. *Electrical Engineering*, volume 68, October 1949, page 856.

5. Synchronized Operation of Polyphase Induction Machines, L. Dreyfus. *Elektrotechnik und Maschinenbau*, (Vienna, Austria) volume 48, 1930, page 1007 (in German).

6. Synchronized Operation of Induction Machines, W. Schuisky. *Archiv Fuer Elektrotechnik* (Berlin, Germany), volume 28, 1934, page 601 (in German).

7. Operation of Induction Motors in Synchronism, T. F. Wall. *Engineering* (London, England), volume 164, 1947, page 579.

8. An Analysis of the Induction Machine, H. C. Stanley. *AIEE Transactions*, volume 57, 1938, pages 751–57.

9. Damping and Synchronizing Torques on Power Selsyns, C. Concordia, Gabriel Kron. *AIEE Transactions*, volume 64, 1945, pages 366–71.

No Discussion

Co-ordination of Fuel Cost and Transmission Loss by Use of the Network Analyzer to Determine Plant Loading Schedules

E. E. GEORGE
FELLOW AIEE

H. W. PAGE
NONMEMBER AIEE

J. B. WARD
MEMBER AIEE

DETERMINATION of the most economical division of load between generating plants is of considerable importance on large interconnected systems, as a small percentage gain in plant or transmission efficiency will result in savings of considerable magnitude. The theory and practical application of incremental heat rates have been known and practiced for years in the preparation of loading schedules,[1] but such schedules have usually been based on principal consideration of total and incremental fuel costs.

Generally, little or no attempt has been made to allocate generation to reduce transmission losses, except in isolated cases where the losses were obviously very great and the fuel penalty relatively light. Where transmission losses have been considered, it has usually been on basis of a simple tie connection between two plants; where more complex arrangements were studied, trial and error calculations were so involved and time-consuming that satisfactory analysis was difficult.

No practical method of determining transmission network losses for routine operating purposes was known to the authors until the loss formula method was developed by one of them in 1942.[2] This method was greatly simplified in 1945 by J. B. Ward who developed a procedure for using the network analyzer to replace the tedious trial and error determinations of power flows from each generating plant and interconnection previously needed in setting up the loss coefficients.

The transmission loss formula was combined with total and incremental fuel costs by two of the authors in 1946 in the preparation of loading schedules for a large system. The method involved a large amount of trial and error calculations, but its possibilities were apparent. As a result, work was started to determine whether or not the network analyzer could be used to solve simultaneously the variables of incremental fuel cost and incremental transmission loss. Results were successful and were applied in the preparation of loading schedules for a large system.

This new time saving method forms the basis of this paper, which has for its purpose the description of how total and incremental transmission losses for a given system may be determined and combined with the incremental heat rates of the several plants of the system and a loading schedule determined that will give minimum delivered cost.

While applied here to a more complicated case, it is evident that the network analyzer can be used to determine proper loading of units, plants, and systems, considering incremental fuel costs alone or in conjunction with transmission losses. It opens up new possibilities for quick determination of proper loading, and the time may come when a simplified network analyzer will be an essential part of the load dispatcher's equipment.

Description and Application of Method

Following is a brief description of the method as applied to a system having eight interconnected plants, the plants having various economies and fuel costs.

To convert the system data into a form suitable for the solution of loading problems on the network analyzer (or by any other means), it is necessary to have a formula for total system transmission loss in terms of megawatt generation at each plant and a formula or curve for each plant showing total fuel cost at that plant in terms of its generation (preferably net). The total transmission loss formula is best determined on the network analyzer, using the method outlined in Appendix II. The formulas or curves of total fuel cost at each plant may be obtained by conventional methods.[3]

The first step is to convert the formula for total transmission loss into a set of equations for incremental transmission loss. This is done by taking the first partial derivative with respect to each plant in turn, using methods of calculus. Table I shows the total transmission loss formula for the system. Table II shows equations of incremental transmission loss for the system.

It will be noted that the incremental-loss equations are symmetrical about the diagonal (from upper left to lower right). This symmetry is essential for electrical solution.

The next step is to convert the formulas or curves of total fuel cost into incremental-cost equations, such as those shown in Table IV. There will be at least one incremental-cost equation for each plant. Plants with more than one unit will have

---

Paper 49-242, recommended by the AIEE System Engineering Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 18, 1949; made available for printing September 12, 1949.

E. E. George is an Electrical Engineer and H. W. Page is a Mechanical Engineer with Ebasco Services, Inc., New York, N. Y. J. B. Ward is Analyzer Engineer with Purdue University, Lafayette, Ind.

The authors wish to acknowledge the assistance of associates and others in the preparation of this paper, with especial credit to Professor L. M. Haupt, A. W. Lotz, and Professor J. R. Eaton.

Table I. Total Transmission Loss in Kilowatts = Sum of Following 36 Terms

$1.53G_a^2$	$+1.26G_b^2$	$+1.57G_c^2$	$+0.98G_d^2$	$+2.18G_e^2$	$+8.73G_f^2$
$+15.22G_g^2$	$+11.33G_h^2$	$+2.12G_aG_b$	$+1.64G_aG_c$	$+0.82G_aG_d$	$-0.78G_aG_e$
$-4.18G_aG_f$	$-5.36G_aG_g$	$-2.80G_aG_h$	$+1.93G_bG_c$	$+0.82G_bG_d$	$-0.78G_bG_e$
$-4.18G_bG_f$	$-5.36G_bG_g$	$-2.80G_bG_h$	$+0.82G_cG_d$	$-0.78G_cG_e$	$-4.18G_cG_f$
$-5.36G_cG_g$	$-2.80G_cG_h$	$+0.36G_dG_e$	$-3.04G_dG_f$	$-4.23G_dG_g$	$-1.69G_dG_h$
$-0.02G_eG_f$	$-1.20G_eG_g$	$-0.58G_eG_h$	$+16.28G_fG_g$	$-0.58G_fG_h$	$-1.69G_gG_h$

G_a, G_b, G_c, and so forth equal net generation in megawatts at plants *a*, *b*, *c*, and so forth.

Table III. Loading Ranges of Plants

Plant	Unit	Minimum and Maximum Loads, Megawatts
a	3	10 to 44
b	8	10 to 45
c	4	8 to 30
d	3	8 to 30
e	1	10 to 44
f	1 and 2	8 to 30
g	1	10 to 44
h	1	6 to 18
	Total	70 to 285

a cost equation for every unit and every combination of units. A combination of dissimilar units may involve a discontinuity where the most efficient unit becomes loaded, thus requiring intersecting or offset cost curves.

The incremental-cost equation may be derived from the total-cost curve graphically, or by arithmetic, or by calculus. When the graphic method is used, the resulting curve should be converted to an equation. A linear equation provides adequate accuracy and is much simpler to use than an equation with square terms. Approximation may be made by drawing a straight line through the points of minimum and maximum loading, and a parallel line tangent to the convex intermediate portion, and then a final line parallel and halfway between the first two. The intercept (at zero load on the plant) and the slope of this final line will determine the incremental-cost equation. Figures 4, 5, and 6 show typical incremental-cost curves, with the linear approximation.

Table III lists the practical minimum and maximum loads of the plants and units under consideration. These limits must be taken into consideration in operating the network analyzer, as explained in the section on the Use of the Network Analyzer in Solving Numerical Co-ordination Equations.

Table IV shows the incremental fuel cost equations of the plants, and units under consideration. It will be noted that, unlike the incremental-loss equations, the incremental fuel cost equations include constant terms and do not involve other plants. This latter condition means that they do not have any mutual terms. Therefore, the mutual terms in the final equations set up on the network analyzer involve only loss coefficients and do not change with availability of plant units. This greatly simplifies the solution.

The next step is to combine incremental transmission loss and incremental fuel cost, but before doing so it is necessary to reduce both of these to dollars or to kilowatts. The latter has the advantage that the incremental loss terms remain in their usual form and are of convenient magnitude for calculation. To make this conversion from dollars to kilowatts, it is necessary to use a factor. This factor β is the weighted average fuel cost of the plants under consideration. The use of this average as a constant throughout the range of loading is an approximation, which has little effect on accuracy of the results, as shown in Table IX.

Table V shows incremental fuel cost terms thus obtained from Table IV.

Table VI is the final group of equations with detached coefficients. This table is obtained from Tables II and V by combining fuel and loss coefficients, placing known terms and constants on the right, and setting variables as column headings. The two constants are combined into a single one, denoted by ϕ. For convenience, the plant symbols are shown at the left to indicate the partial derivative represented. This facilitates preparation of a wiring diagram for the equations. Plant *f* is omitted in this and further steps, because it was not needed in the range of system loads under consideration.

The wiring diagram, Figure 3, is prepared from Table VI, (using simplifications described later). The same diagram of connections is used for loading schedules involving other combinations of units. The only changes from Table VI for such other loading schedules are in the diagonal terms and in the constant terms on the right-hand side of the equality sign. These diagonal terms affect only the values of the series impedances and do not change any circuit connections. The constant terms affect only generator voltages and do not change any circuit connections.

Network analyzer readings are then obtained. Typical readings are plotted in Figure 7, which shows the proper loading for one particular combination of plants and units. Table X shows the accuracy of the results obtained with the network analyzer as compared with results obtained from algebraic calculations.

Proper loading for other combinations is determined in the same manner until all desired combinations have been investigated. Since total fuel cost data are available for all plants, it is a simple matter to prepare total cost curves for each combination of plants or units. Comparison of the total cost curves will indicate what combination of plants should be used to carry a given load, and when additional units or plants should be started or taken off the line. The total cost curves and data, such as that shown in Figure 7, are used in the preparation of the usual tabulated loading schedules.

Table II. Incremental Transmission Loss in Kilowatts Per Megawatt of Generation

With Station Below as Variable									
a	$+3.06G_a$	$+2.12G_b$	$+1.64G_c$	$+0.82G_d$	$-0.78G_e$	$-4.18G_f$	$-5.36G_g$	$-2.80G_h$	$= K_1$
b	$+2.12G_a$	$+2.52G_b$	$+1.93G_c$	$+0.82G_d$	$-0.78G_e$	$-4.18G_f$	$-5.36G_g$	$-2.80G_h$	$= K_1$
c	$+1.64G_a$	$+1.93G_b$	$+3.14G_c$	$+0.82G_d$	$-0.78G_e$	$-4.18G_f$	$-5.36G_g$	$-2.80G_h$	$= K_1$
d	$+0.82G_a$	$+0.82G_b$	$+0.82G_c$	$+1.96G_d$	$+0.36G_e$	$-3.04G_f$	$-4.23G_g$	$-1.69G_h$	$= K_1$
e	$-0.78G_a$	$-0.78G_b$	$-0.78G_c$	$+0.36G_d$	$+4.36G_e$	$-0.02G_f$	$-1.20G_g$	$-0.58G_h$	$= K_1$
f	$-4.18G_a$	$-4.18G_b$	$-4.18G_c$	$-3.04G_d$	$-0.02G_e$	$+17.46G_f$	$+16.28G_g$	$-0.58G_h$	$= K_1$
g	$-5.36G_a$	$-5.36G_b$	$-5.36G_c$	$-4.23G_d$	$-1.20G_e$	$+16.28G_f$	$+30.44G_g$	$-1.69G_h$	$= K_1$
h	$-2.80G_a$	$-2.80G_b$	$-2.80G_c$	$-1.69G_d$	$-0.58G_e$	$-0.58G_f$	$-1.69G_g$	$+22.66G_h$	$= K_1$

Note: G_a, G_b, G_c, and so forth equal net generation in megawatts at plants *a*, *b*, *c*, and so forth.

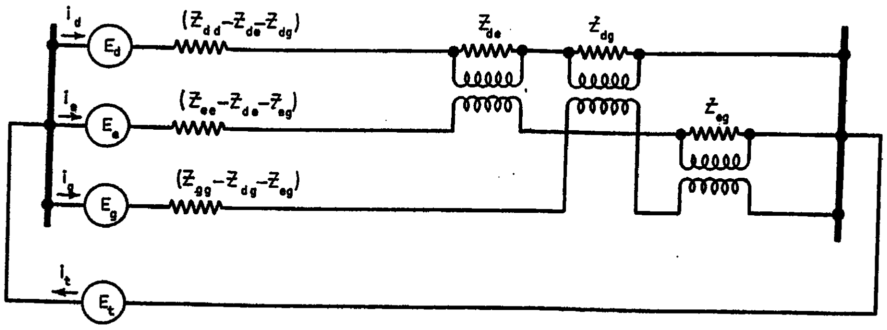


Figure 1. Electric circuit analog for 3-plant optimum loading equations. Coupling transformers are 1 to 1 ratio

Table V. Incremental Fuel Cost Terms of Plants

Plant	Unit	Variable	Constant
a	3	$4.80G_a$	.805
b	8	$3.12G_b$	.885
c	4	$12.80G_c$	.770
d	3	$12.80G_d$	.740
e	1	$3.00G_e$	.905
f	1 and 2	$27.70G_f$	.824
g	1	$2.80G_g$	.885
h	1	$11.00G_h$	.955

Note: G_a, G_b, G_c, and so forth equal net generation in megawatts at plants *a*, *b*, *c*, and so forth.

The Mathematical Derivation of the General Co-ordination Equation

MATHEMATICAL BASIS

The determination of allocation of generation between plants to give minimum total cost of delivered energy presents a problem in calculus, of the type usually discussed under the heading Determination of Maxima and Minima with Subsidiary Conditions. A very clear treatment of this subject may be found in volume II of Differential and Integral Calculus by R. Courant.[4] The desired allocation of generation between plants to give minimum total cost (with due recognition of fuel prices, heat rates, and transmission losses) is obtained when

$$\frac{\delta D_t}{\delta G_n}=\phi \tag{1}$$

where

D_t = total system fuel cost of delivered energy in dollars per hour
G_n = generation in megawatts at any plant n
ϕ = a constant, known in calculus as Lagrange's multiplier

Equation 1 expresses the fact that the total delivered cost of power will be a minimum when the incremental delivered cost is the same for every plant. This fact is known from cut-and-try determinations of total delivered cost, as well as from the principles of calculus.

The problem in hand is to break down the preceding general equation into components involving available plant and line data, and suitable for use on the network analyzer (a-c calculating board).

Because transmission losses are measured in megawatts or kilowatts and fuel costs are measured in dollars per megawatt-hour (or mills per kilowatt-hour), it is not immediately apparent how the two quantities should be combined. Even if they were in the same terms, a constant (Lagrange's multiplier) would be needed to relate them in a single general equation of minimum total delivered cost.

At the optimum allocation (minimum delivered cost), any difference in incremental fuel cost between any two given plants *a* and *b* must be offset by an equal and opposite difference in incremental transmission loss, priced appropriately in terms of incremental fuel cost.

$$\frac{\delta F_t}{\delta G_a}-\frac{\delta F_t}{\delta G_b}=\frac{\beta}{1{,}000}\left[\frac{\delta L_t}{\delta G_b}-\frac{\delta L_t}{\delta G_a}\right] \tag{2}$$

where

F_t = total system generated fuel cost in dollars per hour
L_t = total system transmission loss in kilowatts
G_a = generation in megawatts at plant a
G_b = generation in megawatts at plant b
β = weighted average system incremental fuel cost in dollars per megawatt-hour or mills per kilowatt-hour

Incremental fuel costs for plants can be calculated by conventional methods. The total system transmission loss can be expressed in terms of plant loading by means of a loss formula,[2] and from this formula the incremental loss terms appearing in equation 2 can be obtained by differentiation. The coefficients representing plant loading are in megawatts, but the formula is solved for loss in kilowatts, hence the average incremental fuel cost β must be divided by 1,000.

Equation 3 is derived from Equation 2 by the following steps:

1. Divide through by β.
2. Replace β by an estimated weighted average incremental fuel cost.
3. Multiply through by 1,000.
4. Transpose to collect *a* and *b* terms.
5. Set either half of the equation equal to ϕ instead of equal to the other half.

Because equation 2 applies to each pair of plants, the expression for any single plant may be equated to ϕ instead of equating two expressions to each other.

$$1{,}000\ \frac{\delta G_t}{\delta F_t}\ \frac{\delta F_t}{\delta G_n}+\frac{\delta L_t}{\delta G_n}=\phi \tag{3}$$

where

G_t = total system generation in megawatts
G_n = generation in megawatts at any plant n
$\frac{\delta G_t}{\delta F_t}$ = reciprocal of β
$\frac{\delta F_t}{\delta G_n}$ = incremental fuel cost in dollars per megawatt-hour at plant n with net generation G_n
$\frac{\delta L_t}{\delta G_n}$ = incremental transmission loss in kilowatts for one megawatt change in net generation at plant n

From the form of equation 3 it may be

Table IV. Incremental Fuel Costs of Plants

Plant	Unit	Incremental Fuel Costs
a	3	$4.025+0.0240\ G_a=K_2$
b	8	$4.425+0.0156\ G_b=K_2$
c	4	$3.850+0.0640\ G_c=K_2$
d	3	$3.700+0.0640\ G_d=K_2$
e	1	$4.525+0.0150\ G_e=K_2$
f	1 and 2	$4.120+0.1385\ G_f=K_2$
g	1	$4.425+0.0140\ G_g=K_2$
h	1	$4.775+0.0550\ G_h=K_2$

Note: Incremental fuel costs are in dollars per megawatt hour. G_a, G_b, G_c, and so forth equal net generation in megawatts at plants *a*, *b*, *c*, and so forth.

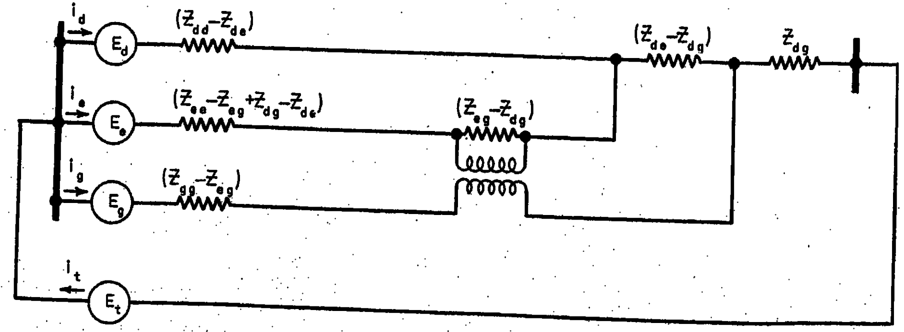


Figure 2. Modified circuit requiring fewer coupling transformers

Table VI. Final Equations for Solution on Network Analyzer

Combined Incremental Fuel Cost and Incremental Transmission Loss

	G_a	G_b	G_c	G_d	G_e	G_g	G_h	
a	+7.86	+2.12	+1.64	+0.82	−0.78	−5.36	−2.80	$=\phi-805$
b	+2.12	+5.64	+1.93	+0.82	−0.78	−5.36	−2.80	$=\phi-885$
c	+1.64	+1.93	+15.94	+0.82	−0.78	−5.36	−2.80	$=\phi-770$
d	+0.82	+0.82	+0.82	+14.76	+0.36	−4.23	−1.69	$=\phi-740$
e	−0.78	−0.78	−0.78	+0.36	+7.36	−1.20	−0.58	$=\phi-905$
g	−5.36	−5.36	−5.36	−4.23	−1.20	+33.24	−1.69	$=\phi-885$
h	−2.80	−2.80	−2.80	−1.69	−0.58	−1.69	+33.66	$=\phi-955$

Note: f row and G_f column were omitted because plant f was not used in the schedule used for illustration. See note for Table V.

seen that β and ϕ qualify as Lagrange's multipliers, and accordingly have physical meanings which become apparent at later stages in the solution. From actual solution of problems by means of equation 3, it has been found that β is equal to the weighted average incremental fuel cost. ϕ may be eliminated in either algebraic or analyzer solutions, but its value is useful in checking analyzer circuits and in verifying either algebraic or analyzer solutions. The value of ϕ is discussed in a later paragraph.

For simplification it can be assumed that the weighted average incremental fuel cost is constant. This is very nearly true, as the rate changes very slowly near the point of optimum allocation between plants. This minor approximation makes all the partial derivative equations linear, provided further that all incremental fuel cost curves can be approximated by straight lines. The problem is soluble without these simplifying assumptions, but the extra work entailed is not justified by the added precision of the results.

The same value may be used for β throughout an individual study of a particular combination of units or for all comparative combinations without serious loss of accuracy. The relatively small error introduced by making this approximation for β is shown in Table IX.

Equation 3 (applied to every plant) is the basic equation for determining the optimum allocation between plants for minimum total cost. Beginning in the next paragraph this equation is expanded into a set of simultaneous equations for a system having three plants. Tables I to VI illustrate a more general expansion for a system of eight plants.

EQUATIONS FOR A 3-PLANT SYSTEM

In order to illustrate the application of the method, assume that only plants d, e, and g are operating in the system referred to in Tables I to VI.

The loss formula will be of the form

$$L_l=G_d^2K_{dd}+G_e^2K_{ee}+G_g^2K_{gg}+G_dG_eK_{de}+G_dG_gK_{dg}+G_eG_gK_{eg} \quad (4)$$

where the K's are coefficients in kilowatts loss per megawatt per megawatt of load, and the G's are the plant loadings in megawatts (see Appendix II).

Taking the partial derivative of equation 4, first with respect to G_d, then with respect to G_e, and finally G_g

$$\frac{\delta L_t}{\delta G_d}=2K_{dd}G_d+K_{de}G_e+K_{dg}G_g \quad (5)$$

$$\frac{\delta L_t}{\delta G_e}=K_{de}G_d+2K_{ee}G_e+K_{eg}G_g \quad (6)$$

$$\frac{\delta L_t}{\delta G_g}=K_{dg}G_d+K_{eg}G_e+2K_{gg}G_g \quad (7)$$

Using approximate linear incremental fuel costs, the first term of equation 3 for each plant will be

$$\frac{1,000}{\beta}\,[I_d+S_dG_d] \text{ and } \frac{1,000}{\beta}[I_e+S_eG_e] \text{ and } \frac{1,000}{\beta}\,[I_g+S_gG_g]$$

In the above equation, I is the intercept of the linear incremental fuel cost line, in dollars per megawatt-hour, and S is the slope of the line in dollars per megawatt-hour per megawatt of load. β is the average system incremental fuel cost in dollars per megawatt-hour. Combining these fuel cost terms with the corresponding incremental transmission loss equations, as indicated in equation 3

$$\left[\frac{1,000}{\beta}\,S_d+2K_{dd}\right]G_d+K_{de}G_e+K_{dg}G_g=\phi-\frac{1,000}{\beta}I_d \quad (8)$$

$$K_{de}G_d+\left[\frac{1,000}{\beta}\,S_e+2K_{ee}\right]G_e+K_{eg}G_g=\phi-\frac{1,000}{\beta}I_e \quad (9)$$

$$K_{dg}G_d+K_{eg}G_e+\left[\frac{1,000}{\beta}\,S_g+2K_{gg}\right]G_g=\phi-\frac{1,000}{\beta}I_g \quad (10)$$

Taking numerical values from Tables I and IV and using a value for β of $5.00 per megawatt-hour, these loading equations become

$$14.76G_d+0.36G_e-\ 4.23G_g=\phi-740 \quad (8A)$$

$$0.36G_d+7.36G_e-\ 1.20G_g=\phi-905 \quad (9A)$$

$$-4.23G_d-1.20G_e+33.24G_g=\phi-885 \quad (10A)$$

Writing the coefficients in matrix form

$$\begin{array}{ccc} d & e & g \\ \hline 14.76 & 0.36 & -4.23 \\ 0.36 & 7.36 & -1.20 \\ -4.23 & -1.20 & 33.24 \end{array} \cdot \begin{array}{c} \\ G_d \\ G_e \\ G_g \end{array} = \begin{array}{c} \\ \phi-740 \\ \phi-905 \\ \phi-885 \end{array}$$

The allocation of total generation between plants d, e, and g in such a manner that these equations are satisfied yields the minimum total fuel cost. The complete set of optimum-loading equations for the system is given in Table VI.

VALUE OF TOTALIZING VOLTAGE

In equation 3 the term on the right, designated by ϕ, is a quantity the value of which is not needed in the solution of

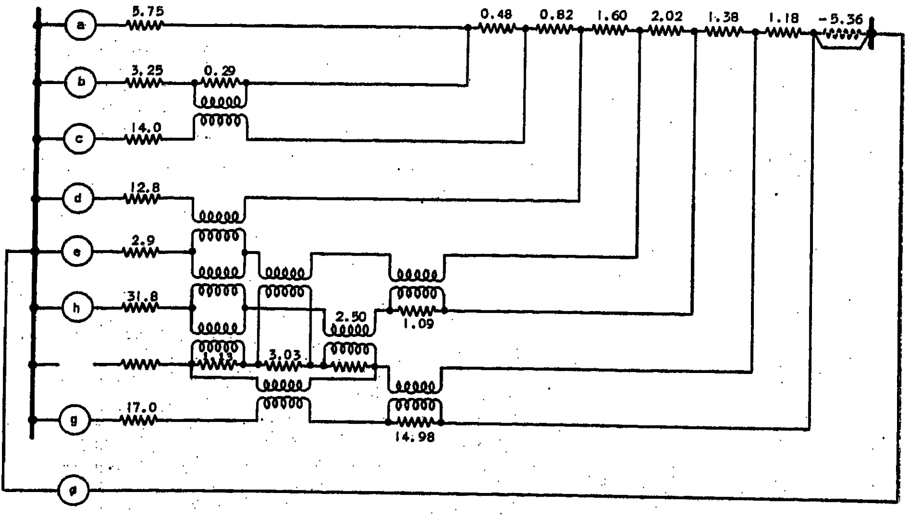


Figure 3. Circuit for equations of Table VI

Numbers correspond to actual coefficients. Analyzer resistances were set at ten times these values the equation by algebraic methods, because it can be eliminated by subtraction after equation 3 is expanded in matrix form as per Table VI.

When the matrix of Table VI is solved on the network analyzer, ϕ becomes the external voltage of the generator in the common return or totalizing circuit. This voltage is varied to get the proper current in the return circuit—corresponding to total system generation. Even in this case, calculation or measurement of the totalizing voltage is not essential to solution. In selecting a voltage and current base for the analyzer set-up, it is helpful to know the range of values of ϕ. This quantity varies with total system load, but not over a wide range. It also varies with the availability of units and with other factors controlling incremental fuel cost or incremental transmission loss. The value of ϕ may be calculated from the data for any individual plant or from the data for the entire system. In either case, the optimum allocation of generation must first be determined.

For any individual plant n, the value of ϕ may be calculated from equation 3. This amounts to substitution of the optimum loading values in any row in Table VI. Substitution in every row of Table VI should always be done for one system load as a check study. If the same value of ϕ is obtained for every row of Table VI, the calculations based thereon, the analyzer connections and the analyzer readings may be considered correct.

As it is difficult to estimate allocation accurately in advance of solution, a preliminary value of ϕ may be calculated from system averages, if needed.

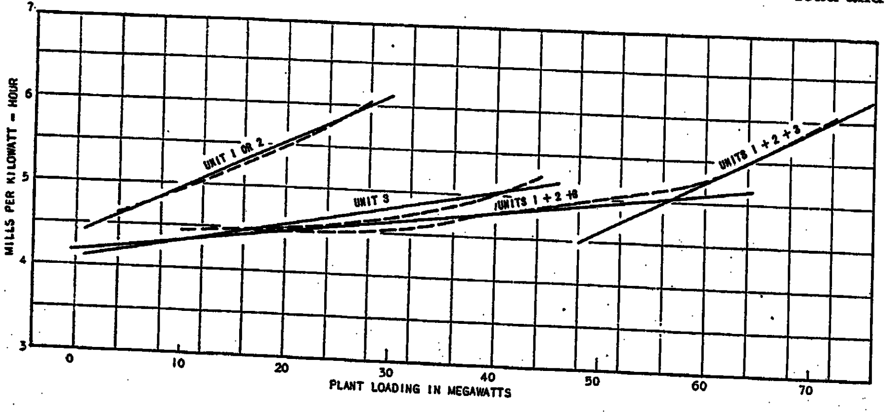
$$\phi=\left[1{,}000+\frac{\delta L_t}{\delta G_t}\right]\left[\text{actual}\frac{\delta F_t}{\delta G_t}\right]\div\left[\text{estimated}\frac{\delta F_t}{\delta G_t}\right] \qquad (11)$$

It will be noted that, except for error in estimating average incremental fuel cost, ϕ is equal to 1,000 plus the average incremental transmission loss (in kilowatts of loss per megawatt of added generation).

Table VII. Equations for Seven Plants for Algebraic Solution

	G_a	G_b	G_c	G_d	G_e	G_g	G_h	
a-b.......	+5.74....	−3.52....	−0.29....	0	0	0	0	= + 80
a-c.......	+6.22....	+0.19....	−14.30....	0	0	0	0	= − 35
a-d.......	+7.04....	+1.30....	+0.82....	−13.94....	−1.14....	−1.13....	−1.11....	= − 65
a-e.......	+8.64....	+2.90....	+2.42....	+0.46....	−8.14....	−4.16....	−2.22....	= +100
a-g.......	+13.22....	+7.48....	+7.00....	+5.05....	+0.42....	−38.60....	−1.11....	= + 80
a-h.......	+10.66....	+4.92....	+4.44....	+2.51....	−0.20....	−3.67....	−36.40....	= +150
	+1.00....	+1.00....	+1.00....	+1.00....	+1.00....	+1.00....	+1.00....	= +200

Figure 4. Incremental fuel cost—plant A



The average incremental transmission loss in kilowatts per megawatt can be shown to be 20 times the total transmission loss in per cent at the same system load. The value of ϕ usually lies between 1,025 and 1,125. It tends to increase with total system generation and to decrease with the number of plants available. It tends to increase as more and more plants reach maximum or minimum load. It also varies in opposite direction to the error in estimating average incremental fuel cost.

The physical meaning of ϕ is that it is a linear multiple of the incremental delivered cost of power from any plant, and that if it has the same value for every plant, the optimum allocation of generation has been found.

Value of Average Incremental Fuel Cost β

In equation 2 the average incremental fuel cost is designated by β. In equation 3, β is replaced by a derivative more clearly setting forth its meaning. Although β is technically a function of the solution of the equation, the allocation of generation is so little affected by the choice of the value of β that the latter is recommended to be estimated.

In general, a single value of β may be used throughout an individual study of a particular combination of units or for all comparative studies. The best single value of β is somewhere between its value when all plants are at minimum load and its value when all plants are at maximum load. These values are easily determined prior to solution of the allocation equations (Table VI). A value about halfway between minimum and maximum values is bound to be exact at some value of total generation where all (or most all) plants are "within range."

β operates on all factors on both sides of equation 3 except incremental transmission loss. Therefore, an error in estimating β produces an error in average allocation reduced in the ratio of total generation to twice total transmission loss. For instance, with a total transmission loss of five per cent, and an incremental transmission loss of ten per cent, an error of twenty per cent in estimating average incremental fuel cost would affect allocation only about two per cent.

Table IX shows a comparison of allocations, using three different estimated average incremental fuel costs: 4.64, 5.00 and 5.40 mills. It may be seen that the allocation is affected only slightly by the variation in estimated average incremental fuel costs.

Use of the Network Analyzer in Solving Numerical Co-ordination Equations

Solution by Network Analyzer

Simultaneous equations such as those given in Table VI, or equations 8(A), 9(A), and 10(A) which define the optimum loading schedule can be solved readily by establishing an electric-circuit analog; that is, by designing an electric circuit whose equations of performance are identical in form with the given equations, and reproducing this circuit on a network analyzer. In this circuit, impedances represent the coefficients of the equations, current represents megawatt loading, and voltage represents the known and undetermined constants on the right-hand side of the equations. Solution for the desired loading schedules requires the following steps.

1. Determination of a suitable equivalent circuit.

2. Choice of suitable current, impedance and voltage multipliers.

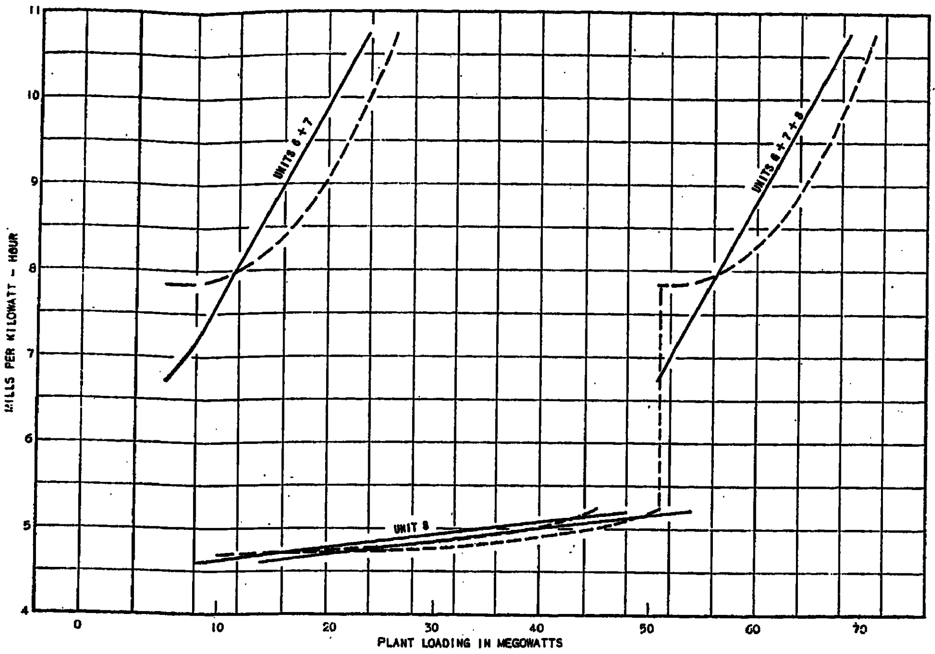


Figure 5. Incremental fuel cost—plant B

3. Adjustment of the analyzer set-up to represent a given combination of machines, fuel costs and total system generation.

4. Measurement of currents in the network and interpretation of the results in terms of plant loading schedule.

The establishment of an electric-circuit analog can be illustrated conveniently with the simple 3-plant system used previously.

Several circuits might be devised which would yield a set of equations similar in form to equations 8(A), 9(A), and 10(A), but one particular circuit which has the advantage of being easily extended to the general case of any given number of plants is shown in Figure 1. The following equations may be written describing the performance of this network, and the desired analogy established by a term-by-term comparison with the above equations.

$$Z_{dd}i_d+Z_{de}i_e+Z_{dg}i_g=E_t-E_d \qquad (12)$$

$$Z_{de}i_d+Z_{ee}i_e+Z_{eg}i_g=E_t-E_e \qquad (13)$$

$$Z_{dg}i_d+Z_{eg}i_e+Z_{gg}i_g=E_t-E_g \qquad (14)$$

Examining the requirements of the circuit which must duplicate the optimum-loading equations, currents, and voltages will be in-phase because of the scalar nature of the corresponding quantities in the loading equations. The loss formula may give rise to negative coefficients, hence the coefficients above have either positive or negative real values. Since negative coefficients appear only in the mutual terms it has been found most convenient to use resistance and to represent negative mutual terms by reversing the polarity of the corresponding coupling transformers.

An alternative circuit for the 3-plant system under discussion is shown in Figure 2. If the order of the equations and variables is chosen so that Z_{dg} in Figure 2 is the largest negative coefficient and Z_{de} is the next-largest negative coefficient (or smallest positive coefficient), the likelihood of encountering negative residual resistance in series with the voltage sources is reduced.

The resistance Z_{dg} in the modified circuit, Figure 2, need not be set up in the process of obtaining the analyzer solution. Since the total current, corresponding to total system load, flows through this branch the resulting voltage drop can be combined with the series voltage source E_t.

The circuit of Figure 1 can be extended to larger systems by providing an additional parallel path, with suitable mutual coupling to other paths, for each additional plant. Mutual coupling coefficients between one particular plant (current path) and all other plants can be obtained by conductive coupling as in Figure 2. Residual mutual coupling, either positive or negative, between other pairs of plants can be represented using resistance and 1:1 coupling transformers. Some further simplification of the network can be accomplished when several mutual coefficients are equal. As an illustration, the circuit used in the solution of the equations of Table VI is shown in Figure 3.

In more simple cases involving few equations, it may be possible to eliminate coupling transformers altogether. Equivalent circuits for parallel transmission lines presented by Starr[8] accomplish this under certain conditions. Such circuits that only involve negative resistance in series with E_t can be solved on a d-c board.

Choice of Multipliers

The quantities of interest in the equivalent circuit are current, voltage, and impedance or resistance. Three multipliers or scale factors are involved in the correlation of the information embodied in the original equations with the equivalent circuit quantities. These are:

1. Ratio of analyzer resistance in ohms to unit value of loading-equation coefficient.
2. Ratio of analyzer current in amperes or per unit to megawatt loading.
3. Ratio of analyzer voltage in volts (or per unit) to unit value of expression on the right-hand side of the loading equations.

When any two of these multipliers are selected arbitrarily, the third is uniquely determined by Ohms Law. The choice of multipliers requires consideration of the following factors:

1. Range of magnitude of coefficients in the original equations, and comparison with range of resistance values which can be set accurately on the analyzer. Consideration should be given to the magnitude of coupling transformer leakage and exciting impedance in choosing a suitable multiplier.
2. Range of variation in individual plant loading, as compared with the upper and lower limits of currents which can be read on the analyzer meters.
3. Minimum and maximum total system generation. The series source E_t must carry current corresponding to the total generation.
4. Current rating of resistance circuits. In Figure 2, for example, the branch labeled $Z_{de}-Z_{dg}$ will carry current corresponding to the sum of plant *d* and plant *e* loadings.

Table VIII. Equations for Six Plants with One Plant Base-Loaded

	G_a	G_b	G_c	G_d	G_g	G_h	
a-b	+5.74	−3.52	−0.29	0	0	0	= + 80.00
a-c	+6.22	+0.19	−14.30	0	0	0	= − 35.00
a-d	+7.04	+1.30	+0.82	−13.94	−1.13	−1.11	= − 14.84
a-g	+13.22	+7.48	+7.00	+5.05	−38.60	−1.11	= + 61.52
a-h	+10.66	+4.92	+4.44	+2.51	−3.67	−36.46	= +158.80
	+1.00	+1.00	+1.00	+1.00	+1.00	+1.00	= +256.00

Note: Plant *e* base-loaded at 44 megawatts with 300-megawatt system load. Arranged for algebraic solution.

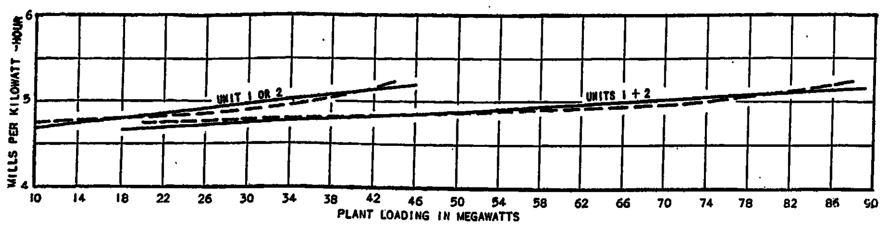


Figure 6. Incremental fuel cost—plant C

5. Voltage range required in the sources of the equivalent circuit, particularly the series source E_t. It is to be noted that the division of current among the several branches in Figure 1, that is, the desired optimum plant load division, depends primarily on voltage differences impressed across the several paths, and on the impedances of the network. Hence, it is permissible to add a fixed voltage to E_d, E_e, and E_g if the same amount is added to E_t.

The choice of multipliers will ultimately depend on the system characteristics and the type of analyzer being used. It may be necessary to use two different voltage multipliers to cover the desired range in system loading. If the current multiplier is modified inversely at the same time, it will not be necessary to reset the resistances.

The two sets of multipliers used in the solution of the sample problem, Table VI, are:

1 ampere = 100 megawatts (or 200 megawatts)
0.1 volt = unit value of RHS (or $^1/_2$ unit value of RHS)*
10 ohms = unit value of coefficient (not changed)

Adjusting, Checking, and Reading the Equivalent Circuit

After the network has been designed and the multipliers selected, the analyzer set-up can be made. It is desirable to check the set-up and this can be done easily, remembering that the impedance coefficients in equation 8, for example, represent "volts per ampere." That is, Z_{eg} is the voltage in circuit e per ampere in circuit g with all paths except g open. For a check, source E_t may be suppressed, all other sources but one opened, and unit current, or some fraction thereof, forced through the one branch or path which is energized. The voltages in all paths measured across the open source terminals will yield the coefficients in the row (or column) of the matrix corresponding to the energized source.

In adjustment of the set-up for obtaining the desired plant loading schedules, the sources are adjusted to the proper voltage differences as determined by applying the voltage multiplier to the right-hand member of the respective equations. The series voltage, E_t, is then adjusted so that the total current, I_t, corresponds to the desired total megawatt loading. Care should be taken to preserve the correct phase relationship between source voltages. Usually E_t (or ϕ) predominates and the individual branch voltages will appear in opposition or displaced 180 degrees from E_t. A plant with a linear incremental cost curve having a negative intercept may call for an individual voltage source in an aiding or additive relationship with E_t. Also, the shifting of a constant component voltage from E_t to all the individual voltages may produce such a condition.

It is desirable to adjust initially for some intermediate total system load which will not result in any plant operating at maximum or minimum load. The division of current among the several paths in the network, as read in the individual sources, gives the optimum division of loading among the plants. A new value of total system load is established by varying the source voltage, E_t, until the total current reaches the desired value.

If an individual plant reaches base load, the corresponding branch current is subsequently held constant by adjusting the individual source voltage in that branch as well as the series voltage E_t. Other voltages are held at the scheduled values until other plants reach base load.

It is not necessary to read all increments of total system load, since between points where base load is attained on some plants, plant loadings will vary linearly.

In the loading equations, the plant loadings are all positive real numbers, hence precautions should be taken in manipulation of the equivalent circuit to keep all currents, through paths corresponding to individual plants, in the same relative direction and in-phase. When one or more plants are at minimum load, the corresponding currents are small, and there is the possibility of maintaining the correct magnitude but the wrong direction (180 degrees out of phase). The sum of the individual plant loadings should be checked against the total load at each reading.

Another situation which may occur frequently in the course of running the loading schedules is the starting up of an additional unit in some plant at an intermediate point between minimum and maximum total system load. This changes the linear incremental fuel cost curve of the plant in question and requires a corresponding change in the voltage and series resistance of that path in the network. If the added unit has widely different characteristics, a discontinuity in the curve may appear in the form of a corner or offset (see Figure 5). The point of change can usually be predicted quite accurately by extrapolating the linear loading curves through points already plotted from lower values of total load. At this predicted value of total load the circuit is changed to conform to the new section of incremental fuel cost curve, and a new minimum-maximum loading bracket established for this plant.

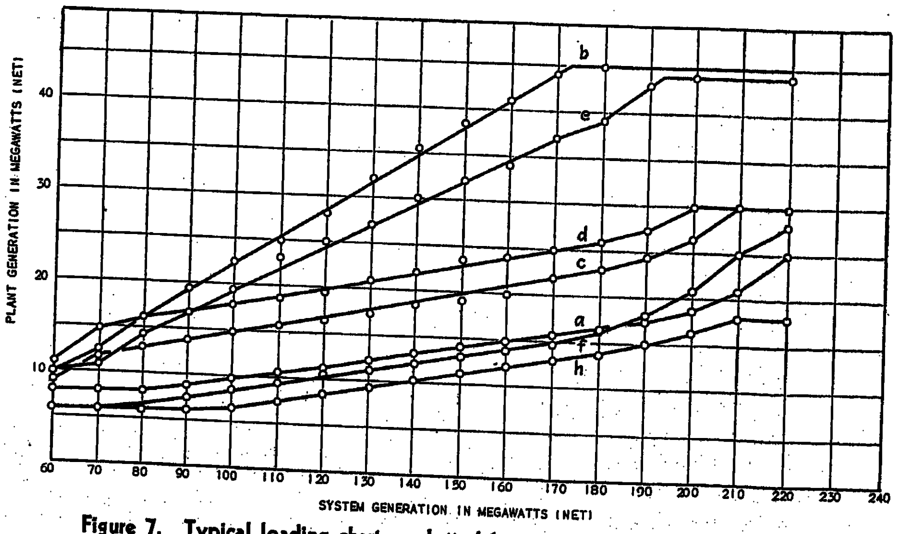


Figure 7. Typical loading chart as plotted from network analyzer readings

---

* Where RHS stands for "right-hand side" of equations 8, 9, or 10 or $[\phi - 1{,}000/\beta I]$.

Appendix I. Algebraic Solution for Optimum Loading

The solution of loading equations by any algebraic method is not recommended except for check studies or for other infrequent use. The algebraic method takes approximately ten times as long as the same work done on the network analyzer if seven plants are involved. The time required with the algebraic method is due to: base loading of some plants at maximum or minimum loads near the upper and lower portions of the schedule; time required to locate and correct the almost inevitable errors in arithmetical operations (generally procedural errors); and the absence of check results until the solution for some value of total system load is completed.

The algebraic method is practical (for occasional use) only where the following procedure is observed:

1. The simultaneous equations are solved by the method recommended in references 5 or 6—that of diagonal matrices or post-multiplication of determinants. See also references 3 and 7.

2. A motor-driven calculating machine is available.

3. All calculations (except the basic data) are carried to five significant figures (this will result in accuracy to tenth megawatts in the answer).

4. The first answer is checked by calculating ϕ in two or more equations.

5. All answers are plotted on a chart showing plant loads against total system load (this is an excellent check against errors as well as an indication of where base-loading will occur).

In the range where no plants are base-loaded the increment of load for any plant is constant for uniform changes in total system generation. This means that the plant loads are straight lines on the chart referred to in (5) above, until any plant reaches maximum or minimum load. Three or more loads are sufficient to determine (and check) the plant load lines in the linear portion.

If the solution of the equations is to be done algebraically, it is preferable to eliminate ϕ. This may be done by subtracting each of the last six equations from the first in Table VI. The necessary seventh equation is as follows

$$1.00G_a+1.00G_b+1.00G_c+1.00G_d+1.00G_e+1.00G_g+1.00G_h=G$$

Table IX. Effect of Changing Estimated Value of β on Allocation of Generation

Plant	$\beta=4.63$	$\beta=5.00$	$\beta=5.40$
a	40.1	39.9	39.8
b	40.8	40.7	40.6
c	20.2	20.4	20.5
d	23.6	23.7	23.9
e	34.8	34.7	34.6
g	26.4	26.3	26.1
h	14.1	14.3	14.5
Total	200.0	200.0	200.0

Notes: Values of β are in dollars per megawatt hour. Loading is given in megawatts.

Table VII shows the resulting equations with ϕ eliminated and the constraining equation added for 200 megawatts total system generation.

One advantage of the diagonal matrix solution is that the constants on the right hand side of the equality sign (including the total system generation) are kept separate from the variable coefficients at all times.

If the equations are arranged so that the totalizing or bottom row in Table VI becomes the right hand column in the final matrix, other values of total system load can be solved by merely changing the numerator in the final determinant by the amount of the change in the total system load. It is always desirable to start with a total system load near the middle of the schedule, so that no plant will be at maximum or minimum load.

As soon as a point in the schedule is reached where any plant will be base-loaded, it is necessary to rearrange the equations of Table VI and repeat the solution. This will be easier than before because there will be one less variable—the work varies as the square of the number of variables. The equations are rearranged by substituting in the proper column in Table VI the base load of the plant in question, moving the resulting numerical quantities to the right-hand side of the equality sign, and eliminating the row corresponding to the base-loaded plant, see Table VIII.

Appendix II. Determination of Transmission Loss Formulas

1. The following procedure for the determination of transmission losses in a power system is based upon the derivation of a loss formula by means of readings on the network analyzer and the application of this loss formula to a variety of generating conditions. Although the simplified method described here involves several approximations it is reasonably accurate for operating conditions where the normal operating angles between generator busses are not too great and where the power factor and geographic distribution of substation loads do not change too greatly. The method described is a combination of those presented in an AIEE paper by E. E. George[2] and in an unpublished paper by J. B. Ward.

2. Set up the system loads and lines for the loss condition expected to be most representative, remembering that losses vary as the square of the system load. Include station use as a load on the bus and use gross generation, or omit station use and use net generation.

3. Set up the generating plants and interconnections for number 2, giving preference to typical loading of major plants and interconnections. Small plants and minor interconnections should be netted, if practicable, as each additional source complicates the loss formula.

4. Read and record megawatts and megavars for the system of number 2 and number 3 including generators, lines, loads, capacitors, and so forth. Read and record all generator voltages and phase angles. Read vector load currents referred to some generator bus.

Table X. Accuracy of Analyzer Method

Plant	Loading in Megawatts Calculated by Algebraic Method	Loading in Megawatts Read on Network Analyzer
a	31.73	31.5
b	27.57	27.7
c	16.61	16.3
d	19.82	19.6
e	24.43	25.0
f		
g	19.70	19.5
h	10.14	10.0
Total	150.00	149.6

Notes: Accuracy of analyzer readings could be improved by pro-rating to total 150.0, preferably with help of graphic plot of adjacent readings (140 and 160 megawatts).

Plant *f* was not used in this study.

5. Supply the system of number 2 and number 3 from one of the major generating plants with all other sources disconnected. Adjust the plant output to any round value giving nearly full scale on the board ammeter, preferably 100 per cent current. Set the meter reference voltage in phase with the generator current, so that the vector reading of generator amperes has a zero reactive component. Using the vector ammeter, read and record the generator current and the current in every line. These readings should be in complex form. Watch and record signs carefully. There is less chance of error if the plugging diagram provides continuity of polarity throughout the system (See number 16).

6. Repeat number 5 for each remaining generator and each active interconnection, one at a time. This will give as many vector current flow diagrams as there are active generating plants and interconnections. Steps 2 to 6 include all the work that has to be done at the analyzer board.

7. Set up tabulation A, listing in the first column each line by the names of its two terminals, followed in the second column by the resistance of the line in per cent ohms, using any convenient base, preferably the one used in number 2. Horizontally opposite each line set down (from the flow diagrams of number 5 and number 6) per unit vector amperes with the real and reactive components in adjacent vertical columns for each of the sources. This provides a basic tabulation from which all of the loss coefficients are derived.

8. Set up tabulation B with the same two columns for line names and line resistances, followed by scalar *I*, *I* squared and *I* squared *R* for each line, using three vertical columns for each source. Total each *IIR* column, multiply by ten, and divide by the base in megavolt-amperes (used in number 7). Except for later correction for voltage and power factor, this total figure provides the *K* constant for each source.

Tabulation B takes care of the square terms in the loss formula.

9. Set up tabulation C of per unit vector products as follows: In two columns at the left set up the name of each line and its per unit resistance as in previous tabulations A and B. To the right set up three columns for each pair of sources. (This may require

more than one page). In the first of the three columns, set up the product of the two real per unit components of current for the two sources in question. In the second column, enter the product of the corresponding reactive components. These components are given in tabulation B. In the third column enter the algebraic sum of the two components multiplied by the corresponding line resistance. Total each product column, multiply by 20, and divide by the base in megavolt-amperes (used in number 7). Tabulation C takes care of the product terms in the loss formula.

10. The square term constants derived in number 8 and the product term constants derived in number 9 require correction for voltage and power factor, so that plant loadings in megawatts squared may be converted to amperes squared. In number 11 this correction is made by using individual per cent voltage and individual per cent power factor for each source.

11. For voltage correction the totals referred to above in number 8 and number 9 should be divided by the square or product of the respective per unit voltages, prevailing for the typical load flow condition as read in number 4. Divide the above totals corrected for voltages by the square or product of the respective power factors (expressed as decimals).

12. The expression for total 3-phase transmission loss in kilowatts on a system consists of addition of the above terms after multiplying each square term by the square of the megawatts loading at its source and multiplying each product term by the product of the megawatts loadings of its two sources.

13. On systems where angles between sources are excessive and likely to affect superposition in the network, it is possible to make the readings of number 5 and number 6 reflect the normal angular condition by setting up a common reference voltage for all sources, maintaining the same angles found in number 4.

14. Losses can be studied separately on two or more portions of system, if there are one or more busses between portions.

15. Excessive losses (due to a single distant source) will distort relative loads and voltages in number 5. On most transmission systems, this effect should be minimized by trimming load impedances to get the normal vector load currents of number 4. Methods have been developed recently to facilitate this operation.

References

1. Economic Loading of Steam Power Plants, and Electric Systems (book), M. J. Steinberg, J. H. Smith. McGraw-Hill Book Company, New York, N. Y., 1935.

2. Intrasystem Transmission Losses, E. E. George. *Electrical Engineering* (*AIEE Transactions*) March 1943, volume 62, pages 153–58.

3. Four Methods for the Solution of Simultaneous Linear Equations, R. H. Scanlan, M. G. Harrison. *Product Engineering* (New York, N. Y.), June 1946, pages 513–15.

4. Differential and Integral Calculus (book), R. Courant. Interscience Publishers, New York, N. Y., volume II, 1936, pages 188–211.

5. The Mathematics of Physics and Chemistry (book), H. Margenau, G. M. Murphy. D. Van Nostrand Company, New York, N. Y., 1943, pages 480–82.

6. Elementary Matrices (book), R. A. Frazer, W. J. Duncan, A. R. Collar. MacMillan Company, New York, N. Y., 1946, pages 125–33.

7. The Marginal Efficiency, A. Meldahl. *Brown Boveri Review* (Baden, Switzerland), August, September 1947, pages 180–83.

8. Equivalent Circuits, F. M. Starr. *AIEE Transactions*, volume 51, part II, 1932, pages 287–98.

Discussion

H. P. St. Clair and C. A. Imburgia (American Gas and Electric Service Corporation, New York, N. Y.): The job of determining the most economical division of load between generating plants is a complicated one and requires a great deal of hard work, not the least of which is the determination of incremental heat rates for each individual unit or type of unit and for each of the several generating plants on the system. The authors referred to the book by Steinberg and Smith[1] in which there was described a special type of slide rule for a ready determination of the most economical division of load between units on the basis of incremental fuel costs only. The authors have pointed out that until recently little attempt has been made to tie in with such determination of economical plant loading the effect of transmission losses.

On the American Gas and Electric Company system a great deal of work has been done in determining incremental heat rates, and this part of the job may be said to be very well in hand. In the application of this data to economical plant loading, we have made use of the Steinberg and Smith incremental slide rule. The question of transmission losses is obviously a very important one on the widespread transmission system which we operate, and from the beginning we have attempted to take into account transmission losses, at least in a rough way wherever they appear to be important. At the present time, however, we are in the midst of a serious study of this problem and have already made considerable progress in devising more accurate means of taking into account transmission losses as a penalty against fuel costs in various stations on the system under different station and system loading conditions. A considerable number of a-c board studies have already been made to serve as a basis for determining quantitative values of fuel cost penalties under different plant loading schedules and at different points on the system load curve.

Obviously, all of this is to a considerable extent a trial and error method. In order to take into account a large number of variations and system operating conditions, a more direct method, such as that described by the authors, if it can be worked out and applied to our problem, will be most welcome. For this reason we are glad to see the interest being taken in this problem as shown by the work of the authors and others who have contributed to this subject.

We hope that this interest will continue and that not only the authors but others will pursue such studies further.

Reference

1. See reference 1 of the paper.

M. J. Steinberg (Consolidated Edison Company of New York Inc., New York, N. Y.): This paper describes the application of a simplified network analyzer for determination of economy loading of generating stations with correction for the effect of transmission line losses. Because of the complex nature of the problem, solution along theoretical lines has been impractical making it necessary to resort to arbitrary and approximate methods. The promise of a relatively simple solution makes desirable the careful study and analysis of the paper, and for this purpose it would be helpful to have available a single-line diagram of the system to which the procedure described in the paper was applied by the authors, together with circuit impedance values. It is hoped, therefore, that this information will be supplied with the authors' written closure. The value of the paper for analytical purposes also would be enhanced if an appendix had

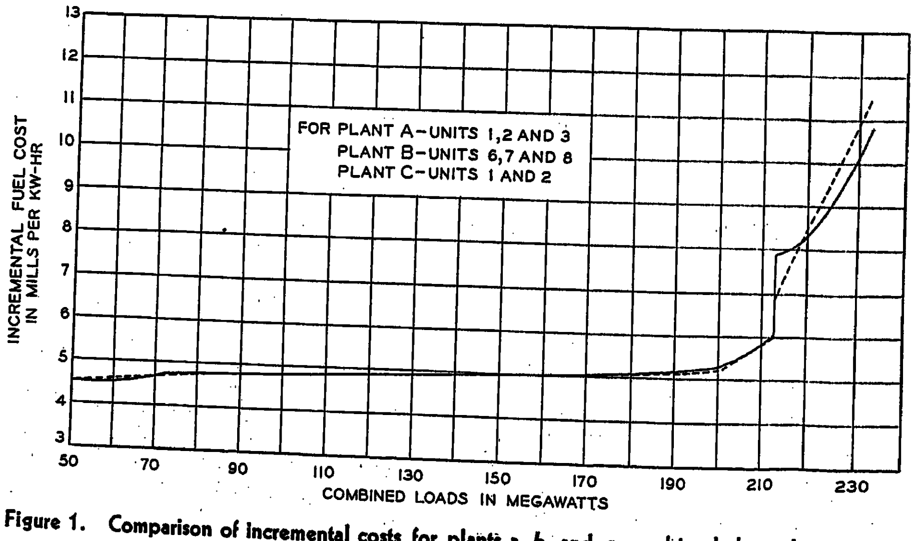


Figure 1. Comparison of incremental costs for plants a, b, and c combined, by using exponential and linear incremental cost curves for individual plants

been included showing the calculation of the K terms which are indicated as the coefficients of the G terms in Table I of the paper, so as to make it unnecessary to refer to other papers for this information.

The writer is impressed by the simplifying assumption which replaces exponential-type incremental cost curves with linear curves, because of its possible application to effect considerable reduction in the computations required to establish performance curves for the various generating stations of the writer's system. To ascertain the variation which might be expected, incremental cost curves were computed for plants a, b, and c combined using both types of cost curves. The results are shown in Figure 1 of this discussion. The variation in the range of load from a minimum value to the value corresponding to the discontinuity in the cost curve for plant b is so small as to be negligible. For combined loads above 213 megawatts the variation shown could be reduced considerably by a more judicious location of the linear curve approximating the performance of plant b for loads above 51 megawatts. It would appear from a preliminary study that the use of linear relations between incremental fuel costs and load will not introduce appreciable error.

Another simplification is introduced by the use of an average value for β. As indicated in the second paragraph following equation 8, this value is a weighted average and, as subsequently stated in the paper, lies about halfway between minimum and maximum values corresponding respectively to the minimum and maximum generated loads when all plants are in service. A sample calculation illustrating the method of weighting employed would be most welcome as clarifying information.

The authors and those associated with them in the analytical studies described in the paper are to be congratulated for their originality of approach and for a valuable contribution to the art. It may well be, as stated in the introduction, "that a simplified network analyzer will be an essential part of the load dispatcher's equipment."

H. B. Smith (Buffalo Niagara Electric Corporation, Buffalo, N. Y.): The material presented in this paper is very interesting and represents a valuable contribution to the techniques for handling power systems.

At the AIEE Fall General Meeting, it was stated that several persons had informed the authors that they had found the paper hard to understand. This was interesting, and perhaps comforting, to me because my reactions were the same. Because of this I feel that a few comments on various parts of the paper might be helpful to some who had difficulty getting through the material:

1. The authors state that: "Table I shows the total transmission loss formula for the system." What system? Evidently it is for a system having eight sources. A 1-line diagram of this system would help.

2. Re ninth paragraph under the section on Description and Application Method: Incremental transmission loss is in kilowatts per megawatt change in generation at some particular plant. Or, for a one hour period, incremental transmission loss is in kilowatt-hours per megawatt change in generation at some particular plant.

Incremental fuel cost is in dollars per megawatt-hour. Thus the conversion to be made is from dollars to kilowatt-hours (or megawatt-hours), and not dollars to kilowatts.

Then the factor β (which might have seemed less mysterious if Greek letters had been avoided) is the average of the increment fuel costs for all generators operating in dollars per kilowatt-hour.

The reciprocal of β is the number of megawatt-hours that could be produced on the average in the rest of the system for each dollar that might be saved in fuel costs due to swapping a megawatt of load from plant a to plant b, and so forth.

3. I believe the title of Table II of the paper is more correctly Incremental Transmission Loss in Kilowatts Per Megawatt Change in Generation (For Small Changes in Generation).

Then, with station a as the variable

$$\frac{\Delta L_t}{\Delta G_n} \doteq \frac{\delta L_t}{\delta G_a} = 3.06\ G_a + 2.12\ G_b + 1.64\ G_c + \ldots\ldots 2.8\ G_h = K_1$$

All the equations in Table II ($\delta L_t/\delta G_a$, $\delta L_t/\delta G_b$, $\delta L_t/\delta G_c$, and so forth) are equal to each other and thus equal to a constant K_1 only when the plants are loaded so that total transmission loss is a minimum.

4. In equation 2, F_t is total system fuel cost of generated energy. It follows then that $\delta F_t/\delta G_a$ is the incremental fuel cost for the entire system for a change in generation at plant a.

Under equation 3, it is stated that $\delta F_t/\delta G_n$ is the incremental fuel cost at plant n for a change in generation at plant n. Why, then, should not this be $\delta F_n/\delta G_n$? The inconsistency is removed if it is pointed out that, for a change in generation at plant $a\ \delta F_t/\delta G_a = \delta F_a/\delta G_a$.

5. A check of the units used in equation 2 may be helpful

$$\left(\frac{\delta F_t}{\delta G_a} - \frac{\delta F_t}{\delta G_b}\right) = \frac{\beta}{1{,}000}\left(\frac{\delta L_t}{\delta G_b} - \frac{\delta L_t}{\delta G_a}\right) \qquad (2)$$

F_t in \$/hr
L_t in kw
G_a and G_b in mw
β in \$/mw hr (not in mills/kwhr)

Substituting in equation 2

$$\frac{\$/\text{hr}}{\text{mw}} = \frac{\$/\text{mw hr}}{1{,}000}\,\frac{\text{kw}}{\text{mw}} = \frac{\$}{\text{kwhr}}\,\frac{\text{kw}}{\text{mw}} = \frac{\$}{\text{hr mw}} = \frac{\$/\text{hr}}{\text{mw}}$$

Numerically dollars per megawatt-hour is equal to mills per kilowatt-hour. However, from the above it can be seen that β in mills per kilowatt-hour would not yield a "units" check for equation 2. Things like this, though clear to the authors, may be confusing to the reader.

6. The authors refer to E. E. George's paper Intrasystem Transmission Losses[1] for the method of determining the loss formulas.

In Appendix I of this paper, "per cent" appears three times, once in the reading and twice in the equations. This is in error and "per cent" should be struck out in these three places.

REFERENCE

1. See reference 2 of the paper.

S. B. Crary (General Electric Company, Schenectady, N. Y.): The authors have made a very important contribution which can be expected to have a significant influence on system design and operation.

As is often the case in the solution of what is considered to be an overwhelming problem, the authors have been forced to make many simplifying engineering assumptions. Herein lies an opportunity for them to materially improve the understanding of their paper. I would like to suggest that they put down in one place the development of their method step-by-step with all of the necessary simplifying assumptions clearly stated.

Readers also will be interested in their evaluation of some of these approximations. We have found the transmission loss formula to be quite accurate for a relatively simple check case. It would help materially in the understanding of the method if more details concerning the system benefits in design and operation were presented. For example, it would be desirable to know what advantages accrue from considering line losses in addition to fuel costs for an actual system.

This comparison, if possible, should be made both for a solution based on the assumptions made in the paper and for a method without the approximations. This would more clearly show the desirability of further refinements in the method.

The line loss formula used by the authors, although approximate, appears to be quite accurate. Its accuracy is questionable only at loadings that are considerably higher than normal loadings and more in the region of emergency operation.

Undoubtedly improvements in the method will develop with use. Even in its present state it appears to be a long step in the direction of improved economy and should re-emphasize in a very understandable and practical way the need for lower loss transmission systems.

C. W. Watchorn (Pennsylvania Water Power Company, Baltimore, Md.): I have only general knowledge as to the operation of a so-called a-c calculating board and consequently, I am in no position to pass expert judgment on the facility with which such computing devices may be used for handling problems of this nature. My general impression is that unless the suggested computations are greatly simplified they would be impractical to use directly for operating purposes. However, it may be possible that general calculations along the line suggested by the authors could possibly be used to great advantage as a basis for judgment in modifying the incremental rates to allow for transmission losses. In this sense, such computations could be quite useful. However, there would still be the difficulty of determining the rates to be used for interchange billing where several systems are interconnected. Another difficulty would be encountered in connection with changing conditions that would result, more generally where several systems are interconnected, when there would be tendencies, under varying and

Table I. Effect of Changing Estimated Value of β

(On Allocation of Generation)

Plant	Light Load		Medium Load		Heavy Load	
	$\beta=5.00$	$\beta=4.81$	$\beta=5.00$	$\beta=5.25$	$\beta=5.00$	$\beta=5.99$
a	22	22	38	39	71	73
b	14	16	41	42	44*	44*
c	13	13	21	21	30*	30*
d	16	16	25	24	50	48
e	18	17	43	41	44*	44*
f	8	8	14	15	30*	30*
h	7	6	16	16	18*	18*
Total	98	98	198	198	287	287

*Base loaded.

Notes: Values of β are in dollars per megawatt-hour. Loading is given in megawatts.

generally unforeseeable conditions, to interchange large blocks of power in one direction under one set of conditions and in other directions under other sets of conditions.

It is believed that the simplifying assumption used by the authors of using straight lines to represent the input-output curves may lead, in many instances, to rather erroneous results, particularly where the curvature for the actual curves is fairly large. This condition would be more generally encountered where several units in the various stations are combined and especially for the higher loads thereon, as shown by the right-hand portion of Figure 5 of the paper, unless such conditions are represented by numerous short straight lines, which may become rather cumbersome to handle.

There are several specific comments I wish to make relating to the section on The Mathematical Derivation of the General Co-ordination Equation. I believe that the significance of equation 2 would be somewhat more apparent on its face if β therein were replaced by $\delta F_t/\delta G_t$, the reciprocal of which is shown under equation 3, and with the appropriate modifications in the explanation of the symbols under equation 2. β could then be substituted in equation 3 as the reciprocal of $\delta G_t/\delta F_t$ which is shown therein, and which, of itself, has no useful significance as related to the problem at hand. This presentation would then logically result in the expression, $1000/\beta$, as shown below equation 7 and also in equations 8, 9, and 10 in the paper. It also is believed that the significance of equation 3 would be more readily appreciated if equated first to $\delta D_t/\delta G_n$ and then ϕ, or equation 1. Equation 3 would then be written as

$$\frac{1{,}000}{\beta}\cdot\frac{\delta F_t}{\delta G_n}+\frac{\delta L_t}{\delta G_n}=\frac{\delta D_t}{\delta G_n}=\phi$$

It also is believed that the statement following equation 10 should read "Taking numerical values from Tables I or II and Table IV, ..." rather than "Taking numerical values from Tables I and IV, ..."

It also is believed that it would help the reader if it were stated under Description and Application of Method as related to the description of Table I that this table shows coefficients, K_{nn} and K_{nm}, as for equation 4, and if the heading of or a footnote on Table I stated that the numerical values were the coefficients as for equation 4. It also would be helpful to state in this portion, and also on Table I that the coefficients were derived in the manner described in Appendix II. In somewhat the same way, the description of Table II, would be clearer if explained as showing the coefficients $2K_{nn}$ and K_{nm} as for equations 5, 6, and 7 and that the numerical values shown on the diagonal, from the upper right to the lower left, were the coefficients $2K_{nn}$.

It is further believed that it is possible the numerical solution may very well be carried out with greater facility by means of automatic digital calculators, such as the *IBM 602A* or *604* calculators, along the general lines described in Appendix I, rather than by means of an analogue calculator such as a network analyzer in the manner described in the paper. Such calculations could then be carried out en masse, practically on a full automatic basis, for any reasonable number of desired conditions, essentially as one group of computations. This should be a large time saver in making the necessary calculations for large systems and interconnected systems. However, this method of solution also would be impractical to use directly for operation purposes and the results again be useful only for judgment modification of the incremental rates to allow for transmission losses.

It might be misleading if it is not pointed out in the paper the approximate value of 5.00 mills per kilowatt-hour used for β therein applies satisfactorily to the example illustrated and that it might be considerably different for other situations. This caution is particularly desirable since ϕ is but slightly greater than 1,000 regardless of conditions and the reader may be left with the impression that β is similarly approximately independent of conditions.

It is suggested that all through the paper, where the expression "fuel cost" or "fuel costs" is used, including the title, that this be changed to recognize the fact that other costs than fuel are involved in the cost of production with changes in generation. The term "variable generating costs" might be appropriate for this purpose.

I have followed through the remaining portions of the paper in so far as I am able (limited as stated, to my meager knowledge of a-c calculating boards) and find everything else to appear to be all right.

E. E. George, H. W. Page and **J. B. Ward:** The authors wish to thank those who have contributed verbal or written discussions of this paper. The interest shown in the subject and the recognition of the possibilities of the described procedures are extremely gratifying.

A number of simplifying approximations

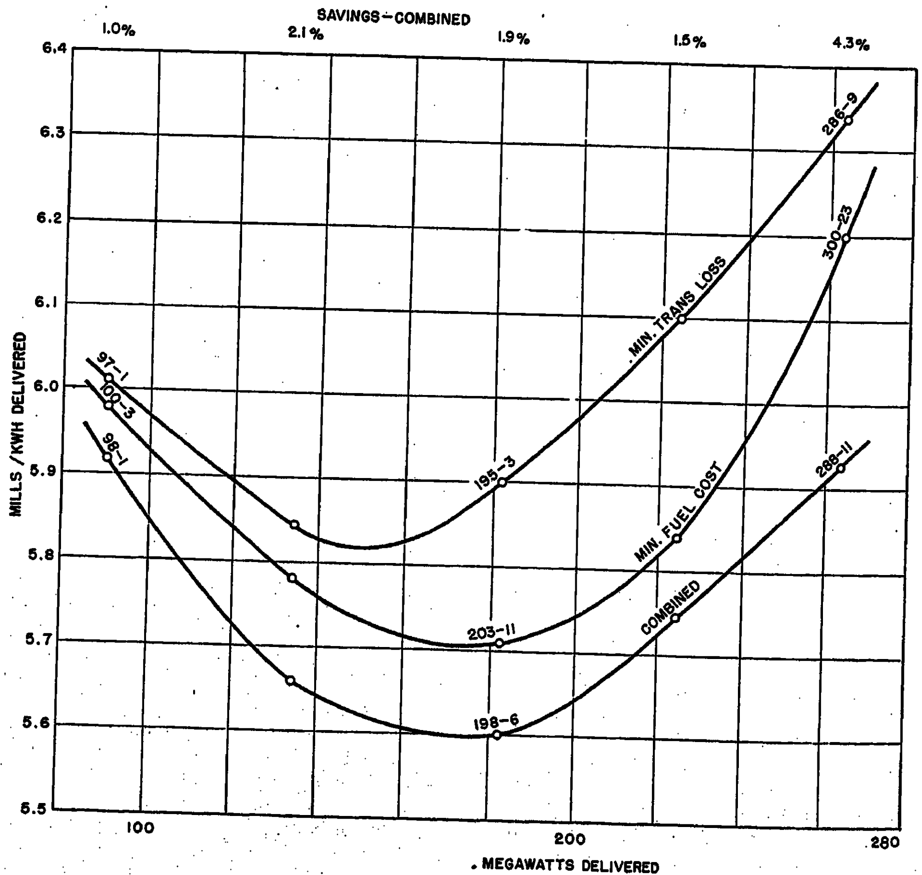


Figure 2. Relative cost of delivered energy for one combination of units

were used in the derivation of the incremental transmission loss formulas and their combination with incremental fuel costs. For the system illustrated and for which loading schedules were derived by the new method, the approximations can be considered to have little effect on the results and, in general, the discussion has borne out their acceptability. It must be recognized that electric generating systems vary in their separate descriptions and characteristics. The approximations made in the paper may have to be modified or supplemented by additional simplifying approximations in applying the method to a particular system. It is felt however, that the basic procedures can find wide application in the derivation of system loading schedules and in system planning. The extent to which the details of the method are applied will depend on the system itself and on the results that are desired.

Comments by H. B. Smith on symbols, units, and definitions are appropriate and should be helpful to many readers. F_t and F_n are synonymous because the total and the partial differentials of F are equal. This results from the fact that the incremental fuel costs of the various plants are independent of one another.

With regard to E. E. George's paper on Intrasystem Transmission Losses, the authors are unable to agree with Mr. Smith on his comment. The confusion probably arises from the fact that line flows were expressed in per cent in George's paper, whereas they are expressed in per unit in the present paper.

M. J. Steinberg suggested that the calculation of the K terms be included in the closure. This is not being done because an improved method of determining the loss coefficients has been developed. It is hoped that this new procedure will be presented to the AIEE in the near future. Steinberg also requested information on the effect of varying β. Table I of the discussion shows that this approximation does not have any great effect on the determination of proper loading providing that the value of β is properly chosen. The weighted average value of β used in the paper is an approximation obtained by taking a load for each plant, halfway between minimum and maximum capabilities, multiplying this load by the incremental fuel cost at that load, adding the products and dividing the sum by the total of the individual loads. This approximate value was found to be satisfactory for the particular system under consideration. For other systems, it may be found desirable to use more than one value of β, each value being applied for conditions approximating those for which it was developed.

A 1-line diagram of the system under consideration was requested by Steinberg and Smith. This is not included herewith for lack of space but copies are available on request to the authors.

H. P. St. Clair and C. A. Imburgia have expressed their interest in the application of the new method and their comments are appreciated. Their interest in the matter of incremental losses has already resulted in the development of a more direct method of measuring incremental loss coefficients on the network analyzer.

In answer to S. B. Crary's question regarding system benefits, it must be pointed out that savings which can be made depend on the fuel costs and other characteristics of the particular system. It is believed that Figure 2 of the discussion will serve to illustrate the savings that are being obtained on the system discussed in this paper.

The three curves shown indicate respectively delivered energy costs obtaining (1) when plants are loaded for minimum transmission loss, (2) when plants are loaded for minimum fuel cost and (3) when fuel costs and transmission losses are combined to give minimum delivered cost. The percentage figures at top of figure indicate the savings effected by considering transmission losses. The figures on the curves represent the megawatts of generation required and the megawatts of transmission loss sustained in each case to result in megawatt delivery shown on the abscissa of the chart. In this particular case, system demand can be met with 12 megawatts less generating capacity when plants are loaded according to condition 3. This serves to illustrate the use of the method in system planning. By analyzing the system on the network analyzer, the location for new generating capacity can be determined which will result in proper compromise between transmission losses and fuel costs. The lowered transmission losses can be reflected in reduced capital investment in generating capacity.

C. W. Watchorn questions use of straight lines to represent incremental cost curves. The authors suggest that wherever an extreme case of curvature is found, the curve can be approximated by two or three intersecting straight lines. Experience on the network analyzer has shown that such a combination of straight line equations is not difficult to handle. Watchorn makes some helpful suggestions relating to symbols and notation. Although the text has not been revised to incorporate these suggestions, they may improve the clarity of the mathematical derivation.

The authors have not had actual experience with automatic calculators such as the International Business Machine tabulating machine and cannot comment in detail on the suggestion to use such equipment, although inclined to doubt that there is any mechanism more suitable than the network analyzer for making plant loading schedules on the average system. This statement is influenced by the fact that practically every power system has engineers familiar with the use of the network analyzer.

As Watchorn states it should be noted that the value of β varies widely on various different systems, and might be as low as one mill or as high as ten mills. Experience in fuel cost studies leads the authors to feel that fuel costs only should be considered when dealing with incremental production costs for the purpose of making loading schedules on the network analyzer, although there is nothing in the methods recommended which would preclude the addition of other factors, if desired.

Prediction of Ultimate Temperature Rise from Early Heat Run Data

J. E. RYAN
MEMBER AIEE

Synopsis: When temperature-time data from heat runs departs markedly from a simple exponential form, existing methods of predicting the ultimate rise from early data may be inaccurate. This paper presents a graphical method for doing this which, although widely applicable, is especially useful on electromagnets. Substantial savings in testing time result from its use.

THE experimental determination of the steady-state temperature rise of electric apparatus is a tedious matter, particularly when the apparatus is large. In such cases, hours may be spent waiting for equilibrium to be reached. Obviously, great economies in testing time could be made if an accurate method for extrapolating early heat run data were available.

Several authors[1,2,3] have set forth methods for doing this when the temperature rise of the body being measured is of the form

$$\theta = A(1-\epsilon^{-\alpha t}) \tag{1}$$

throughout the heating period. However, this simple relation is followed only when the device under test acts at all times as a single body at uniform temperature throughout. Mr. V. Karapetoff[4] pointed out that the temperature rise of a particular part of a device, such as the winding, is more accurately expressed by

$$\theta = A(1-\epsilon^{-\alpha t}) \pm B(1-\epsilon^{-\beta t}) \pm C(1-\epsilon^{-\gamma t}) \pm \ldots \tag{2}$$

The number of exponential terms required depends upon the number of heat sources and heat-absorbing masses, as well as the thermal resistances connecting the latter. For a great number of devices such as tractive magnets, which contain only two major masses and heat sources, two terms are sufficient.

Cooney,[5] and later Jeffrey and Osborn,[6] took account of the double-exponential nature of temperature rise when developing their methods for predicting winding temperature as loads varied, but at least one complete heat run was required as a preliminary for their considerations.

This paper presents a method by which early heat run data on 2-mass devices can be extrapolated to find the steady-state temperature of the mass (for example, the winding) being measured.

Nomenclature

θ = temperature rise of the part of the device being measured, degrees centigrade
t = time from start of heat run, minutes
α, β, γ = reciprocals of thermal time constants, minutes^{-1}
A, B, C = steady-state temperature rise components, degrees centigrade
$Y_a = A\alpha\epsilon^{-\alpha t}$
$Y_b = B\beta\epsilon^{-\beta t}$
$Y_c = A\alpha\epsilon^{-\alpha t} + B\beta\epsilon^{-\beta t}$
$Y_d = A\alpha\epsilon^{-\alpha t} - B\beta\epsilon^{-\beta t}$

Subscripts and superscripts

1 and 2 refer to arbitrary times
a, *b*, *c*, *d* refer to corresponding curves
u refers to ultimate rise
Primes denote values at the time when ultimate rise prediction is made

Derivation

If the 2-term form of the winding temperature equation 2 be differentiated, there results

$$\frac{d\theta}{dt} = A\alpha\epsilon^{-\alpha t} \pm B\beta\epsilon^{-\beta t} \tag{3}$$

Either of the terms on the right-hand side of equation 3 will plot as a straight line on semilog paper when the co-ordinates are chosen as in Figure 1. For the first term (curve "A"), the scale value at the Y-axis intercept is $A\alpha$, and the slope is

$$-\alpha = \frac{\log_\epsilon Y_{a2} - \log_\epsilon Y_{a1}}{t_2 - t_1} \tag{4}$$

For the second term (curve "B"), the Y-axis intercept is at $B\beta$ (or $-B\beta$) and the slope is

$$-\beta = \frac{\log_\epsilon Y_{b2} - \log_\epsilon Y_{b1}}{t_2 - t_1} \tag{5}$$

In general, one of the exponentials will decay faster than the others as time proceeds, due to having a larger coefficient of t in the exponent. Therefore, let β be substantially greater than α. Values of A and B used in Figure 1 are arbitrary but typical.

Adding the ordinate scale values of curves "A" and "B," Figure 1, at each of a number of values of t and plotting these sums on the same graph, the $d\theta/dt$ versus t relation defined by equation 3 is obtained (curve "C"). The shape of this curve is typical for the coil of a d-c magnet wherein the copper loss of the coil causes a

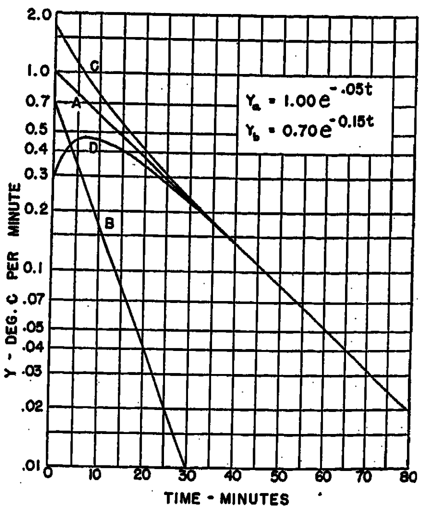


Figure 1. Typical forms of rate-of-rise curves

Paper 49-244, recommended by the AIEE Industrial Control Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted November 16, 1948; made available for printing September 2, 1949.

J. E. RYAN is with the General Electric Company, Schenectady, N. Y.

Table I. Determination of Rate of Rise for Large D-C Magnet

t	θ	$2\Delta\theta$	$2\Delta t$	$\Delta\theta/\Delta t$
0	0			
10		48.0	20	2.40
20	48.0			
30		15.5	20	0.78
40	63.5			
50		9.0	20	0.45
60	72.5			
70		6.3	20	0.31
80	78.8			
90		5.2	20	0.26
100	83.0			
110		3.0	20	0.15
120	86.0			
130		2.8	20	0.14
140	88.8			
150		2.1	20	0.10
160	90.9			

relatively rapid rise of coil temperature at first, decreasing as the heat conducts into and gradually raises the temperature of the iron.

By subtracting the curve "B" ordinates from those of curve "A" instead of adding, the second typical form of the $d\theta/dt$ versus t curve is obtained (curve "D"). This represents the condition in a large a-c magnet coil which receives heat from the iron.

Now, if the earlier parts of either curve "C" or "D," Figure 1, were given in the first place, the two component curves "A" and "B" could be found by trial, knowing that both of the latter have to be straight lines. This is particularly easy if β is much larger than α, for then curve "C" or "D" becomes a straight line relatively early in the run. In any case, however, a few trials will locate two straight lines whose Y values, when added or subtracted as the case may require, will closely approximate the corresponding $d\theta/dt$ values.

Having found these component lines, $A\alpha$, α, $B\beta$, and β may be found from the intercepts and slopes. Hence, A and B become known. Theoretically, this is all that is necessary to find θ_u since, from equation 2

$$\theta_u = A \pm B \tag{6}$$

However, it is more accurate to extrapolate from the last experimental value of θ as follows

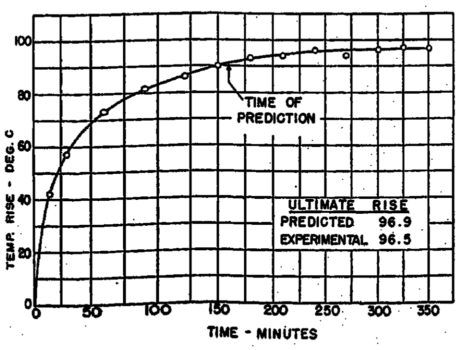


Figure 2 (left). Temperature rise of a large d-c magnet

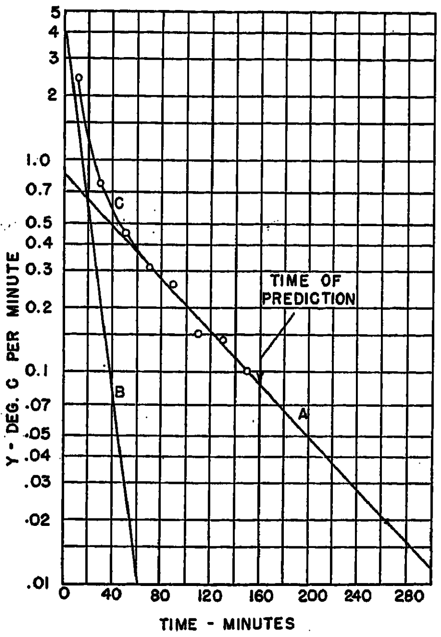


Figure 3. Rate-of-rise curves of a large d-c magnet

$$\theta_u = \theta_{t'} + \int_{t'}^{\infty} \left(\frac{d\theta}{dt}\right) dt =$$

$$\theta_{t'} + A\alpha \int_{t'}^{\infty} \epsilon^{-\alpha t} dt \pm B\beta \int_{t'}^{\infty} \epsilon^{-\beta t} dt$$

$$= \theta_{t'} + A\epsilon^{-\alpha t'} \pm B\epsilon^{-\beta t'} \tag{7}$$

Since, at time t', the ordinates of the component lines are, respectively

$$Y_a' = A\alpha\epsilon^{-\alpha t'} \tag{8}$$

$$Y_b' = B\beta\epsilon^{-\beta t'} \tag{9}$$

equation 7 may be written as

$$\theta_u = \theta_{t'} + \frac{Y_a'}{\alpha} \pm \frac{Y_b'}{\beta} \tag{10}$$

For finding from experimental data the values of $d\theta/dt$ required for the preceding analysis, the approximation

$$\frac{d\theta}{dt} \cong \frac{\Delta\theta}{\Delta t} = \frac{\theta_{t+\Delta t} - \theta_{t-\Delta t}}{2\Delta t} \tag{11}$$

Table II. Determination of Rate of Rise for Large A-C Solenoid

t	θ	$2\Delta\theta$	$2\Delta t$	$\Delta\theta/\Delta t$
0	0			
2.5		1.4	5	0.28
5	1.4			
10		3.6	10	0.36
15	5.0			
20		5.5	10	0.55
25	9.5			
30		5.9	10	0.59
35	15.4			
40		6.0	10	0.60
45	21.4			
50		5.6	10	0.56
55	27.0			
60		5.2	10	0.52
65	32.2			
70		5.6	10	0.56
75	37.8			
80		5.3	10	0.53
85	43.1			
90		5.1	10	0.51
95	48.2			
100		4.3	10	0.43
105	52.5			
110		3.7	10	0.37
115	56.2			
120		3.7	10	0.37
125	59.0			
130		2.6	10	0.26
135	62.5			
140		2.4	10	0.24
145	64.9			
150		2.1	10	0.21
155	67.0			
160		1.5	10	0.15
165	68.5			

is used, Δt being a reasonably small time interval. It is usually desirable to smooth the θ versus t data before differentiating.

Application

Figure 2 is the smoothed temperature-time curve of a large d-c magnet, energized at constant voltage. In Table I, values taken from the curve at 10-minute intervals are used in equation 11 to find $\Delta\theta/\Delta t$. The latter are then plotted as curve "C," Figure 3.

In this case, the curve becomes substantially straight as early as $t = 80$ but the time of prediction is delayed until $t = 160$ to be more certain.

By trial, lines "A" and "B" are located

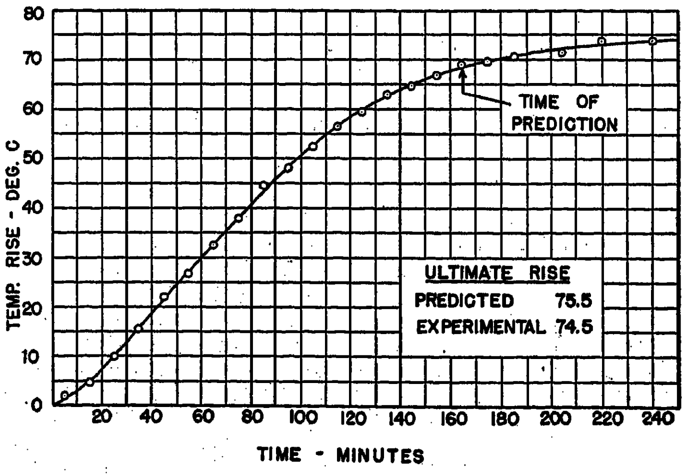


Figure 4 (right). Temperature rise of a large a-c solenoid so that their Y values add to give a close approximation to the known portion of curve "C". Using equations 4 and 5

$$\alpha = 0.0144$$
$$\beta = 0.100 \qquad (12)$$

Then, from the curves of Figures 2 and 3 at $t' = 160$

$$\theta_{t'} = 90.9$$
$$Y_a' = 0.086$$
$$Y_b' \cong 0 \qquad (13)$$

Substituting these into equation 10, the predicted ultimate temperature rise is

$$\theta_u = 90.9 + \frac{0.086}{0.0144} = 96.9 \qquad (14)$$

This is practically identical with the experimental value obtained later. Figure 4 is the smoothed temperature rise curve of a large a-c solenoid, energized at constant voltage.

Following the same procedure as before, the $\Delta\theta/\Delta t$ values for Figure 5 are obtained in Table II. When plotted, these do not indicate early convergence to a straight line so, by trial, lines "A" and "B" are located so that the difference of their ordinates gives curve "D". These yield

$$\alpha = 0.0218$$
$$\beta = 0.0274 \qquad (15)$$

Further, from Figures 4 and 5,

$$\theta_{t'} = 68.5$$
$$Y_a' = 0.220$$
$$Y_b' = 0.086 \qquad (16)$$

Substituting these into equation 10, it is predicted that

$$\theta_u = 68.5 + \frac{0.220}{0.0218} - \frac{0.086}{0.0274} = 75.5 \qquad (17)$$

If a line were drawn through the last six or seven experimental points of Figure 5, assuming incorrectly that this represents the $A\alpha\epsilon^{-\alpha t}$ asymptote and that the $B\beta\epsilon^{-\beta t}$, term has already become negligible, the prediction made on this basis is still within a degree of the correct value.

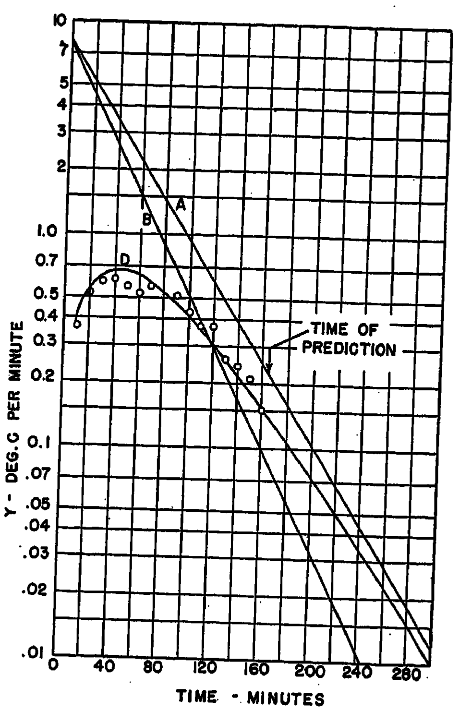


Figure 5. Rate-of-rise curves of a large a-c solenoid

Discussion

The second example above is more difficult than usual since α and β are fairly close together. It was used because it illustrates the complete method. Quite often the third term on the right side of equation 10 can be neglected when the estimate is undertaken, as was true in the first case.

Great accuracy in determining the component lines "A" and "B" of Figures 3 and 5 is not required. For example, an error of 10 per cent in slope will cause about the same per cent error in the estimate of the remaining rise, but if the rise at the estimate time is already 80 per cent of the ultimate, this will result in only $(1.0-0.80)(0.10) = 0.02$ or 2 per cent error in the final figure.

If the predicted rise is not the one desired, a readjustment of the voltage (or current) is made, causing the temperature to approach a new equilibrium. The $d\theta/dt$ versus t plot is continued without interruption. It will deviate from its original locus, but will still be of the form of equation 3; hence, the new θ_u may be estimated in the regular manner, confining the determination of component lines to the period after readjustment.

Although the procedure presented in this paper is applicable to devices of all sizes, it is most useful for large, slow heating ones where the possible time saving justifies the tabular and graphical work. This is frequently more than 50 per cent of the time otherwise required.

References

1. The Thermal Time Constants of Dynamo-Electric Machines, A. E. Kennelly. *AIEE Transactions*, volume 44, February 1925, pages 137–54.

2. Predicting Heat Rise on Electrical Machinery, A. F. Puchstein, T. C. Lloyd. *Product Engineering* (New York, N. Y.) volume 8, August 1937, pages 305–06.

3. Production Temperature Rise Tests on Transformers, H. S. Holbrook. *Journal*, Institution of Electrical Engineers (London, England) volume 88, part II, number 3, June 1941.

4. Parameters of Heating Curves of Electrical Machinery, Vladimir Karapetoff. *AIEE Transactions*, volume 45, February 1926, pages 349–54.

5. Predetermination of Self-Cooled Oil-Immersed Transformer Temperatures Before Conditions Are Constant, W. H. Cooney. *AIEE Transactions*, volume 44, May 1925, pages 611–18.

6. Interpretation of Time-Temperature Curves of Rotating Machines, F. Jeffrey, R. M. Osborn. *Allis-Chalmers Review* (Milwaukee, Wis.) volume 6, number 2, June 1941, pages 20–29.

No Discussion

Centralized Traffic Control for Railroads

W. D. HAILES
MEMBER AIEE

Synopsis: Railroads have installed many thousands of miles of a control system that retains all the time-tested safety features of railway signaling and centralizes the control of traffic on long sections of railroad. Special code type communication circuits and equipment have been perfected to furnish the remote control demanded by this system. The new communication facilities can be combined with other services on the same line wires and the range of control can be extended by the use of carrier current equipment. This centralized form of control has proved to be a dependable source of additional traffic capacity and increased earning ability.

CENTRALIZED Traffic Control (CTC) stands for an improved system of railroad operation as well as the organization of equipment and circuits by which it is accomplished.

Under this system of operation, train movements over a railroad division or major portion thereof are directed by supervised signal indications without the use of train orders or train superiority.

CTC combines an underlying railway signaling system with a supervisory communication system to concentrate the control of railroad traffic at a strategic point called the Control Office. The communication system automatically furnishes the operator at the control office with essential information concerning the constantly changing positions of the trains in his territory.

This information is displayed on the operating panel of a control machine at the control office, along with additional information about the positions of field switches and signals in the CTC territory. The communication system also provides the operator with means with which to quickly control the motorized switches and signals under his supervision. Thus with CTC, trains proceed under full protection of an approved signal system over routes that have been set up by the operator at the control office in accordance with his plan of operation for his territory. The operator is in full charge and is able to quickly change his plan of operation to meet unexpected changes in operating conditions and thus avoid long and costly delays that occur without CTC under these circumstances.

Train operation by signal indication only without the use of train orders was first instituted on the St. Louis—San Francisco Railway in 1914; however, previous to July 25, 1927, every installation of any appreciable extent, or requiring the operation of switches, necessitated operators at intermediate points. In these installations a train director coordinated the activities of operators who threw certain switches, cleared certain signals, and reported time of passage of the trains.

On July 25, 1927, the New York Central, Ohio Central Lines, completed the first installation of a new system. This installation covered 40 miles, extending from Stanley to Berwick, Ohio. It was called centralized traffic control and was the first system that made it possible for the train director to do all of the foregoing operations by himself.

Since this first installation, CTC has had a steady growth and widespread application. By 1937 there were 1,975 track miles in service and on January 1, 1948, this figure had risen to a grand total of 9,972 track miles on 65 railroads.[1]

Safety, speed, track capacity, and operating economy are so closely related and interdependent that signaling systems affect each one to some degree. Before the advent of CTC, however, a signal system was generally installed primarily for the safety it provided. CTC has changed this for, in addition to safeguarding train movements, signals with CTC provide a supervisory control over train movements that can be concentrated at a strategic point from which the traffic of a whole railroad division, or more, can be regulated and co-ordinated so as to effect large savings in time and money.

To accomplish this it was necessary to discover a way of controlling switches and signals many miles away and do it safely. It was not until the idea of decentralizing the safety features while centralizing control facilities was conceived that CTC became feasible. It was this contribution that made it possible to design a system that would conform to the "fail safe" principle and still provide supervisory control of remote switches and signals. The switches can be moved and signals cleared to give a "proceed" indication only if the local safety circuits at the remote point in the field permit them to do so. These local circuits associated with each field layout insure that no switch will move if there is a train moving across it or permit it to move if a train has been given a signal to proceed over it. Neither will a signal give a proceed indication unless the local safety circuits show that it is safe for the train to proceed. The operator at the control office might send a control to move a switch or clear a signal when it was not safe to move the switch or clear the signal; or the communication portion of the CTC system might fail to deliver the control exactly as intended because of some detailed failure along the CTC line circuit, but the switch would not move and the signal would not clear. The local safety circuits would prevent the control from being carried out and satisfy the fail-safe principle which is essential to proper signaling systems. In a device or system that is organized and built in accordance with the fail-safe principle, any failure in the system itself will automatically result in a safe condition. In a signaling system this means that the system will auto-

---

Paper 49-245, recommended by the AIEE Communications and Land Transportation Committees and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted February 21, 1949; made available for printing September 2, 1949.

W. D. HAILES is with the General Railway Signal Company, Rochester, N. Y.

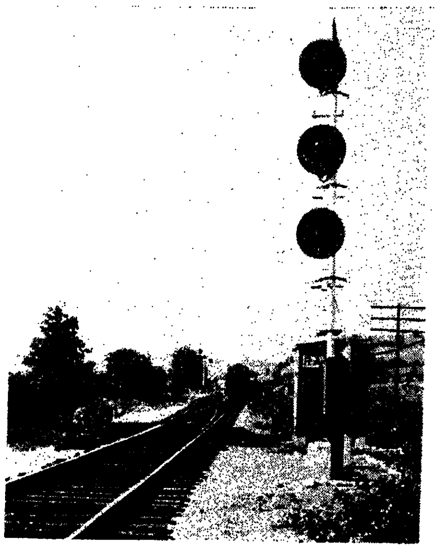


Figure 1. A typical field location

matically display or enforce a restrictive signal or restrictive control in case of a failure of the system itself.

Salient Characteristics

The principles of CTC are applicable to any track layout whether it be single-track, multiple track, or a combination thereof. In fact, a CTC installation is basically a series of layouts or plants rather widely separated from each other but co-ordinated and controlled remotely from the CTC control office. The layout at the end of a passing siding on single-track railroad is a very common layout and will be used as the typical example in explaining CTC.

A siding end has a single switch and a group of associated signals. It has a short track circuit, which includes the switch, that is known as the detector or "OS" track circuit, plus two "approach" track circuits extending in each direction away from the switch on the main track. Frequently the side track also is equipped with a track circuit. The track circuit is probably the most important single element on which modern signaling systems are built. To obtain a track circuit a section of track is insulated from the track at either end by insulated rail joints placed in each rail. A source of electric energy (direct or alternating current) is connected between the rails at one end of this insulated section of track and an electric relay is connected across the rails at the opposite end of the section. A closed circuit is thus set up over which the relay receives electric energy to pick it up only so long as there are no broken rails and the rails of the section are not shunted by the wheels and axles of a train or car. These track circuits provide the primary or basic safety features. For example, unless a track section is unoccupied and the track relay is picked up to its energized position, the signals governing train movements over that section can not display a proceed indication. In a similar manner the other necessary safeguards are derived from the local signaling circuits including the interlocking of each remote layout with the next one. This is frequently done by signal line circuits extending from one layout to the next.

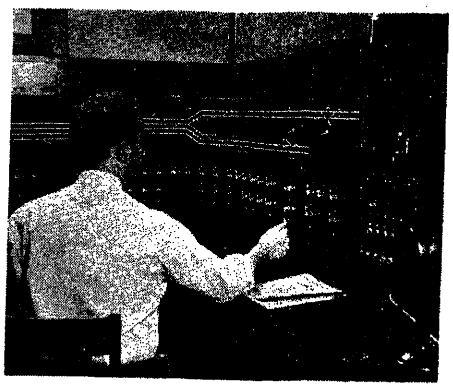


Figure 3. Portion of a control machine on the Boston and Maine Railroad with the operator sending out a control code

Figure 2. Signal relays and code equipment inside a field cabin on the Boston and Maine Railroad

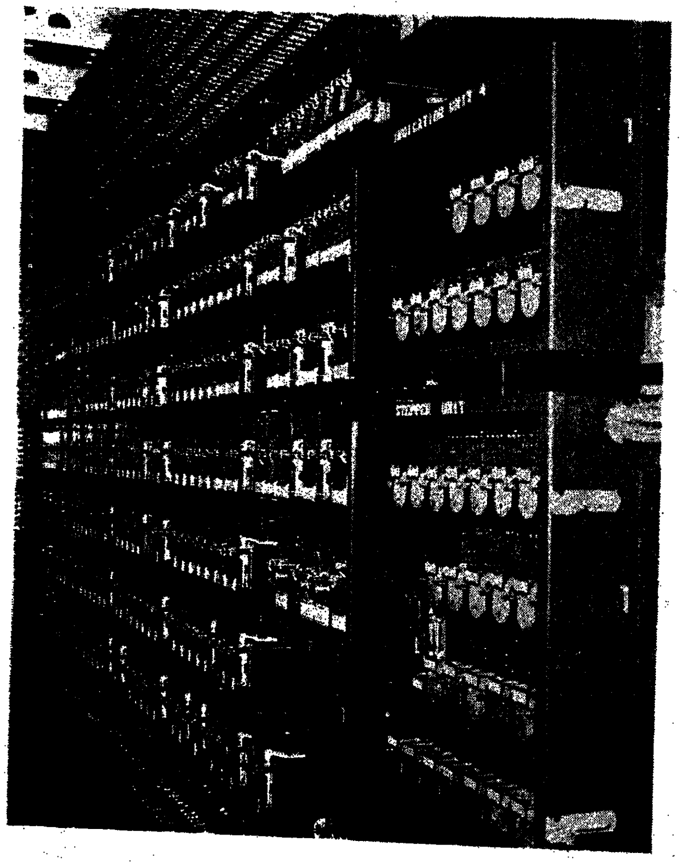
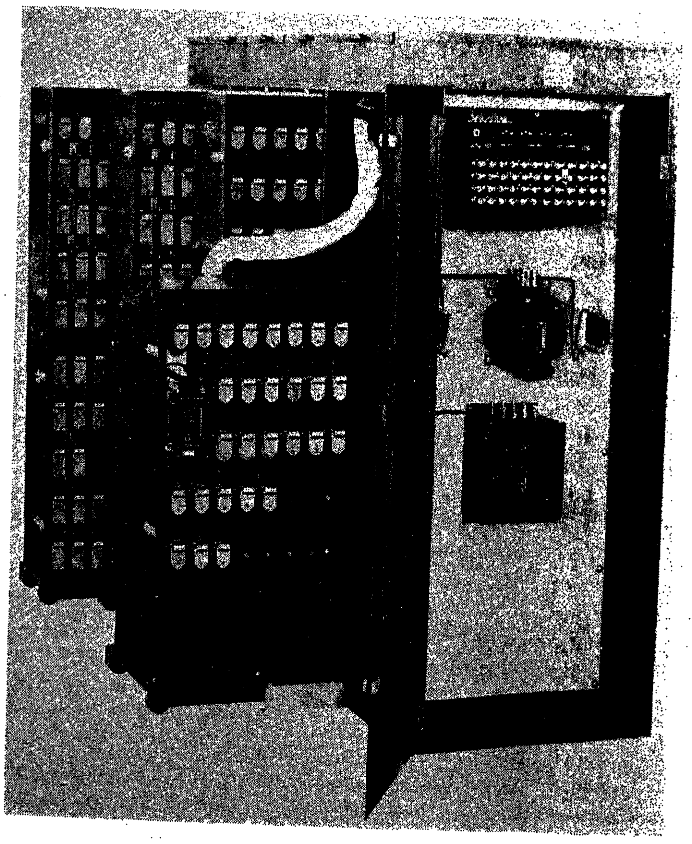


Figure 4. Rear of a control machine having a slide type of support for code relay units



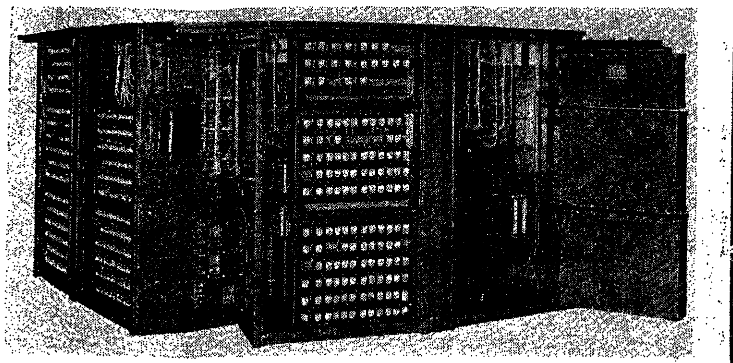


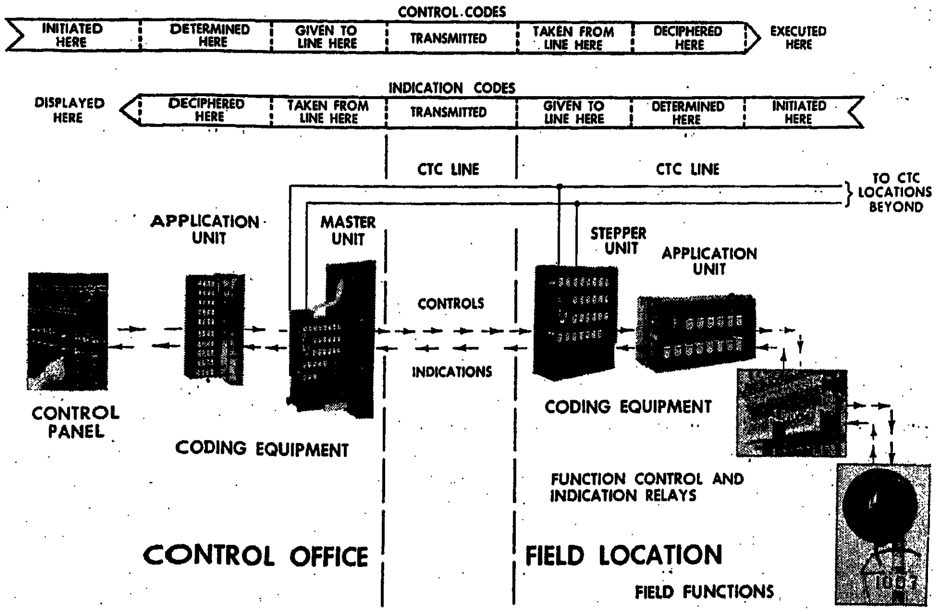
Figure 5. Rear of a control machine on the Union Pacific Railroad having a hinged type of support for code relay units

Recently there have been developed schemes by which this control can be obtained by means of special track circuits known as "coded" track circuits. These circuits can transmit "vital" controls in addition to performing their primary vital function of detecting occupancy or a broken rail.

A vital control is one on which the safe operation of trains depends and it must be given in accordance with the fail-safe principle described previously.

A coded track circuit is one in which the energy can be supplied to the track circuit in a series of pulses which on arrival at the other end of the track circuit cause the track relay to alternately pick up and release in conformity with the code pattern used in applying energy to the circuit. Either steady energy or coded energy will serve to show unoccupancy and therefore the choice of steady or coded energy can be used for a control.

Figure 6. Organization of a coded CTC system



With full CTC control the track switch and signals at a layout are motorized so that they can be operated from local energy by remote control. Also the local signal circuits automatically select the appropriate signal unit when the switch is moved from one position to the other. For example, when a switch is moved to the "reverse" position, the local signal circuits select the signal unit that governs train movements over the switch when it is set for the side track. When a switch is moved to the "normal" position, the local signal circuits select the signal unit that governs train movements over the switch when it is set for the main track. These are some of the more important and simple acts that are performed automatically by the local circuits and equipment of the signaling system to guarantee safe conduct of trains. The signal circuits and equipment, particularly relays, are also the sources of information from which the communication portion of a CTC system automatically reports the movement of trains and the positions occupied by switches and signals. It is the movement of a track relay from its picked-up to its released position that momentarily opens the starting circuit of the CTC communication system and causes it to report that a train has just entered the track section associated with the relay that released as the train shunted the rails of the section. Certain other relays are the means by which the supervisory controls are introduced into the signal system.

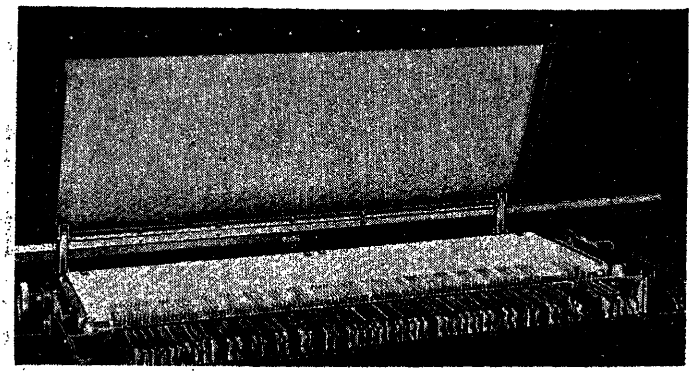


Figure 7. Automatic train recorder, with transparent cover raised, installed in desk of control machine

CTC requires a very special kind of communication system and a great deal of development has gone into the present-day circuits and equipment. The chief requisites for this communication system are as follows:

It shall automatically and quickly transmit essential information called "indications" concerning movement of trains and other information from a series of points in the field called "field locations" to the operator at the control office.

It shall quickly transmit directives called "controls" from the operator to any one of the field locations to produce the action he desires to accomplish there.

It shall perform its functions on the fewest number of line wires and at least cost for equipment and maintenance.

It shall perform with a nearly faultless reliability; for, notwithstanding the fact that vital controls are not entrusted to it, the communication system of a CTC system must be reliable if the value of CTC is to be realized.

It shall be capable of operation from local energy sources in case of failure of commercial power sources.

It should permit the superposition of other communication facilities on the CTC wires and be capable of application to existing line circuits carrying other services without causing mutual interference.

It shall be readily adaptable to remote control over a carrier current link circuit

At first a relatively large number of line wires were used to effect the control desired. Some of the early installations used one line wire from the control office to each of the outlying field locations, plus a common return wire made up in the form of a cable supported by a steel messenger wire. However, it soon became evident that the cost for so many line wires would become prohibitive, particularly since CTC was being contemplated for long stretches of territory. It thus became necessary to develop systems that required only a few wires. This meant that schemes would have to be found to use the same line wires successively for all field locations for controls as well as indications.

Two general solutions for dividing line time among the several field locations have been used. One of these solutions distributed the CTC line time by scanning all locations, one after another, every time even one indication or one control was to be transmitted. In this way a complete set of indications and controls could be sent on one trip of the scanning operation, but there is rarely, if ever, a need for this facility and it became costly in line time and scanning equipment when contemplated for large installations.

The other solution is based on a selective use of the CTC line circuit. In this scheme the line is used to serve one location at a time and only when needed to report a change at the location or to deliver a control to the location. The result of this approach to the problem is the coded CTC communication system. Practically all CTC installations today are controlled by some form of the coded CTC communication system.

The name comes from the fact that controls and indications are transmitted in the form of coded messages that can be sent over the CTC line circuit or circuits. Each code consists of a series of digits whose characteristics comprise the message. The first part of a control code contains the digits that select the particular location and station and the last part contains the digits that deliver a complete set of controls to the field station. It will be well at this point to define location and station.

A point in the field at which one or more switches and signals are grouped and handled by one field code unit is called a CTC field location. If a location is complex, there will be two or more subdivisions within the location in order to facilitate communication to and from the location. These subdivisions are called CTC stations. If a location has only one station, the terms station and location stand for the same thing and station is usually used. In fact, station is the fundamental unit and will be used hereafter.

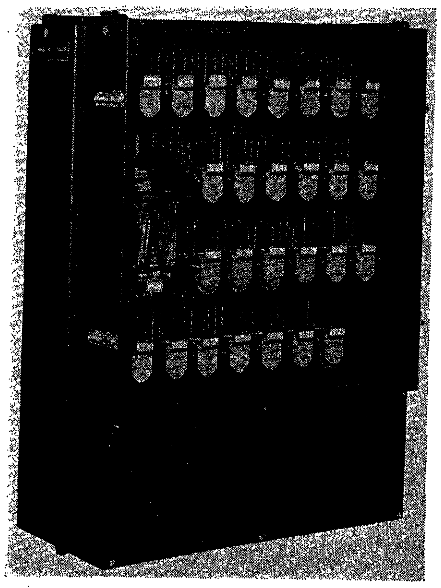


Figure 8. Field stepper unit

Similarly, the first part of an indication code contains the digits that register, in the control office, the identity of the station that is transmitting, and the second part contains the digits that completely describe the field conditions at the field station.

When a CTC code system is not required to transmit either controls or indications, it automatically goes into a period of rest and remains quiescent but sensitized to detect any change that would call for its operation. When set into operation it goes through a prescribed series of steps called a cycle. Step by step, synchronism is maintained by the CTC line circuit between the control office and field stations and each step carries a part of the coded message.

Among the coded CTC systems in operation today are: a 3-wire system using a "wire code" for controls and indications. A 3-wire system using a "polar code" for controls and "open or closed" code for indications. A 2-wire system using a polar code for controls and "time code" for indications; and a 2-wire system using time codes for both controls and indications.

In all of these the energy for the CTC line circuit is supplied from one or more line batteries at the control office.

The so-called 3-wire wire code system steps each time one of the line circuits is opened. Since there are actually two circuits using a common return, it is possible to exercise a 3-way choice on each step. If circuit *A* is broken it constitutes one choice, if circuit *B* is broken it constitutes the second choice; and if both circuits are broken it is the third choice. In this system each digit can be given one of three characteristics with which to transmit controls or indications. This system is fast acting and relatively simple, but it requires three line wires, and there are important considerations that favor a 2-wire line circuit.

The 3-wire polar code system has a control circuit and an indication circuit using a common return wire. The stepping is done by interrupting the control circuit. However, since the stepping will progress with either polarity of line current, a 2-way choice is available on each step. The control wire may be made positive or negative with respect to common and the field polar line relays will detect this difference. Thus each digit can be given one of two characteristics with which to transmit a control.

This system uses the third wire to transmit indications. During each step the intervals when the control line is de-energized and when it is energized provide two opportunities to send an indication digit. There is a 2-way choice for each digit since the indication circuit can be closed or opened during each interval to give character to each digit. This system is fast acting and relatively simple and is capable of transmitting controls and indications simultaneously. However, it has the handicap of requiring three line wires and does not lend itself readily to combination with other services on the same line wires.

The 2-wire polar code system uses the same scheme that is used in the 3-wire polar code system for transmitting controls. But in this system indications are transmitted by a time code. During an indication cycle the rate of stepping is determined by the field station that is transmitting and each step provides half-step intervals, each of which can be made relatively short or long. Hence each digit can be given one of two characteristics with which to transmit indications.

This system has the advantage of the code responsive device at each field station being a simple polar relay and it requires only two wires. However, it has coils and contacts in series with the line circuit at each location. This is objectionable when combining with other services.

In the 2-wire time code system each half step can be made relatively short or long and thus provide a 2-way choice for each digit of the code. This is true for indications as well as controls. The field line relays are polar relays, but polarity is used in this system only to enforce some common condition on all field stations and thus prevent unwanted action. The code equipment at each field station and the control office must provide means for transmitting short and long intervals and means for detecting the difference between short and long intervals of time in order to send and receive time codes. This tends to make the time code system somewhat more complex than the polar code or the wire code systems, but the advantages of this system outweigh these objections. The time code system can be operated on two line wires without the use of relay coils or contacts in the line circuit at each field station. The line circuit can be balanced to provide an acceptable circuit for voice or carrier communication and it can be more readily superposed on existing line circuits than the other types of CTC line circuits.[2,3] The first 2-wire time code system was placed in service on September 17, 1929, on the Chicago, Burlington and Quincy Railroad, and the first 2-wire time code system having field stations in multiple went into service on July 31, 1940, on the Pennsylvania Railroad.

In developing coded CTC systems it was found that there were a relatively large number of schemes that could be used to transmit controls at the will of the operator to any one of several field stations, but there are very few schemes that can automatically transmit indications from each field station to the control office. The difficulty arises from the fact that trains moving at various points along the CTC territory set up indications at random intervals, each one demanding a share of the CTC line circuit time for transmission to the control office.

It is necessary therefore to enforce some form of superiority among field stations so that they will transmit their information in an orderly fashion instead of interfering with one another with the resultant garbling or loss of indications. It also is necessary to give priority to controls so that an important directive can be sent to the field even though there are indications waiting to come in from the field.

To enforce priority among field stations, two schemes have been used in various ways. One scheme is based on the relative geographic positions of the field stations with respect to the control office where the line battery is located. The other employs a principle called "code superiority". In the geographic scheme each station is inferior to all those that are nearer the control office and superior to all those beyond it. This superiority resides in the ability of a station to deprive more distant stations of line battery energy by opening or shunting the CTC line circuit.

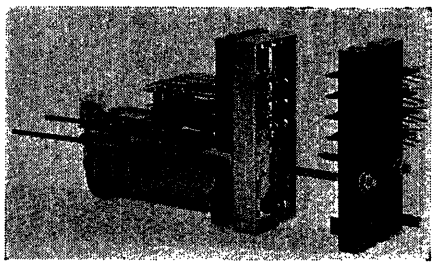


Figure 10. Typical code relay

Referring now to the code superiority method, it is possible to arrange the code responsive circuits so that a station with a particular arrangement of code digits can force other field stations to retire from the line temporarily. The various codes can be arranged in order, with respect to superiority, and they can be assigned without regard to relative geographic locations of the field stations. The advantage of this code superiority method is that any field station can be given first priority, so it could always take the line to report its indications even though there were other stations nearer the control office ready to transmit.

However, the geographic scheme is simpler and is used more widely. Furthermore, the addition of means to prevent a location from holding the line has removed most of the advantages of the code superiority scheme.

The size of CTC territory that can be handled from one control office is quite unlimited and it has probably not yet been reached, although some of the longer installations probably approach a practical limit beyond which it may not pay to extend the territory.

There are, however, rather definite limits on the size of CTC installations that can be handled by one CTC section as defined by the length of its CTC line circuit. There are two factors: line attenuation of the CTC code impulses; and capacity to transmit codes rapidly enough to keep up with field changes. The first depends on the size and kind of line wires used and the second depends on the number of field stations in the territory and the number of trains that are on the section at the same time. It is therefore difficult and rather meaningless to attempt to give a value for the maximum distance or the largest number of stations that can be controlled by one CTC section. However, there are many CTC sections 75 to 100 miles long with from 30 to 35 stations per section that are successfully handling 20 to 40 trains a day.

This brings us to a consideration of ways and means of extending CTC beyond the capacity of one CTC code section. One way is to start out from the control office with a second pair of line wires extending to a second more remote code section and thus provide a second path over which to transmit codes independently of the first section. This has been done in several installations. It is costly, however, and would probably not be used today except for special circumstances. The present method is to employ carrier currents superposed on the line wires of the first CTC code section to control a second CTC code section extending beyond the first section. This principle of extension can be used to control several additional sections by using additional carrier frequencies.

Each carrier circuit acts as a link between its CTC code section and the control office and provides a separate communication path over which information

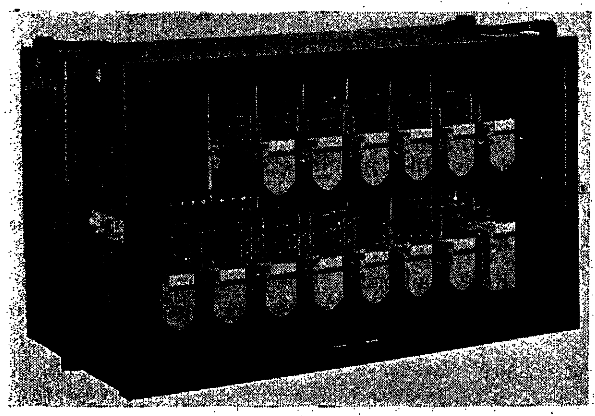


Figure 9 (left). Field application unit

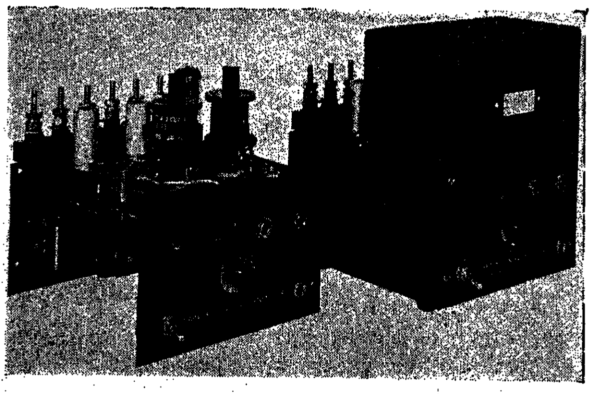


Figure 11 (right). Carrier control transmitter and receiver. Covers removed from transmitter

can be transmitted independently of the other sections. The full application of this principle of extension by carrier link entails the use of carrier repeaters to overcome the attenuation encountered in traversing long line circuits. It also requires the use of various kinds of filters to block and pass the several frequencies involved. The first carrier-controlled section of centralized traffic control extended from Delta to Redding on the Southern Pacific Railway and it was controlled from Dunsmuir, Calif. Carrier control has proved to be an important development in CTC and a great many installations have one or more carrier-controlled sections.

Equipment

The equipment of a CTC system falls into two main classifications: signal equipment; and code equipment. The principal units in the former class are the switch machine, the signal mechanism, and signal relays of many kinds. These devices have been brought to a very high degree of perfection and reliability over a period of years. They are not new, although some new features have been added since the advent of CTC. The functions of these devices have been discussed previously but a discussion of the devices themselves is beyond the scope of this paper.

The second division of CTC equipment, namely, the code equipment, is in reality a special form of communication system that has been developed to meet the needs of CTC. Figures 1 through 12 show some of the equipment that has been developed for this service.

At a typical control office the CTC communication equipment will include a control machine, a code relay unit called a master unit, and several CTC relay units called application units. These code relay units are used to transmit and receive CTC codes. Frequently the code relay units are housed in the control machine behind the control panel which carries a track diagram of the CTC territory, indication lights, control levers, and pushbuttons.

A control code is initiated on the control panel, its code characteristics are determined in the application unit and it is given to the line by the master unit. An indication code is taken from the line at the control office by the master unit, deciphered in the application unit and displayed on the control panel.[2] Figure 6 illustrates this procedure; it shows the organization of a coded CTC system.

The control machine is often equipped with auxiliary controls, such as an employee call. A train recorder is often mounted in the desk of the control machine. This device automatically records the arrival and departure times of trains as they pass field locations in the CTC territory. The record is made on a slowly moving roll of paper by pens that are actuated by certain indication codes. These records show the delays that each train may have encountered in its run through the CTC territory and where they occurred. The record is taken from the CTC machine each day and it is studied by the railroad operating department in order to discover how to improve the operation under CTC.

At a typical field location the CTC communication equipment will consist of a code relay unit called a stepper unit and one or more CTC relay units called application units. This apparatus, plus the signal relays, local batteries, and chargers, is usually housed in a small steel cabin. The cabin is frequently equipped with a telephone booth for use by the train crews in talking with the control office as well as for the use of the maintenance forces. This booth is arranged so that the telephone can be used without going into the main portion of the cabin where the signal and code equipment is located.

An indication code is initiated by the operation of a field function relay, its code characteristics are determined in the field application unit and it is given to the line by the stepper unit. A control is taken from the line at a field location by the stepper unit, deciphered in the field application unit and delivered to the field functions that are to be controlled,[2] as shown in Figure 6.

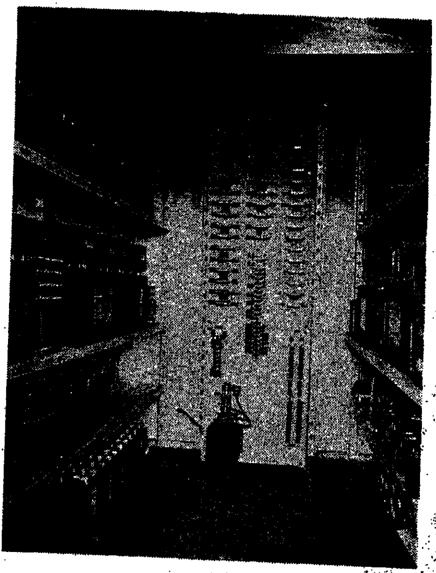


Figure 12. Interior of a carrier repeater station on the Union Pacific Railroad

The relays used in the CTC coding equipment are the result of intensive development and long experience. They are designed to operate for years and for millions of operations without the need for servicing or overhaul. Special care in design and manufacture is responsible for their very high degree of reliability. Contacts are carefully made, cleaned and adjusted, and protected from dust and dirt by enclosing the relays in cabinets. Arc suppressors are used to protect nearly all contacts except those that operate only occasionally. Arc suppressors also are required to prevent CTC code systems from interfering with radio reception. Many of the relays employed in CTC are equipped with quick-detachable connections. This feature also is used on many small relay units to assure easy and quick replacement for inspection and to clear trouble promptly.

Several pieces of equipment for combining CTC with other services have been developed. These include chokes, blocking condensers, matching transformers, and a group of filters. Low pass, high pass, band pass, and band elimination filters have been employed and their use is increasing as CTC is extended to longer distances and more complex line circuits.

The basic equipment used in carrier control consists of a carrier transmitter, a carrier receiver, and a code converter unit. The transmitter and receiver are electronic units; the code converter is a code relay unit, resembling a field stepper, in which a code arriving by carrier is converted into a CTC line code and vice versa. The transmitters and receivers can be obtained for operation directly from the 24-volt CTC battery or from the 110-volt 60-cycle power supply. In one design, the transmitter and receiver are built around the *26A6* and *26A7GT* tubes which can be operated directly from the local 24-volt CTC battery without the use of any other power supply. The transmitter and receiver are designed for operation on frequencies ranging from approximately 10 to 30 kc, but it is anticipated that frequencies above and below these values will be used in the future.

The transmitter has an output level of 18 decibels above one milliwatt (+18dbm), 63.1 milliwatts, which is the highest power level that is permitted under the recommendations of the Communications Section of the Association of American Railroads. This power level was selected several years ago by mutual agreement among the various companies operating carrier telephone and telegraph circuits. It is considered to be the highest level that can be used and still permit interference between circuits to be reduced to acceptable limits by practicable measures. It is equivalent to 6.15 volts when applied to a 600-ohm open type line. The transmitter is equipped with facilities for measuring the current in each tube and to control the output.

The receiver is capable of operation at 12 decibels below one milliwatt (−12 dbm) which is equivalent to 0.19 volt across a 600-ohm open type line. It has an output of 250 milliwatts and is equipped with facilities with which to measure the current in each tube and to control the gain.

Benefits derived from CTC

Railroads benefit by the use of CTC[4] because it:

1. Saves train hours.
2. Reduces overtime hours.
3. Increases availability and productivity of locomotives.
4. Expedites movement of freight traffic, thereby increasing the utilization of freight cars.
5. Eliminates train order or block offices and makes these employees available for other important work.
6. Increases safety of operations.
7. Provides broken rail protection where automatic block signals are not now in use.
8. Often permits increased train tonnage.
9. Eliminates train stops, thereby saving fuel, and reduces the possibilities of break-in-two's.
10. Assists in obtaining minimum unit cost of operation.

CTC is the only satisfactory substitute for additional trackage when the capacity of existing facilities has become saturated, and it may be provided for a fraction of the investment cost for an additional main track. Conversely, it also represents a satisfactory means of eliminating multiple tracks with the attendant savings in maintenance expenditures.

Conclusion

CTC has shown itself to be a dependable source of additional traffic capacity and earning ability.[5] CTC with cab signals extends the system all the way from the operator at the control office to the engineman riding in his cab. It is expected that its use will continue to grow. It is believed that new uses will be developed for CTC and that the system will be modified to suit the new uses. Development work is being carried out on faster communication systems in anticipation of some of the new uses that are expected.

References

1. Tabulation of Statistics. Interstate Commerce Commission (Washington, D. C.) January 1, 1948.

2. Centralized Traffic Control, Type K Class M Coded System. *Handbook Number 30*, General Railway Signal Company (Rochester, N. Y.) 1948.

3. Centralized Traffic Control Manuals Number 506A and Number 507. Union Switch and Signal Company (Swissvale, Pa.) 1947.

4. Operating Advantages and Possible Modifications in Rules in CTC Territory. *Report of Committee Number 2*, American Association of Railroad Superintendents (Chicago, Ill.) page 21, 1948.

5. American Railway Signaling Principles and Practices. American Association of Railroads (Washington, D. C.) 1946, chapter III, pages 54–66.

No Discussion

Attenuation and Delay Equalizers for Coaxial Lines

W. R. LUNDRY
NONMEMBER AIEE

Synopsis: This paper describes the theoretical and practical aspects of a series of attenuation and delay equalizers developed for the *L-1* coaxial system. Attention is focused on the adjustable attenuation equalizers and the delay equalizers. An indication is given of the precision required in manufacture in order to meet the close tolerances of television or long telephone circuits.

THE general problem of equalization is discussed in a companion paper.[1] The fixed or basic equalizers used at auxiliary and main repeaters are of conventional types and will not be discussed here. Instead attention will be directed to the equalizers located at main stations. These comprise the delay equalizers used on television circuits only and the adjustable attenuation equalizers, usually designated the *A* and *B* equalizers used on both telephone and television circuits.

Adjustable Attenuation Equalizers

The adjustable loss characteristics obtained in the *A* and *B* equalizers are shown by the curves of Figure 4. These curves show the maximum adjustment range above and below a reference loss which is approximately 40 decibels in each equalizer. Pairs of curves are identified by a number corresponding to the line-up frequency in kilocycles. The loss can be adjusted continuously or in small steps to fall on curves intermediate to those shown. All intermediate curves are proportional, to a high degree of approximation, to the limiting cases shown.

THEORY OF DESIGN

The *A* and *B* equalizers are complex assemblies of adjustable networks which may be derived from a simple prototype developed by Bode.[2]

In its simplest form, this structure may be represented and used as shown in Figure 1. Here, a 2-terminal network is used to adjust the transmission of a circuit containing a sending impedance R_s and a receiving impedance R_r. The structure and behavior of this 2-terminal network are our immediate concern. It consists of a 4-terminal network, *N*, associated with two resistances R_1 and R_a.

The 4-terminal network *N* is one having constant resistance image impedances at the pairs of terminals connected to the resistances R_1 and R_a, and an image transfer constant, φ_a. If the image impedances are equal and have the value R_o, then it is apparent that when $R_a=R_o$ the insertion loss of the complete structure is independent of frequency. Let the insertion loss of the structure be called θ and the value assumed when $R_a=R_o$ be θ_o. Then there is a value of R_1 such that

$$\tanh\frac{1}{2}(\theta-\theta_o)=\frac{K}{2}\rho_a\epsilon^{-2\varphi_a} \qquad (1)$$

where

$$\rho_a=-\frac{R_o-R_a}{R_o+R_a},\ K=2\ \tanh\frac{\theta_o}{2}$$

$$\theta-\theta_o=2\ \tanh^{-1}\frac{K}{2}\rho_a\epsilon^{-2\varphi_a}=K\rho_a\epsilon^{-2\varphi_a}+\frac{1}{12}(K\rho_a\epsilon^{-2\varphi_a})^3+\frac{1}{80}(K\rho_a\epsilon^{-2\varphi_a})^5+\ldots \qquad (2)$$

In most practical cases the first term of this series gives an adequate representation of the performance. The design problem then becomes one of choosing a structure for *N* whose image transfer constant, φ_a, will give the desired value of $\theta-\theta_o$ as a function of frequency when substituted in equation 2. Since we are interested only in the loss characteristics of the structure we may write

$$\alpha-\alpha_o\cong Re[K\rho_a\epsilon^{-2\varphi_a}] \qquad (3)$$

This structure can be designed to give any of the adjustable loss characteristics required in the *L-1* system. However, another structure giving identical characteristics has some practical advantages.

Consider a network *N* of Figure 1 in which the first section is a bridged-T loss equalizer as shown in Figure 2(A), with $R_b=R_c=R_o$ and $Z_1Z_2=R_o^2$. Now φ_a as used in equation 3 is given by

$$\epsilon^{\varphi_a}=\left(1+\frac{Z_1}{R_o}\right)\epsilon^{\varphi_a'} \qquad (4)$$

The section having an image transfer constant, φ_a' has been called a "building out" section.

Suppose the network of Figure 2(A) is redrawn as in Figure 2(B). Then when $R_a=R_c=R_o$ the structure may be used as an adjustable network with R_b as the control resistance. Moreover, the circuit may be opened at the left of R_b and a building-out section having image impedance R_o may be inserted. If this section has an image transfer constant φ_b' then θ_b, obtained from

$$\epsilon^{\varphi_b}=\left(1+\frac{Z_2}{R_0}\right)\epsilon^{\varphi_b'} \qquad (5)$$

may be used instead of φ_a in equation 3 to give the performance of the structure.

This structure with the added building-out section becomes particularly interesting when we use both R_a and R_b as adjustable controls. It can be shown that

$$\theta-\theta_0=2\ \tanh^{-1}\frac{K}{2}(\rho_a\epsilon^{-2\varphi_a}+\rho_b\epsilon^{-2\varphi_b})$$

$$\equiv 2\ \tanh^{-1}\frac{\varphi_{ab}}{2}$$

$$=\varphi_{ab}+\frac{1}{12}(\varphi_{ab})^3+\frac{1}{80}(\varphi_{ab})^5+\ldots \qquad (6)$$

Paper 49-246, recommended by the AIEE Communication Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–22, 1949. Manuscript submitted July 18, 1949; made available for printing September 2, 1949.

W. R. LUNDRY is with the Bell Telephone Laboratories, Inc., Murray Hill, N. J.

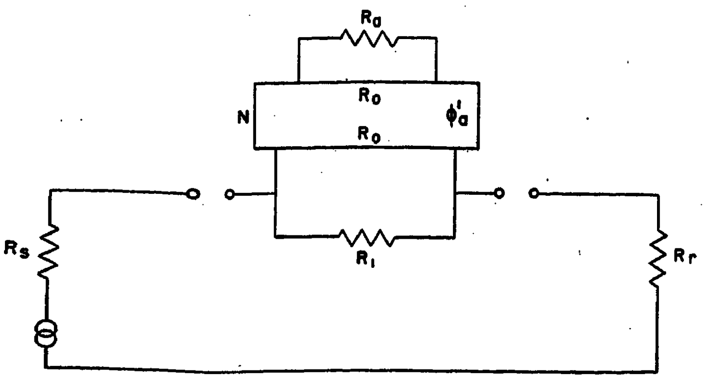


Figure 1. Adjustable network—prototype

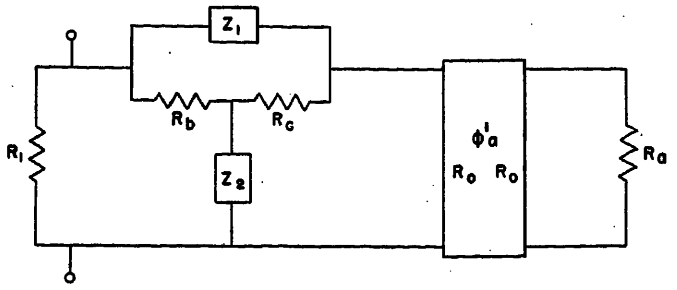


Figure 2(A) (top right). Prototype network with building-out section

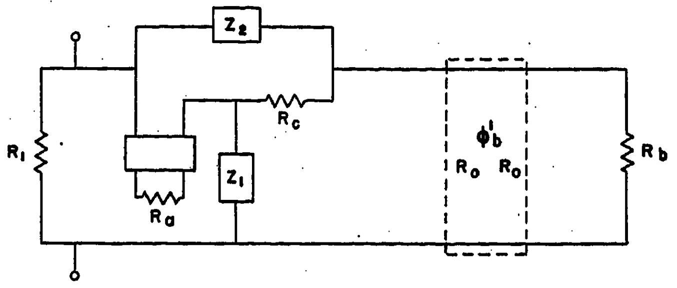


Figure 2(B) (right). Inverted prototype with building-out sections

Here again the first term of the series is an adequate approximation, so that

$$\alpha - \alpha_0 \cong R_e\varphi_{ab} = Re[K(\rho_a\epsilon^{-2\varphi_a} + \rho_b\epsilon^{-2\varphi_b})] \quad (7)$$

This would make it appear that adjustment of the control resistances R_a and R_b, which vary the values of ρ_a and ρ_b respectively between -1 and $+1$, would produce independent changes of the insertion loss, α. This, of course, is not strictly true in view of equation 6. Moreover, φ_a and φ_b are not completely independent as will be evident from a study of equations 4 and 5. Since $Z_1Z_2 = R_o^2$, small values of Z_1 give large values of Z_2. At frequencies where ρ_a must produce large changes in loss then ρ_b can at best produce only small changes. Practically, this leads to designs in which ρ_a and ρ_b are used to control the loss in distinct parts of the frequency range.

Both the A and B equalizers consist principally of combinations of 2-terminal impedances similar to Figure 3 or its inverse. The use of the network of Figure 3 as the series arm and of its inverse as the shunt arm of a constant-resistance bridged-T equalizer is a fairly obvious application. By using rheostats in which both R_a and its inverse are ganged to a single shaft and similarly for R_b and its inverse, a 4-terminal network is obtained which gives two adjustable loss characteristics while always presenting a constant-resistance impedance to the through circuit. Any number of such networks may be connected in tandem with complete independence of the adjustable controls in successive 4-terminal networks. One such section is indicated in Figure 5, the second section from the left. Sections of this type may be used when it is necessary to provide independent controls working in overlapping frequency ranges.

When practical considerations prevent the use of this type of section the configuration shown by the third section from the left in Figure 5 may be used. While this does not achieve complete independence of the adjustable characteristics, careful design will make the loss changes produced by one of the adjustable resistances tolerably independent of the values at which the other resistances may be setting. The theory of this configuration is quite involved and beyond the scope of this paper.

Another structure which has proved particularly useful is one which uses networks of the type shown by Figure 3 as series arms and its inverse as the shunt arms of a ladder network as shown by Figure 6. To understand the operation of this network, consider again Figure 3. When φ_a' has a large real component, that is, a high attenuation, the impedance z_a is very nearly R_o for all values of R_a. Similarly, Z_b equals, very nearly, R_o for all values of R_b. But, this is the condition for the impedance of the complete 2-terminal structure to be equal to R_o. (This condition also is achieved if the real parts of φ_a and φ_b are large, regardless of the values of φ_a' and φ_b'.) Thus, when R_a and R_b are required to produce loss changes over small portions of the frequency band there is left a large part of the band over which the impedance of the 2-terminal network is essentially a pure resistance. By taking advantage of this fact and giving careful attention to the frequency behavior of adjacent series and shunt branches a configuration may be found in which the loss variation produced by any one control is substantially independent of the settings of all other controls.

Design and Performance of the A Equalizer

The configurations described above and exhibited as block schematics in Figure 5 form the backbone of the A equalizer. There are other features which deserve more than passing mention. Loss adjustments which are required in large steps are obtained by switching in whole equalizer sections, by changing the element values of the individual arms of such section or by modifying the configuration of the arms by adding additional groups of elements. Switching of this type is accomplished by means of standard telephone switchboard keys.

Three of the loss adjustments are frequently made by means of pilot frequency regulators. In order to implement this, thermistors are used as the control element (resistance R_a or R_b of Figure 3). By controlling a small amount of power to a heater winding in the thermistor, its temperature may be changed to give a large change in the resistance of the thermistor element. This in turn controls the loss. For locations where pilot regulation is not used, provision is made to supply regulated direct-current to the thermistor heaters through manually adjusted potentiometers. In such cases it is necessary to protect the thermistors against ambient temperature changes. This is accomplished by placing the thermistors in an oven having a thermostatically controlled heater element operating from 110-volt a-c power.

In addition to its adjustable sections, the A equalizer also contains a series of fixed loss sections which serve to equalize the accumulated fixed errors of 11 auxiliary repeater sections. These are simple configurations of familiar equalizer types. In some cases they are associated with adjustable sections in order to reduce the over-all A equalizer loss to tolerable values.

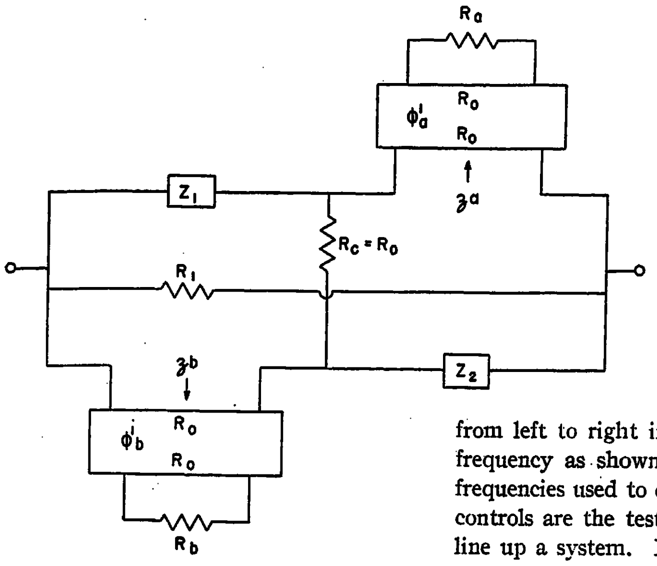


Figure 3. Adjustable network with two controls

The assembled *A* equalizer occupies 12¼ inches of relay rack space. All controls are accessible from the front panel and are arranged in two rows reading from left to right in order of ascending frequency as shown on Figure 9. The frequencies used to designate the various controls are the test frequencies used to line up a system. Each dial or key actually controls the loss characteristic over a moderately wide frequency range. The meter is provided to enable the operating force to check the thermistor voltages in order to determine when the regulating networks are operating near the end of their adjustment range. When such an indication is obtained by a reading of less than 0.3 volt or more than 2.0 volts, the coarse key associated with that particular line-up frequency is operated to provide a block of equalization similar in shape to that provided by the regulating network. When this is done the pilot channel regulator acts to compensate for the change and thus restores itself to mid-scale operation.

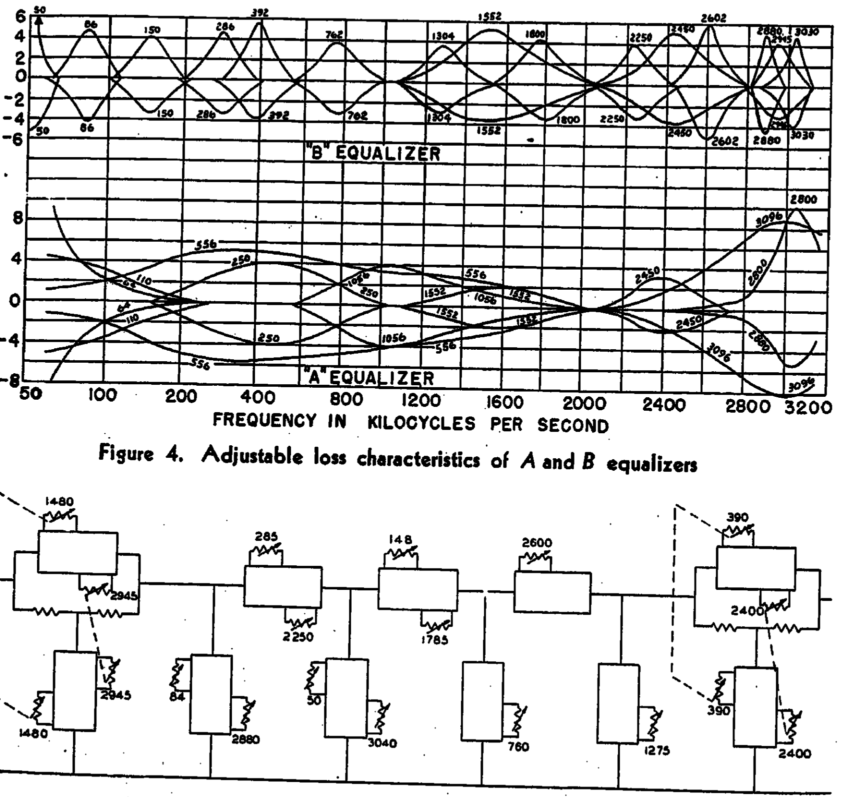


Figure 4. Adjustable loss characteristics of *A* and *B* equalizers

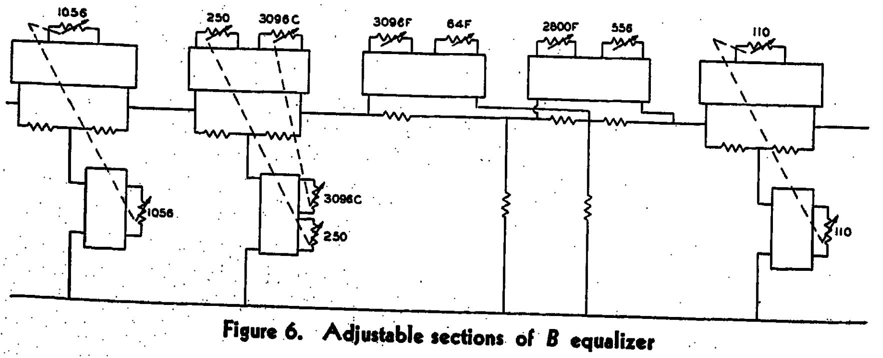


Figure 5. Adjustable sections of *A* equalizer

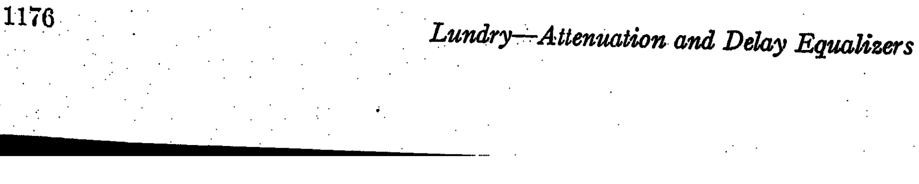


Figure 6. Adjustable sections of *B* equalizer

Completed equalizers are factory tested to insure a product uniformity of ±0.13 decibel. This check is made for the nominal, minimum, and maximum positions of each dial and key individually. This test is made by comparing each equalizer with a reference standard equalizer on a recording loss circuit. A continuous curve of the loss over the frequency range 50 to 3,150 kc is plotted in three minutes with this circuit. Twenty-four curves are taken on each completed equalizer.

In order to meet these extremely close end-product limits, careful control is exercised at each step in the manufacturing process. The individual capacitors used in the equalizer are manufactured to limits of ±1/2 or ±1 per cent, depending on the type of loss section. All resistors are manufactured to ±1 per cent tolerance. The inductors are manufactured in most cases to ±1 per cent and to ±2 per cent wherever these limits are tolerable. In many cases, however, closer limits are required on the inductance-capacitance product of coil-capacitor combinations. In these cases adjustable inductors are used. The adjustment is obtained by use of a magnetic core in the center of the solenoidal winding. The core and the winding form are threaded so that rotation of the core moves it longitudinally inside the winding. When such coils are used the resonant frequency of a coil-capacitor combination is adjusted to ±0.5 per cent. In some cases the coils being adjusted may be as low as 0.2 microhenry. Thus the adjustment requires setting the effective inductance to ±0.002 microhenry. Careful attention to details are required in setting up and adjusting the sections to this precision.

Design and Performance of the *B* Equalizer

The "B" equalizer has 15 adjustable loss characteristics shown at the top of Figure 4, obtained by using the configuration shown in Figure 6. The equalizer itself is shown in Figure 10. All adjustments are made by dials having 20 uniform loss steps. These are arranged on

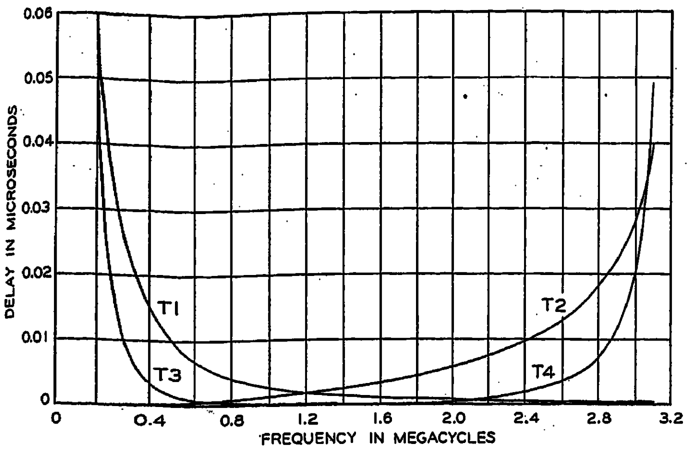


Figure 7. Basic delay characteristics of system

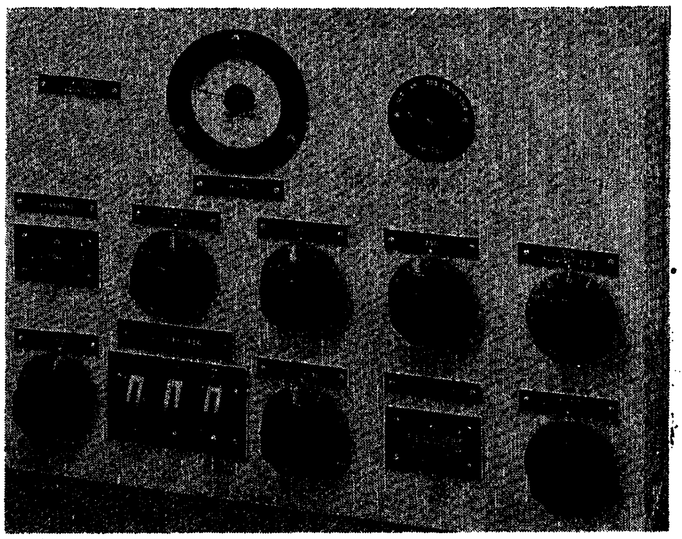


Figure 9 (right). *A* equalizer

the face of the equalizer in three rows reading from left to right in order of ascending frequency, except for the three dials which provide small steps duplicating the performance of coarse keys in the *A* equalizer. In order to simplify the line-up procedure these three dials are located at the upper left, to facilitate their adjustment before beginning the routine adjustment of the *B* equalizer controls.

The sections of the *B* equalizer are associated in unit assemblies in order to break up the assembly into parts which may be manufactured and tested conveniently. Typical unit assemblies are shown in Figure 11. The individual components in this assembly are mounted on fibre strips which individually form a subassembly to which requirements may be assigned for purposes of checking or trouble shooting.

In general, the loss characteristics of the *B* equalizer tend to be narrower and hence, to require a more precise setting of the resonant frequency of coil-capacitor combinations. For example, one of the *B* equalizer characteristics adjusts the loss over a band 200 kc wide centered at 3,030 kc where the resonant frequency must be adjusted to ±6 kc or ±0.2 per cent. This circuit is so sensitive to the parasitic capacities of the surrounding metal container that it is necessary to assemble the network completely and then to make the adjustment through holes in the can. The adjusting holes are later closed by soldering on a metal disk.

Since *B* equalizers are used less frequently in the circuit, wider end product limits are tolerable. The performance of the completed equalizer is checked by comparing its loss to that of a standard equalizer at the maximum and minimum settings of each of the 15 controls on a recording loss circuit. Limits of ±0.25 decibels are set on these characteristics.

Delay Equalizers

LINE AND EQUALIZER CHARACTERISTICS

The delay characteristics encountered in the *L-1* system are comparatively simple to describe. They consist principally of the delay arising from the minimum phase associated with the gain cut-offs of the system. For a detailed discussion of the minimum-phase concept, see H. W. Bode, Network Analysis and Feedback Amplifier Design.[3] The four types of amplifier (auxiliary, receiving, transmitting, and flat-gain) used in the system have different cut-off characteristics so that it is necessary to provide a flexible system of equalizers which will allow for different numbers and proportions of these types in any one circuit.

A satisfactory description of the delay of a circuit is obtained by using four delay characteristics, two to describe the behavior at the higher frequencies and two at the lower frequencies, that is

$$T = T_0 + K_1T_1 + K_2T_2 + K_3T_3 + K_4T_4$$

where T is the envelope delay of the system, T_1,—, T_4 are delay characteristics (shown in Figure 7) and K_1,—, K_4 are constants. The constant delay represented by T_0 arises principally from the finite transmission time of the cable but includes any other effects which introduce linear phase.

Figure 8. Delay characteristics of typical equalizers

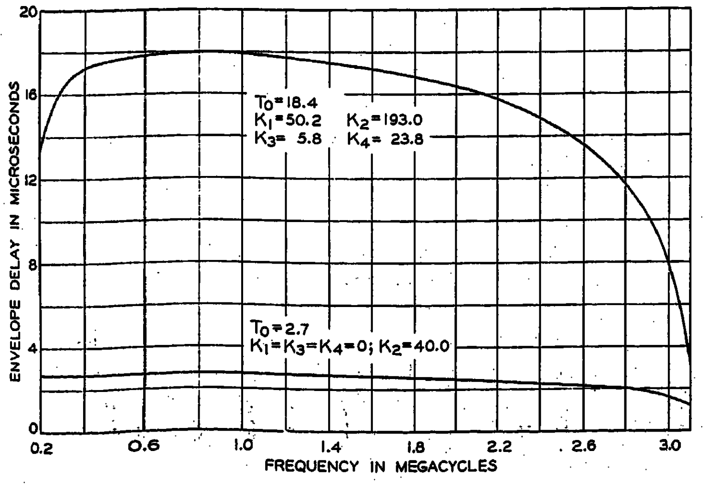
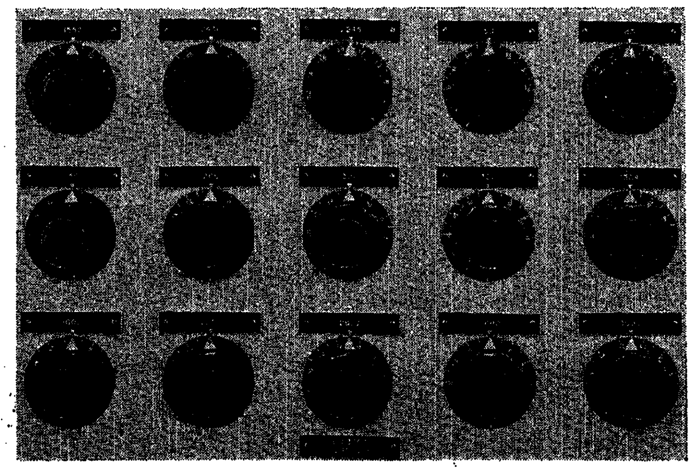


Figure 10. *B* equalizer



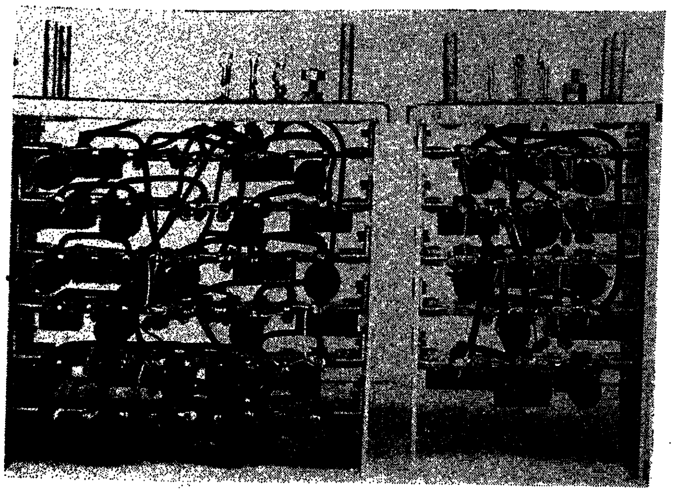


Figure 11. Typical units of B equalizer

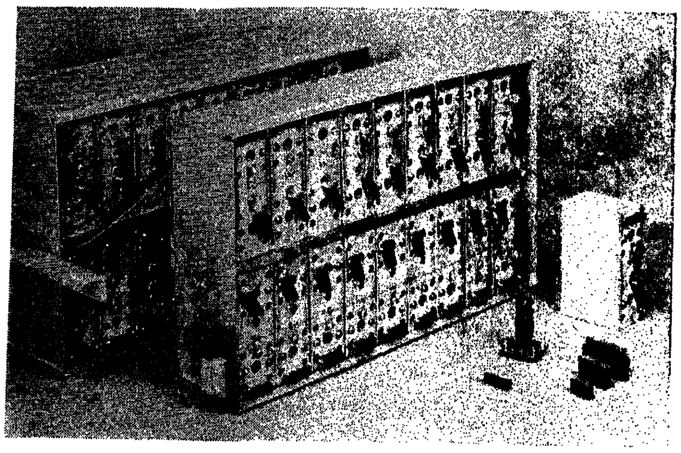


Figure 12. Assembly and components of typical delay equalizer

From a study of several short systems the most frequently occurring values of K_1, —, K_4 were determined and a series of equalizers was designed. The larger units in this series are designed to equalize an average system and have a delay characteristic which is a composite of the functions T_1, —, T_4. Smaller units are used to permit trimming the equalization to specific circuits. These usually have characteristics which are defined by one of the T_1, —, T_4.

Delay equalizers consist of a series of "all-pass" or "nonminimum phase" sections. Physical restrictions limit these to positive delay, that is to a phase characteristic which is a monotonic, increasing function of frequency. This means that the equalizers must be designed to have a delay complementary to that of the unequalized system so that the sum of equalizer plus system delay is constant with frequency at a value equal to or greater than the maximum delay of the unequalized system. Thus the delay of the equalizers is expressible in the form

$$T_e = T_0 - K_1T_1 - K_2T_2 - K_3T_3 - K_4T_4$$

Practically, the minimum value of T_e in the useful frequency range is substantially greater than zero in order to permit adequate design control of the delay characteristic near the edges of the band. This requires that T_0 be greater than the maximum value of ΣK_iT_i, regardless of the frequency at which this maximum occurs. Thus, an equalizer equalizing for T_2 alone would have a value of T_0 determined by the value of K_2T_2 at 3.1 megacycles, while an equalizer correcting for T_1 alone would have a value of T_0 determined by the value of K_1T_1 at 0.2 megacycle. On the other hand, an equalizer compensating for both of these distortions would have a T_0 determined by the larger of them (since $T_2=0$ at 0.2 megacycle and $T_1=0$ at 3.1 megacycles). For equal distortion at the two ends of the band, the delay level (T_0) would be half as much in the combination equalizer as in the case where two independent equalizers are used.

The delay level is not particularly important from an operational point of view. However, the fact that this level must be built up by means of all-pass sections is important economically. The equal distortion case just cited would require at least twice as many sections in the individual equalizers as in a combination design.

The delay characteristics of one equalizer of each type are shown on Figure 8.

Small adjustment of delay must be made periodically to compensate variations in the system, due to temperature changes, aging, and similar effects. It proves more economical to make these adjustments by adding small increments of delay simulating the delay of the system. This requires that the equalizers used in a circuit shall be chosen to equalize the maximum distortion ever expected in the line. Then, most of the time, the delay is built up by the adjustable networks to match the equalizer characteristics.

DESIGN AND PERFORMANCE OF DELAY EQUALIZERS

The designs of delay equalizers for this system follow the potential analogue method.[4] To fit the inherently unbalanced nature of the coaxial circuit, unbalanced bridged-T sections are used to realize the design.

A family of six delay equalizers and two building-out networks has been developed. The largest of these contains 52 all-pass sections, the smallest eight sections. Each section is assembled as a completely shielded unit to simplify adjusting, testing, and handling.

An exploded view of one of these equalizers is shown in Figure 12. Each of the 52 all-pass sections is assembled in its individual shield. Most of these contain two inductances and three capacitances arranged in conventional bridged-T configuration. The capacitances are manufactured to close tolerances—usually 1/2 per cent. The inductors contain threaded magnetic plugs which are used to adjust the inductance to resonate with the associated capacitors.

The critical frequencies of the sections, corresponding to the resonant frequencies of the series and shunt arms, are adjusted to an accuracy of ±0.5 kc. At 3,000 kc this corresponds to a frequency accuracy of 0.017 per cent, or, in terms of the adjustable element, an inductance accuracy of 0.008 per cent.

The comparatively wide manufacturing variations encountered in the dissipation factor of the inductors makes it necessary to provide means for adjusting each one individually. This is done by adding two resistances external to the unit assembly at the time the resonance frequency adjustments are made. Selected resistance values are used to hold the loss of each section within ±0.03 decibel of its nominal value.

The completed equalizers are tested for transmission loss and phase shift to insure a product uniformity of ±0.1 decibel loss and ±1 degree phase over the entire operating band. Input and output impedances of each network are required to have a reflection coefficient less than 5 per cent against a 75-ohm resistance.

These limits are extremely close. However, they are barely adequate to insure satisfactory delay equalization of present-day television circuits, 1,000 miles long (for example, New York—Chicago). Such circuits use approximately 30 equalizers distributed along the line and the cumulative effect of such manufacturing deviations is large compared to the tolerances acceptable for present-day television transmission. For circuits of transcontinental length further refinement of the equalization method will be required.

References

1. EQUALIZATION OF COAXIAL LINES, K. E. Gould, *AIEE Transactions*, volume 68, part II, 1949, pages 1187–92.

2. VARIABLE EQUALIZERS, H. W. Bode. *Bell System Technical Journal* (New York, N. Y.), volume XVII, April 1938.

3. NETWORK ANALYSIS AND FEEDBACK AMPLIFIER DESIGN (book), H. W. Bode. D. Van Nostrand and Company (New York, N. Y.), chapters 14 and 15, 1945

4. DELAY EQUALIZATION OF EIGHT KILOCYCLE PROGRAM CIRCUITS, C. H. Dagnall, P. W. Rounds. *Bell System Technical Journal* (New York, N. Y.), volume XXVIII, April 1949.

No Discussion

Stability of Tandem Regulators in the L-1 Carrier System

J. P. KINZER
NONMEMBER AIEE

Synopsis: The *L-1* coaxial carrier system, with its dynamically-regulated amplifiers, has envelope gain in the vicinity of two cycles per second. With a large number of such amplifiers in tandem, the transient response to an input step function has a peak-to-peak amplitude, or oscillation, many times greater than the size of the initial step. It therefore is necessary to insure that the over-all envelope gain is not so great that small input disturbances normally occurring in system operation are magnified to a point where transmission is impaired.

Calculated effects agree with experimental observations with small steps.

THE *L-1* carrier system[1] transmits a band from 64 to 3,096 kc over coaxial conductors with repeater stations at about 8-mile intervals for $^3/_8$-inch coaxial, and $5^1/_2$-mile intervals for the older $^1/_4$-inch coaxial. A pilot frequency of 2,064 kc is supplied at the transmitting terminal. Somewhat more than half the repeater stations are equipped with pilot regulators, which operate on their associated amplifiers to maintain a constant level of pilot at the amplifier outputs. By this means, variations in the loss of the coaxial (caused by temperature changes, for example) are compensated, and the over-all transmission maintained constant. At stations not having pilot regulators, manual gain controls are supplied and these are readjusted occasionally.

In a transcontinental coaxial circuit, there may be more than 200 regulated amplifiers in tandem. Under this circumstance there arises a problem of over-all dynamic stability, even though individual regulated amplifiers may be entirely stable. The instability manifests itself as a continuous irregular variation in transmission of moderate amplitude, termed "normal jitter," with occasional large excursions or "hits" of perhaps several decibels (db).

Figure 1 shows a block schematic of a line amplifier with its associated regulator. The 2,064-kc pilot frequency is picked off by a crystal filter, amplified and rectified. The rectified direct current is used to control the bias on an oscillator tube and thus control its 2,000-cycle output. This output heats the thermistor, which is in the feed-back path of the line amplifier and controls its gain. The circuit is so arranged that a drop in pilot level results in an increase in amplifier gain which restores the pilot level to almost its original value. Thus a change in the pilot level at the input of the amplifier appears at the output greatly reduced in magnitude.

Any variation, or amplitude modulation of the pilot level, is usualy spoken of as the envelope. If the pilot-frequency amplitude modulation is sinusoidal, one speaks of the envelope frequency.

If the envelope frequency is relatively high, say more than 100 cycles per second, the thermistor is unable to follow because of thermal lag, and the pilot variation is passed through the amplifier-regulator combination without change of amplitude or phase. On the other hand, at very low frequency the thermistor is able to follow the variation, and it suffers a loss going through the amplifier-regulator combination. In the *L-1* system, this loss is 20 to 30 db at zero frequency. The combination therefore has an envelope transmission-frequency characteristic.

The envelope transmission characteristic can be measured directly. This has been done in the field on lines with different numbers of regulators in tandem. It also can be calculated from measurements on the feed-back loop of the combination. This "backward-acting" amplifier-regulator combination is a feed-back circuit with a rather well-defined feed-back loop, and general principles of feed-back-amplifier design apply.[2,3] A convenient place to break the feed-back loop for measurement of low-frequency complex $\mu\beta$ (notation is that of Bode)[2] is at the grid of *V-3* (line designated D-C Bias in Figure 1). The envelope transmission *ET*, is then given by

$$ET=\frac{1}{1-\mu\beta}$$

The two methods of determining the envelope transmission characteristic agree well. Figure 2 shows a representative characteristic. Note that there is actually an envelope gain of about 0.3 db at 2.5 cycles per second. This adds directly for tandem regulators, so that 130 regulators would have an over-all envelope gain of some 40 db.

This means that if the 2,064-kc pilot at the input of a 130-db regulator system has 1/10 per cent modulation with 2.5 cycles per second (for example), at the output of this system the pilot would have 100 times as much modulation, that is, 10 per cent. Of course, this situation normally does not exist, as the pilot frequency supplies are exceptionally well stabilized. However, it might occur as a trouble condition in a station with an abnormal cross-talk path between two independent nonsynchronized pilot supplies.

Input disturbances more likely to occur in service are sudden changes, called hits, caused, for example, by bridging a high-impedance transmission measuring set across the line to make a maintenance measurement, or by switching from one line to another. This type of input transient is easy to create and lends itself as well as any to mathematical analysis.

Paper 49-247, recommended by the AIEE Communication Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 20, 1949; made available for printing September 2, 1949.

J. P. KINZER is with the Bell Telephone Laboratories, Inc., New York, N. Y.

The results summarized herein include the contributions of many other members of the technical staff of the Bell Telephone Laboratories. To these, appropriate appreciation is extended herewith.

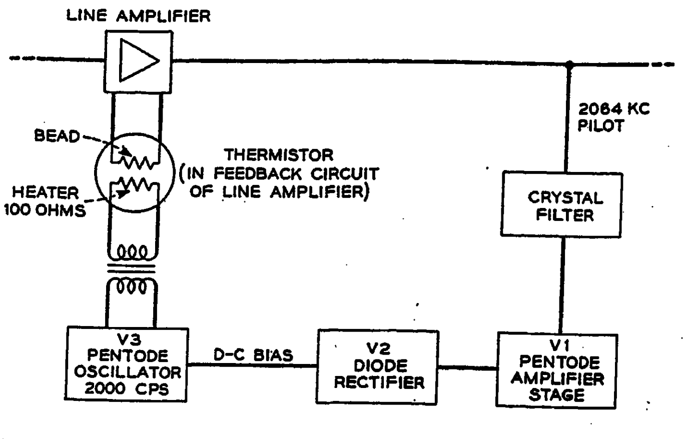

LINE AMPLIFIER
BEAD
HEATER 100 OHMS
THERMISTOR (IN FEEDBACK CIRCUIT OF LINE AMPLIFIER)
2064 KC PILOT
CRYSTAL FILTER
V3 PENTODE OSCILLATOR 2000 CPS
D-C BIAS
V2 DIODE RECTIFIER
V1 PENTODE AMPLIFIER STAGE

Figure 1 (above). Block schematic of pilot regulator and line amplifier

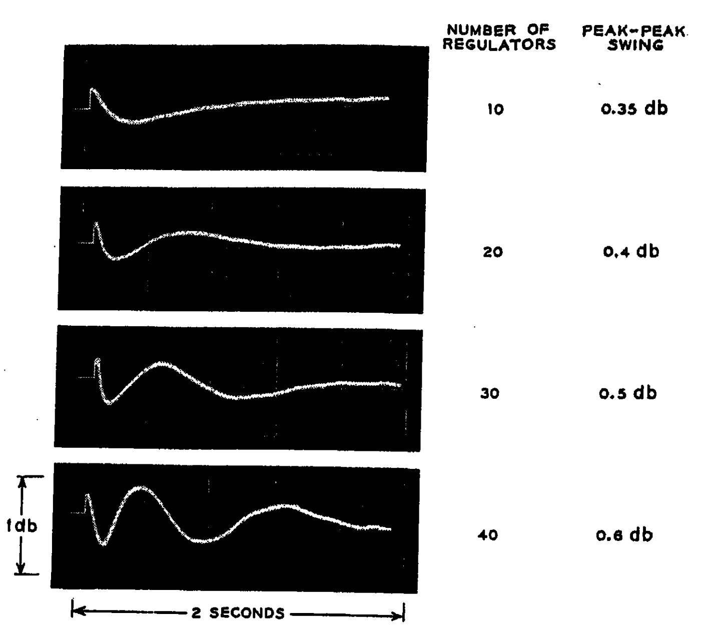


Figure 3 (right). Transient response to 0.2-db step-up, of loops with 10, 20, 30, and 40 regulators

Oscillograms were retouched to show the initial step plainly

Figure 3 shows a typical transient produced by 0.2-db stepup in pilot level. The circuits were so arranged that the pilot could be observed simultaneously after passing through 10, 20, 30, and 40 regulators. Note that as the number of regulators in tandem, and hence the overall envelope gain, increases, the magnitude of the transient increases. When the envelope gain is large, the magnitude of the transient may be much greater than the initial disturbance. The ratio of the peak-to-peak magnitude of the transient to the size of the original step is called the magnification.

Figure 4 shows the relation between total envelope gain and magnification. Various field measurements on the *L-1* system are indicated by dots, circles, and so forth. The heavy line thus represents the experimentally observed relation between total envelope gain and magnification, and the dashed line a reasonable extrapolation. The light line shows a mathematically derived relation. The agreement between theory and observation is good.

Large transients represent one type of difficulty resulting from excessive envelope gain. Another, called normal jitter, is shown on Figure 5. This is believed to be the end result of the summation of a large number of transients caused by tiny disturbances all along the system. It is believed that the major source of these disturbances is power supply fluctuation, changing the operating points of vacuum tubes in regulators and amplifiers. Normal jitter is very erratic in nature and its magnitude varies greatly from time to time. The oscillogram shows about $^1/_2$-db peak-to-peak normal jitter; it was taken on a system that had almost 40-db envelope gain, or about 0.5 db per regulator. The system of Figure 3 had about 0.4-db envelope gain per regulator. Since these oscillograms were made, design changes in the regulator have reduced the envelope gain to about 0.3 db maximum, as shown in Figure 2. The envelope gain also de-

Figure 2 (below). Envelope transmission of line amplifier with pilot regulator

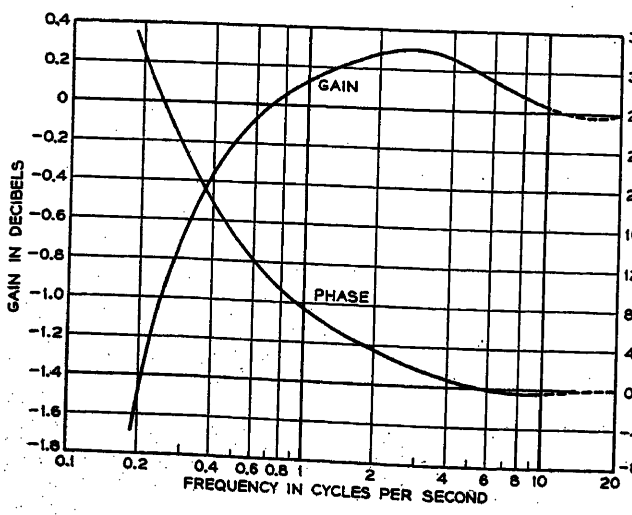
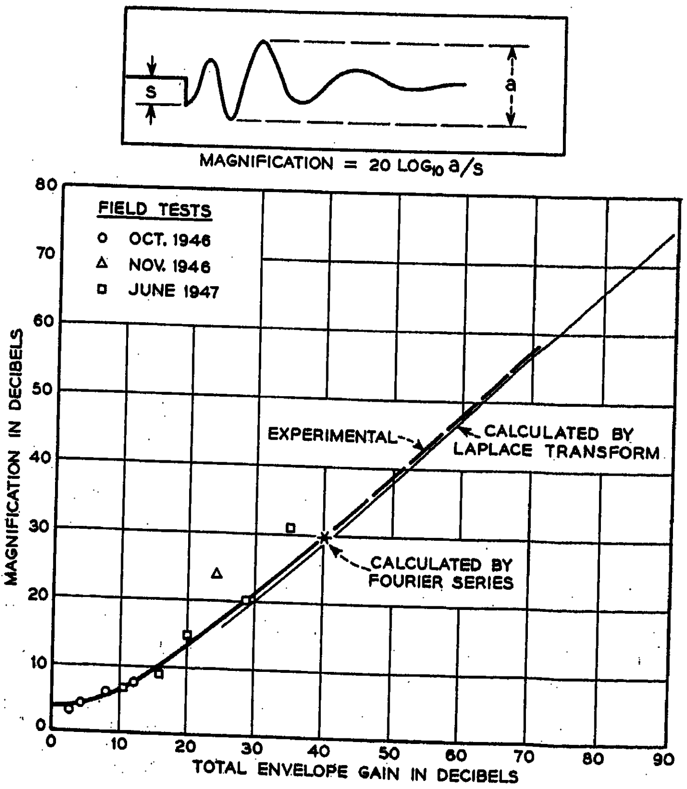


Figure 4. Transient performance of cascaded regulated amplifiers



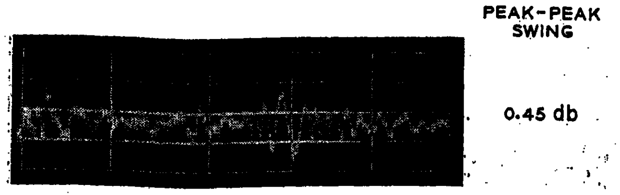


Figure 5. Normal jitter on loop with 86 regulators

pends on the portion of the regulation range which is in use, and thus on the time of the year.

Calculation of Transient Response to Step Input

It is desirable to be able to calculate the transient response of a system, before it is available for test, from the known envelope transmission characteristic. Two methods of doing this are by the Fourier Series and the Laplace Transform. In principle, these are essentially equivalent, but the techniques of working out the final result differ greatly.

By the Fourier Series method, the steady-state response of a system to a square wave input is calculated. If the period of the square wave is large compared to the duration of the transient response to a step function, the steady-state response to the square wave is then very closely a repetition of the transient responses to steps in alternate directions. The method is simple enough in principle, but rather tedious in operation because extensive computations are necessary. Briefly, each component of the (input) spectrum of the square wave is modified by the transmission characteristic (amplitude and phase) of the system and the modified (output) components combined to give the response. This must be done for each instant of time, and to achieve any accuracy, a considerable number of components must be taken.[4] The method is, however, well adapted to machine computation.

As a verification of the validity of the method, the transient response was calculated for a system on which experimental data were available. The comparison of computed and measured response is shown in Figure 6; it is seen that the agreement is good. On this figure, the solid lines represent the limits of a strip within which fall all of six experimental characteristics. The dotted line represents the calculated response. The zero of time has been taken at the instant of the first peak. The start of the transient is not well defined on the oscillograms, and the Fourier Series method gives poor results near the origin, as is well known.

The definition of the first peak is better than that of the later ones, which tend to be broader. For clarity, Figure 6 has been drawn to a larger scale than is justified by the accuracy of the measurements.

The calculated response of a system with 40-db envelope gain is shown in Figure 7. The response was first calculated by a relay computer using harmonics up to the 139th, and in steps of 2 degrees. This was to determine the region of maximum oscillation, and was followed by a fine structure determination in this region using steps of 0.5 degree.

As shown in Figure 7, the initial step is lost in the inaccuracy of the method near the origin, and the dotted line has been drawn to show the probable response near the origin. From this curve the magnification ratio is 32, that is, 30 db. The value is shown as the star in Figure 4 and has been used as a guide in the drawing of the curve.

By the Laplace Transform method, the response to a unit step function is computed by analytic methods. An analytic expression is fitted to the experimental envelope transmission characteristic. The direct Laplace Transform of the unit step is multiplied by this expression, and an inverse Laplace Transform made to obtain the result. Analytic methods are necessary because this is the only feasible way to carry out the necessary integrations in the complex plane.

The technique used is to assume a rather simple expression for a single regulator, and raise it to *n*th power for *n* regulators in tandem. This method is the one by which the theoretical curve of Figure 4 has been calculated. Details are given in Appendix I. An alternative procedure is to fit an analytic expression, say a power series, to the experimental overall envelope transmission characteristic, and proceed from this point. This attack has not been attempted here.

Experimental Results

The theoretical approaches discussed above of necessity imply a linear system. However, it has been observed that as the size of step is increased, nonlinearity effects in the system operate to reduce the magnification rather rapidly and to produce differences between steps up and steps down in pilot level. See Figure 8.

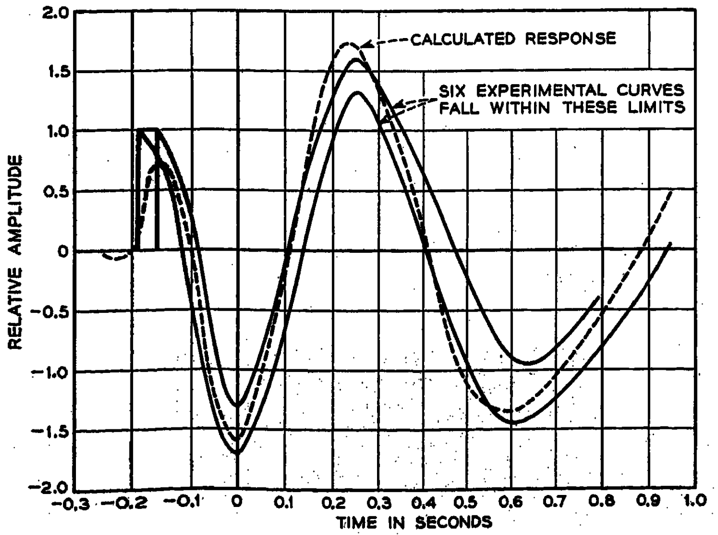
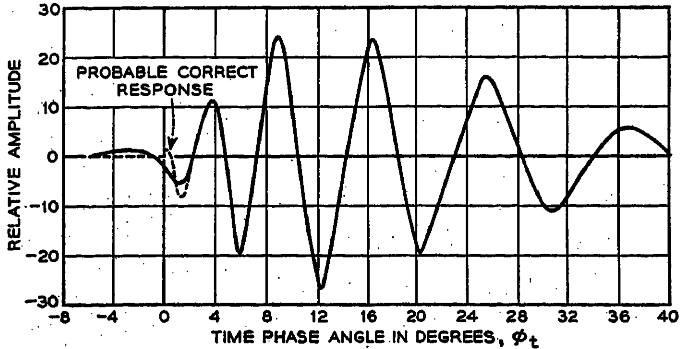
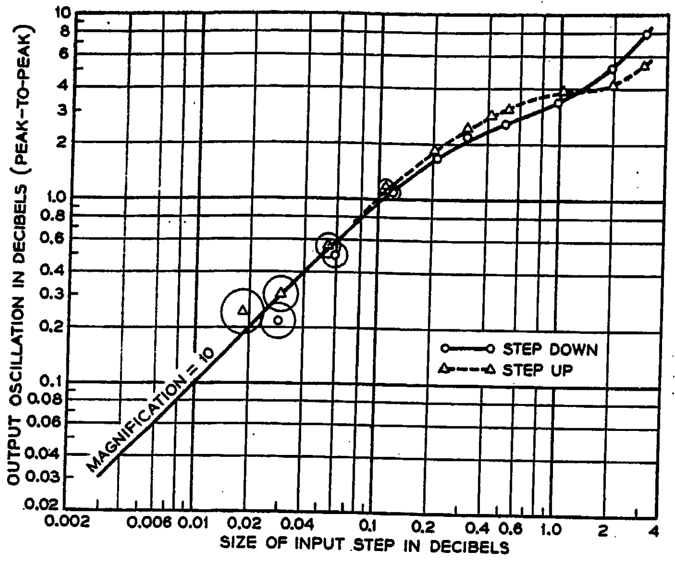


Figure 6 (left). Comparison of calculated and experimental transient response of system with 16-db envelope gain

Figure 7 (below). Calculated transient response of system with 40-db envelope gain





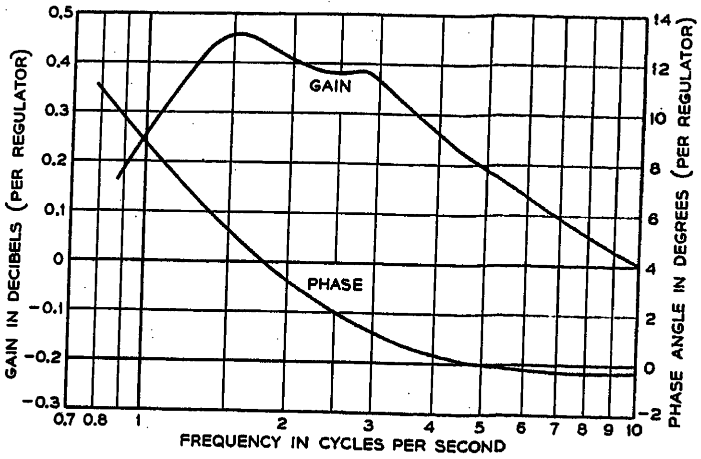


Figure 8 (left). Transient performance of system with 28-db envelope gain

Figure 9 (above). Envelope transmission of the system of Figure 8. This loop had 62 regulators

OVER-ALL SYSTEM ENVELOPE TRANSMISSION

System envelope transmission characteristics are measured on a loop by applying a small percentage of low-frequency sinusoidal modulation to the outgoing 2,064-kc pilot, and comparing this in magnitude and phase with the modulation of the received pilot. The low-frequency oscillator has two outputs, whose relative phase can be varied over 360 degrees and read directly from a phase dial. One output is connected to the horizontal drive of a long persistence oscilloscope (*P-7* screen), and the other used to modulate the pilot. An attenuator is provided in this output to permit variation of the amount of pilot modulation. The pilot envelope is applied to the vertical deflection of the oscilloscope. Rotation of the phase dial collapses an ellipse on the scope screen into a straight line. Two scopes are used, one at the output of the transmitting amplifier, the second at the output of the receiving amplifier. The procedure is to adjust the phase dial and attenuator to obtain reference traces on the two scopes in turn, and take the difference between readings. If a low-frequency oscillator with the variable phase feature is not available, the phase can be determined from measurements of the major and minor axes of the ellipse. In another method, a slow speed linear sweep is applied to the horizontal deflection of the oscilloscope, the scope spots are blanked abruptly during its progress, and the phases of the traces at the instant of blanking are determined.

To override system normal jitter and other extraneous disturbances, it is necessary to use so high a percentage of pilot modulation that nonlinearity effects are appreciable. Also, at frequencies below the peak of envelope transmission, harmonics in the oscillator become troublesome as they are amplified with respect to the fundamental in going through the system. Both these effects result in considerable distortion of the ellipses and make measurements at envelope frequencies below one cycle per second very uncertain.

Typical results on a loop with 62 regulators, reduced to a per regulator basis, are shown in Figure 9. It is found that the general shape and magnitude of these gain and phase curves are in agreement with those for a single regulated amplifier under similar working conditions. This indicates that there probably is no large error in the measurements and that the regulators probably do not differ greatly from typical specimens. It has been noted that there is a decided flattening of the gain characteristic for many regulators in tandem. This may be a consequence of the peak in envelope gain coming at slightly different frequencies for different regulators.

TRANSIENT RESPONSE

Figure 10 shows a typical response, in this case for 0.1-db stepdown applied to the system of Figures 8 and 9. Here the peak-to-peak oscillation is 1.1 db. The data from many such oscillograms are plotted as shown in Figure 8, the object being to get the best possible extrapolation back to the small step case. The magnitude of the oscillation in db is plotted against the step size on log-log paper, and a 45-degree asymptote drawn that best fits the plotted points. The intercept of this asymptote with the 1-db abscissa gives the magnification ratio. On Figure 8, the size of each circle gives an indication of the estimated accuracy of the respective plotted point. It is evident that the magnification can be determined only roughly.

All the magnifications determined in this way have been plotted on Figure 4, which represents the best presently available experimental relation between the envelope gain and magnification.

Reduction of Envelope Gain

The envelope gain of a particular regulator-amplifier combination varies widely; it depends largely upon the characteristics of the oscillator tube in the regulator and on the operating point of the thermistor in the amplifier. The latter is related to the amplifier gain. The average envelope gain of a large number of tubes, all measured at the point on the thermistor characteristic which yields maximum envelope gain (this point corresponds closely to average amplifier gain), is called the average maximum envelope gain; this was found to be 0.7 db per

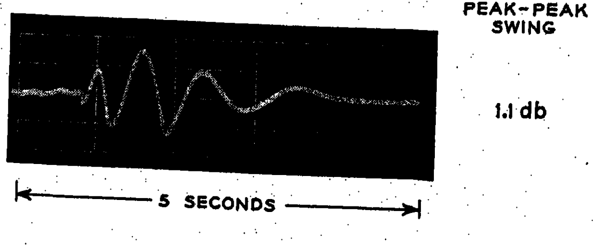
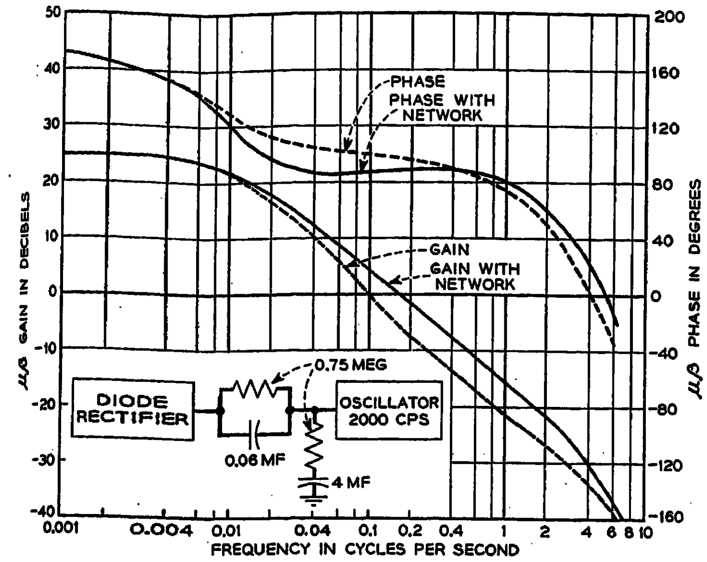


Figure 10. Transient response to 0.1-db step-down of system of Figure 8



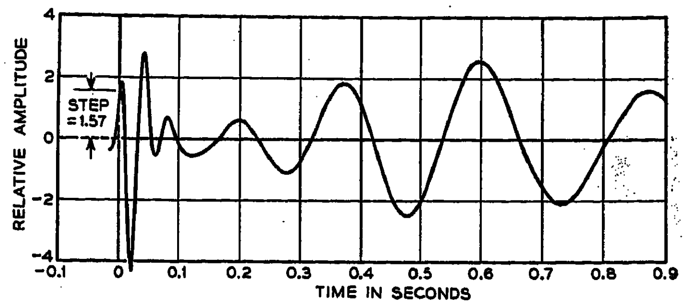


Figure 11 (left). Feed-back loop ($\mu\beta$) characteristic of regulator and amplifier, with and without corrective network

Figure 13 (above). Calculated transient response of system with mop-up regulators, in which the envelope transmission characteristic has two peaks, each of 20-db gain

regulator for the regulators as used in the field tests. With an arbitrary limit of, say, 40 db on the envelope gain, the maximum number of regulators in tandem would be limited to about 60. This limit can be met readily on message facilities because traffic flexibility considerations dictate the termination of the coaxial system and the supply of fresh pilot frequency at least this often. Furthermore, field experience indicates that the maximum value of 0.7 db is not likely to be reached on all regulators. Nevertheless, methods for the reduction of envelope gain were explored.

It is not particularly difficult to design a completely new amplifier-regulator combination with a large reduction in envelope gain. It is quite a different problem, however, to devise a method for practical modification of large numbers of existing equipments. A number of methods were tried and found unsuitable for one reason or another. The modification finally adopted is the addition of a corrective network in the feed-back circuit, inserted at the oscillator grid (lead labelled D-C Bias in Figure 1). The network configuration and its effect on $\mu\beta$ of a particular regulator are shown in Figure 11. This network is of a type, well known in feed-back amplifier design, in which phase is transferred from a region (0.05 cycle per second) where it is in excess to a region (5 cycles per second) where it is needed. This network gives somewhat more than 2 to 1 reduction in envelope gain and results in a regulator-amplifier combination whose average maximum envelope transmission is shown in Figure 2.

Use of a larger capacitor in a network like that of Figure 11 gives more reduction in envelope gain. A network using 30 microfarads was field tested and found to yield a reduction of 6 to 1 in envelope gain. However, the 4-microfarad capacitor is the largest that can be included in the regulator case; an electrolytic capacitor cannot be used because of leakage requirements.

Mop-Up Regulator

If further reduction of the envelope gain should be required, this can be supplied by the use, at infrequent intervals, of mop-up regulators, whose function is to supply envelope loss at the frequency where the other regulators have envelope gain. This regulator is similar in principle to the other 2,064-kc regulators but its circuit details are quite different and it operates in conjunction with a different thermistor in its associated amplifier. Trial models have been built which are externally similar and mechanically interchangeable with the standard regulator and mount in the same position in the office as the ordinary regulator which they replace.

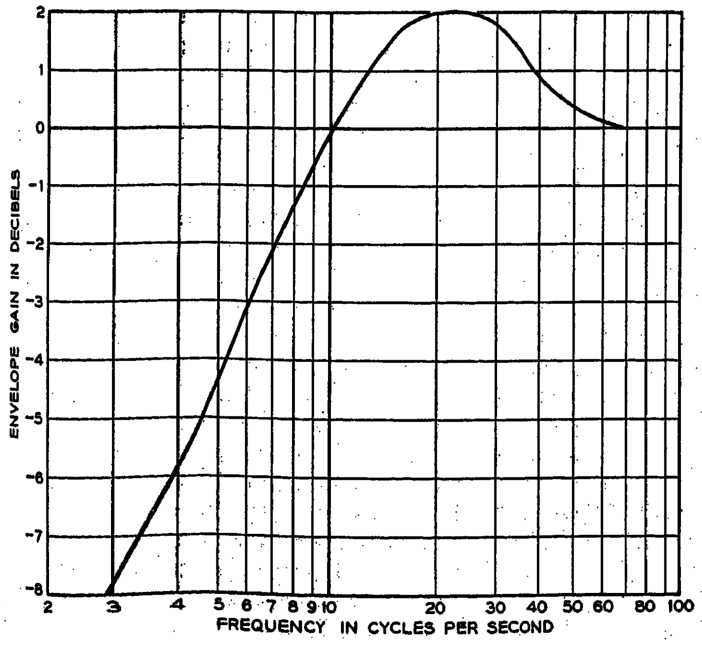
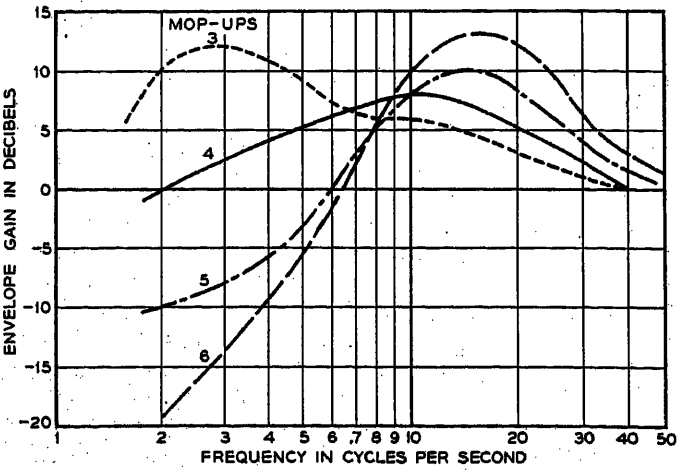
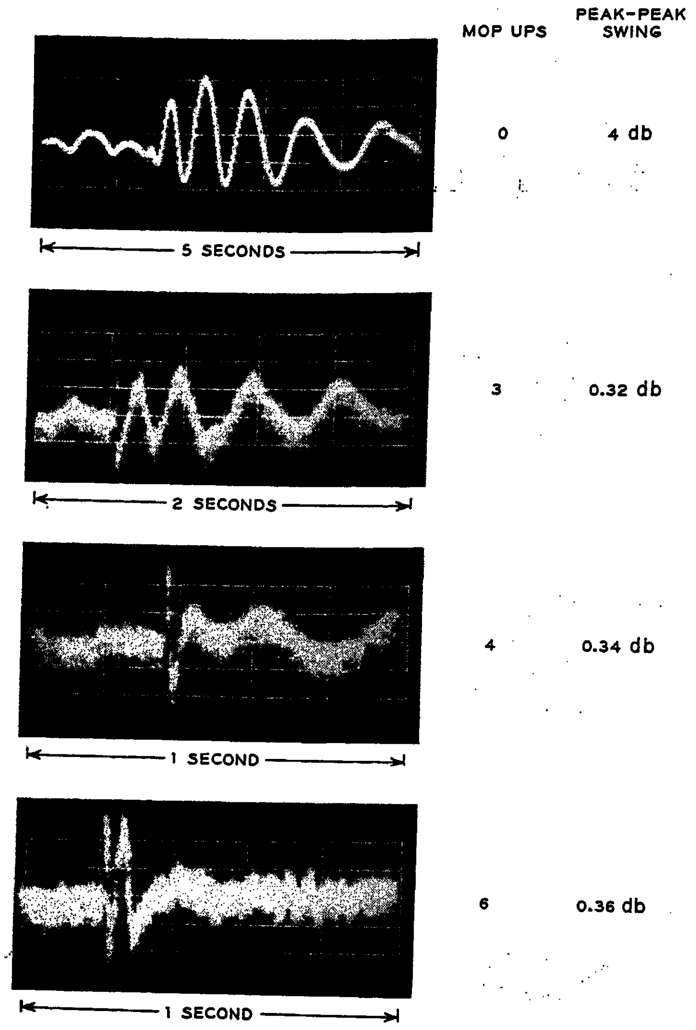


Figure 12 (left). Average envelope gain of six experimental mop-up regulators

Figure 14. Envelope gain of 2,000-mile circuit with mop-up regulators





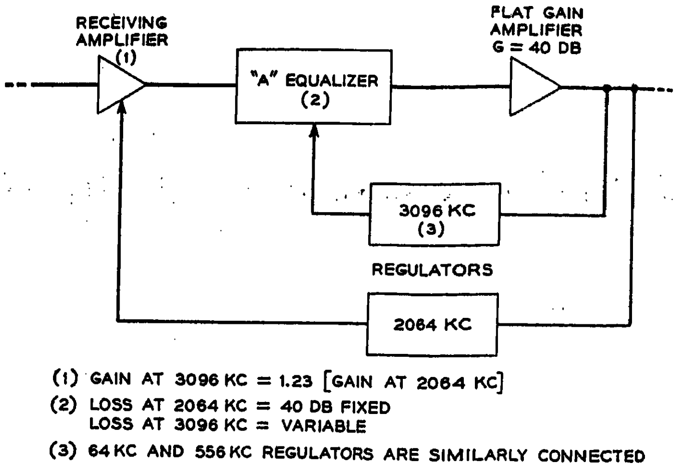

RECEIVING AMPLIFIER
(1)
"A" EQUALIZER
(2)
FLAT GAIN AMPLIFIER
G = 40 DB
3096 KC
(3)
REGULATORS
2064 KC
(1) GAIN AT 3096 KC = 1.23 [GAIN AT 2064 KC]
(2) LOSS AT 2064 KC = 40 DB FIXED
LOSS AT 3096 KC = VARIABLE
(3) 64 KC AND 556 KC REGULATORS ARE SIMILARLY CONNECTED

Figure 15 (left). Transient response to 0.2-db step-up, of 2,000-mile loop with mop-up regulators

Figure 16 (above). Simplified block schematic of regulators at a main station

Figure 12 shows the average envelope transmission of six trial model mop-up amplifier-regulator combinations. Note that considerable envelope gain exists in the vicinity of 25 cycles per second.

The determination of the correct spacing for the mop-up regulators is quite complex as it depends on a large number of factors, some of which are not too well known. If only over-all envelope gain is considered, it is in principle possible to space and adjust the mop-up regulators so as to equalize the low-frequency (2 to 4 cycles) envelope gain of the normal regulators and the higher frequency envelope gain of the mop-up units.

Naturally, it is to be expected that the characteristics of the transient response (to a step input) of such a system will be different than that observed on systems with only a single peak of envelope gain. Calculation of the transient response by Fourier synthesis was therefore made with punched card equipment.

The calculated transient response of 67 tandem regulators with the 4-microfarad network (20-db peak envelope gain) gives a value for the magnification of 14 db, which agrees well with the previous experimental result of 13.5 db, as shown in Figure 4. This indicates that for the same total peak envelope gain, the slight difference in shape of the envelope transmission characteristics of the regulator with or without the 4-microfarad network is unimportant.

The calculated transient response of a hypothetical system, so made up of mop-up and ordinary regulators that the two peaks of envelope gain are each 20 db, is shown in Figure 13. The initial step shocks the mop-up regulators into a transient response whose chief component is about 25 cycles per second. This transient dies out in about 0.1 second, during which time the other regulators change relatively little. The mop-up regulators now are ready to go to work on the transient introduced by the line regulators. Without the mop-ups this would build up to a tremendous amount (to ordinates of more than ±400 on the scale of Figure 13) but the mop-ups are successful in holding the swings to the extent shown. The transient is substantially over in about two seconds.

The magnification is about 13 db for the high-frequency part, and 10 db for the low-frequency part, of the transient. This is slightly more favorable than the result for a single-peaked characteristic.

A field test of the mop-up regulator was made on a circuit equipped with 134 ordinary regulators. The envelope gain of the normal circuit was 40 db, or 0.3 db per regulator. Over-all envelope gain measurements of the circuit with three, four, five and six mop-ups are shown in Figure 14. One might interpret these as showing that something between three and four mop-ups would be the optimum number for this line.

Transient tests showed the mop-up regulators to give the expected improvement. Figure 15 shows the response to a 0.2 db step upward. The peak-to-peak swing of 4 db for the no mop-up condition is seen to be reduced by a factor of about 12 to 1. The no mop-up case shows a magnification of 28 db which, for 40-db envelope gain, is in good agreement with Figure 4. This magnification is reduced to 4–5 db by the mop-ups. The three mop-up condition best shows the character of the response predicted by the Fourier synthesis. As the number of mop-ups is increased, with resulting increase in high-frequency envelope gain and decrease in low-frequency envelope gain, the initial higher frequency transient is enhanced and the lower frequency transient is decreased. This results in a reduction of the duration of the transient. The mop-up regulators also give a marked reduction in line jitter. The addition of one mop-up shows an improvement of 3 to 1, and three mop-ups give an improvement of 6.5 to 1. For the six mop-up case it is difficult to determine the amount of additional improvement as the jitter is masked by 120-cycle noise, which appears as the fuzzy broad trace on the four and six mop-up cases of Figure 15.

Office Regulators

In addition to the main 2,064-kc pilot frequency, the *L-1* system transmits other pilot frequencies, at 64, 556, and 3,096 kc. These pilots are used to control the characteristic of a transmission equalizer (the *A* equalizer) at main stations by means of backward acting regulators very similar to the line regulator. The general arrangement is shown in Figure 16.

In a transcontinental circuit, there are about 40 such stations.

At each of the pilot frequencies, envelope disturbances in the neighborhood of 2 cycles per second are increased by the envelope gain of these regulators. Furthermore, variations in the line pilot (2,064 kc) react on the office pilots, because a change in the line amplifier gain affects all frequencies. The design of the line amplifier is such that the change in gain in db is proportional to $\sqrt{\text{frequency}}$. The networks in the *A* equalizer are such that the loss at line pilot stays fixed, so there is no reaction of the office pilots on the line pilot.

The simplified analysis of Appendix II indicates that because of the $\sqrt{f}$ factor, the transient performance at 3,096 kc will always tend to be worse than at 2,064 kc. Similarly, conditions are likely to be more favorable at 64 and 556 kc. The equipment arrangements of the office regulators differ from those of the line regulator, and considerable difficulty would be experienced in incorporating the 4-microfarad network. For these, a reduction of envelope gain to 0.3 db is obtained by a cathode resistor in oscillator tube *V-3*. This leaves these regulators with 20-db d-c feedback which is adequate from a regulation standpoint.

Appendix I. Transient Response by Laplace Transform Method

Suppose that the steady-state envelope transmission characteristic of a single regulated amplifier can be considered as that of a 4-terminal linear invariant unilateral network.

Let $p=\sigma+j\omega$ be the complex frequency, and let $y(j\omega)$ be the envelope transfer ratio, that is

$$y(j\omega)=\frac{M_o}{M_i}=\frac{\text{per cent modulation of output pilot frequency}}{\text{per cent modulation of input pilot frequency}}$$

If transit time between amplifiers is neglected, the envelope transfer ratio of n similar tandem regulated amplifiers is $[y(j\omega)]^n$. The transfer function is then $[y(p)]^n$ and the response to a unit step function is given by the inverse Laplace transform

$$M_n(t)=\mathcal{L}^{-1}\left[\frac{1}{p}\,y^n(p)\right] \tag{1}$$

If $y(p)$ is expressible as a rational algebraic fraction, then the inverse transformation is entirely straightforward.[5] Unfortunately, for large n, the number of terms in the result is prohibitive.

Equation 1 gives the response for all instants of time. For engineering purposes, however, it suffices to determine approximately the peak-to-peak swing (oscillation) of the transient and its duration. To this end, the "principle of stationary phase"[6,7] may be used.

This principle is perhaps most clearly explained physically in connection with the Fourier synthesis of the response to a square wave input

$$M_n(t)=\sum_{h=1}^{\infty} A_h{}^{(n)}\sin\,(n\varphi_h+h\varphi_t);\quad (h\text{ odd})$$

in which

$A_h{}^{(n)}$ = the amplitude of the hth harmonic of the fundamental after traversing n regulated amplifiers

φ_h = its phase shift in traversing a single amplifier

φ_t = the time-phase, that is, the phase of the fundamental at the instant under consideration

The principal contribution to M_n comes from those components having the largest values of $A_h{}^n$. If these are all clustered around the largest, say A_m, then they all add directly when the phase angle $(n\varphi_h+h\varphi_t)$ is nearly the same for all. This occurs at the time, t_m, when the phase is stationary with respect to h in the neighborhood of $h=m$, that is, when

$$\varphi_{t_m}=-n\left[\frac{\Delta\varphi_h}{\Delta h}\right]_{h=m} \tag{2}$$

This addition is very clearly seen in the course of calculation of a Fourier synthesis.

To apply the principle, the transfer ratio is expressed in terms of its amplitude and phase characteristics

$$y(\omega)=A(\omega)\exp\,[jB(\omega)]$$

which are in turn expanded in Taylor's series about ω_0, the frequency at which $A(\omega)$ has its maximum value, $A(\omega_0)$. For typographical simplicity, write

$$A_0=A(\omega_0)$$

$$A_0{}'=\left[\frac{d}{d\omega}A(\omega)\right]_{\omega=\omega_0},\text{ and so forth}$$

$$\delta=(\omega-\omega_0)$$

Then the Taylor's series are

$$A(\omega)=A_0+\frac{A_0{}''}{2}\delta^2+\ldots$$

$$B(\omega)=B_0+B_a\delta'+\frac{B_0{}''}{2}\delta^2+\ldots$$

At the maximum of $A(\omega)$, $A_0{}'=0$, it also is assumed here that $A_0{}''<0$ (this is necessary for a maximum) and that the other terms of the series can be neglected.

Using the approximation $\log(1+a)=a$, one has

$$\log\left[\frac{A(\omega)}{A_0}\right]=\frac{A_0{}''}{2A_0}\delta^2$$

$$\left[\frac{A(\omega)}{A_0}\right]^n=\exp\left[n\log\frac{A(\omega)}{A_0}\right]=\exp\left[n\frac{A_0{}''}{2A_0}\delta^2\right]$$

so that, in the vicinity of ω_0,

$$y^n(\omega)=A_0{}^n\exp\left[n\frac{A_0{}''}{2A_0}\delta^2+jn\times\left(B_0+B_0{}'\delta+\frac{B_0{}''}{2}\delta^2\right)\right] \tag{3}$$

The further approximation now is made that, in the important range of ω, the spectrum of the step function is sufficiently constant so that it is given by $1/j\omega_0$.

The foregoing now are substituted in the Fourier integral

$$M_n(t)=\mathcal{R}_e\left[\frac{1}{\pi}\int_0^{\infty}S(\omega)\exp\,(j\omega t)d\omega\right] \tag{4}$$

in which $S(\omega)$ is the spectrum of the response to the unit step. This form is used because the physical network has equal transfer ratios for positive and negative values of ω, whereas the approximation, equation 3, is valid only for values of ω in the vicinity of positive ω_0. Furthermore, because the value of equation 3 is negligible for negative values of ω, the lower limit of integration in equation 4 can be made $-\infty$.

With the following additional substitutions made in equations 3 and 4

$$p=j\omega,\ p_0=j\omega_0,\ \omega=2\pi f$$

$$\delta=-j(p-j\omega_0)$$

$$\rho=-\frac{n}{2}\left(\frac{A_0{}''}{A_0}+jB_0{}''\right)$$

$$\lambda=nB_0{}'$$

equation 4 becomes

$$M_n(t)=\mathcal{R}_e\left[\frac{2A_0{}^n\exp\,(jnB_0)}{j\omega_0}\times\int_{-\infty}^{+\infty}\exp\,[\rho(p-p_0)^2+\lambda(p-p_0)]\exp(pt)\,df\right]$$

in which the integral is of the exact form given in the Campbell and Foster[8] tables. Application of pairs 206 and 729 of the tables yields

$$M_n(t)=\mathcal{R}_e\left(\frac{A_0{}^n}{j\omega_0\sqrt{\pi\rho}}\exp\,(jnB_0+j\omega_0 t)\times\exp\left[-\frac{(t+\lambda)^2}{4\rho}\right]\right)$$

The magnitude of the oscillation now is quickly determined. The second exponential has its largest value, unity, when

$$t=-\lambda=-nB_0{}'$$

which is the exact analog of equation 2. The complex factor $\sqrt{\rho}$ can be expressed in terms of its magnitude and phase. This phase and the factor $1/j$ are combined with the first exponential, which is recognized as a rotating unit vector; it therefore assumes the extreme values ± 1.

Thus, the peak-to-peak swing of $M_n(t)$ is given by

$$M_n \text{ (peak-peak)} = \frac{2A_0{}^n}{\omega_0\sqrt{\pi|\rho|}}$$

or, in a somewhat different form

$$M_n \text{ (peak-peak)} = \sqrt{\frac{8}{\pi}}\,\frac{A_0{}^n}{\omega_0\sqrt{n}}\,G_0 \qquad (5)$$

in which

$$G_0 = \left[\left(\frac{A_0''}{A_0}\right)^2 + (B_0'')^2\right]^{-1/4}$$

To introduce envelope gain and magnification, there now is defined

E = envelope gain = 20 $\log_{10} A_0{}^n$ decibels
M = magnification = 20 $\log_{10} M_n$ (peak-peak) decibels

whereupon equation 5 can be thrown into the form

$$M = E - 10 \log_{10} E + 17 + 20 \log_{10} F_0 \text{ decibels}$$

$$F_0 = \frac{G_0\sqrt{\log_{10} A_0}}{\omega_0}$$

If F_0 were a constant, the magnification would depend only upon the total maximum envelope gain and not upon the shape of the envelope gain-frequency characteristic. The term in F_0 is therefore a measure of the effect of this characteristic.

A good fit to the experimental envelope characteristic of a single regulated amplifier with 0.7-db maximum envelope gain is obtained with a transfer function of the form

$$y(p) = \frac{p(p+1)}{(p+\alpha)(p+\beta)}$$

For $\alpha = 0.1$, $\beta = 0.87$, it is found that $A_0 =$ 0.78 db, $F_0 = -10.7$ db. Hence, the theoretical relation between envelope gain and magnification is given by

$$M = E - 10 \log_{10} E + 6 \text{ decibels}$$

This is plotted as the theoretical curve in Figure 4; the agreement between theory and experiment is quite satisfactory.

Appendix II. Envelope Modulation of 3,096-kc Pilot

Attention is here confined to single-frequency sine wave modulation of the pilot frequencies.

Symbols

A_a = amplitude modulation ratio of 2,064-kc pilot entering line
$A_a{}^{(n)}$ = amplitude modulation ratio of 2,064 kc leaving nth main station
A_b = amplitude modulation ratio of 3,096-kc pilot entering line
$A_b{}^{(n)}$ = amplitude modulation ratio of 3,096 kc leaving nth main station
R_a = voltage ratio corresponding to the envelope gain of the 2,064-kc regulators in one main section (that is, of six 2,064-kc regulators on the average)
R_b = voltage ratio corresponding to the envelope gain of a 3,096-kc regulator
n = number of main sections
K_a = normal level of 2,064-kc pilot, volts
K_b = normal level of 3,096-kc pilot, volts

Analysis

The maximum voltage at 2,064 kc entering the section is $K_a(1+A_a)$ and leaving the section is $K_a(1+R_aA_a)$. Hence the maximum "instantaneous" gain is

$$IG_a = 20 \log \frac{1+R_aA_a}{1+A_a} \text{ decibels}$$

There is a phase angle associated with the envelope gain and a more exact treatment would take this into account.

For small x, 20 log $(1+x) = 8.68x$, and $IG_a = 8.68\,A_a(R_a-1)$ decibels. Since variations in system equivalent in decibels follow a $\sqrt{f}$ law, at 3,096 kc the gain is

$$IG_b = \sqrt{\frac{3{,}096}{2{,}064}}\,(IG_a) = 1.225(IG_a) \text{ decibels}$$

If the 3,096 kc enters the section at a constant level of K_b volts, its maximum level at the end is

$$K_b[1+1.225A_a(R_a-1)] \text{ volts}$$

That is, the 3,096-kc pilot has an amplitude modulation at the end of the section, although it had none entering the section. On the basis of the usual definition of envelope gain, then, at 3,096 kc it is infinite; some modification is therefore necessary.

Suppose now the 3,096-kc pilot entering the section has an amplitude modulation A_b in phase with A_a, that is, its maximum level is $K_b(1+A_b)$. Then the corresponding level at the output of the section is $K_b(1+A_b)[1+1.225\,A_a(R_a-1)]$; neglecting the term in A_aA_b, the 3,096 kc at the output has an amplitude modulation of $[A_b+1.225\,A_b(R_a-1)]$.

So far no 3,096-kc regulator has been assumed. At a main station, the arrangements are as shown on Figure 16. We assume that the A equalizer has a fixed 40-db loss at 2,064 kc, regardless of its loss variations at 3,096 kc. At the input to the A equalizer, the 3,096-kc pilot has the amplitude modulation given above. The 3,096-kc regulator sees this modulation and tries to compensate for it, but because the A equalizer-regulator combination has envelope gain, instead it increases the amplitude by the factor R_b. At the input to the next line section, the amplitude variations are therefore

$$A_a{}^{(1)} = A_aR_a$$
$$A_b{}^{(1)} = R_b[A_b+1.225\,A_a(R_a-1)]$$

If subsequent sections are similar to the first, at the output of the nth section the modulations are

$$A_a{}^{(n)} = A_aR_a{}^n$$

$$A_b{}^{(n)} = R_b{}^n\left(A_b + 1.225\,A_a(R_a-1)\times\left[\frac{\left(\dfrac{R_a}{R_b}\right)^n - 1}{\dfrac{R_a}{R_b} - 1}\right]\right)$$

If R_a is larger than R_b, and if A_a is comparable with A_b (both suppositions are valid for the *L-1* system), then for n large enough, the above reduces to

$$A_b{}^{(n)} = 1.225\,A_a{}^{(n)}\,\frac{(R_a-1)R_b}{(R_a-R_b)} \text{ approximately}$$

For $R_a = 1.23$ (1.8 db) and $R_b = 1.035$ (0.3 db), $A_b{}^{(n)} = 1.5\,A_a{}^{(n)}$.

This gives the interesting result that (for more than about 12 sections for the assumed values of envelope gain) the modulation of the 3,096-kc pilot is only about 3.5 db greater than that of the 2,064 kc and that the input modulation on the 3,096 kc is of little importance.

If the envelope gain of the line regulators is considerably less than that of the office regulators, so that R_a and R_b are nearly the same, then the effect of A_b predominates.

This simplified analysis indicates that the transient performance at 3,096 kc will always tend to be worse than at 2,064 kc. However, conditions are likely to be more favorable at 64 and 556 kc because the $\sqrt{f}$ factor (1.225 for 3,096 kc) is less than unity for these frequencies.

References

1. Progress in Coaxial Telephone and Television Systems, L. G. Abraham. *AIEE Transactions*, volume 67, part II, 1948, pages 1520–27.

2. Network Analysis and Feedback Amplifier (book), H. W. Bode. D. Van Nostrand and Company, New York, N. Y., 1945.

3. Automatic Volume Control as a Feedback Problem, B. M. Oliver. *Proceedings*, Institute of Radio Engineers (New York, N. Y.) volume 36, 1948, pages 466–73.

4. Analysis of Systems with Known Transmission-Frequency Characteristics by Fourier Integrals, W. L. Sullivan. *Electrical Engineering*, volume 61, May 1942, pages 248–56.

5. Transients in Linear Systems (book), M. F. Gardner, J. L. Barnes. John Wiley and Sons, New York, N. Y., volume 1, 1942, chapter VI.

6. Hydrodynamics (book), Sir Horace Lamb. Sixth edition, Dover Publishing Company, New York, N. Y., 1945.

7. Frequency Analysis, Modulation and Noise (book), S. Goldman. D. Van Nostrand and Company, New York, N. Y., 1948, section 4.14.

8. Fourier Integrals for Practical Applications (book), G. A. Campbell, R. M. Foster. D. Van Nostrand and Company, New York, N. Y., 1942.

No Discussion

Equalization of Coaxial Lines

K. E. GOULD
ASSOCIATE AIEE

EQUALIZATION of coaxial lines has been discussed in a general way in a number of previous papers.[1,2,3] The present paper is intended to describe in somewhat more detail the equalization methods used in the Bell System's coaxial lines now in operation.

For telephone service, the requirements on equalization are fairly modest in terms of the capabilities of the present coaxial system. In the first place, the misalignment (that is, the variation in level from the nominal value) must not exceed a few decibels at any point in the system, throughout the transmission band, in order to avoid excessive noise because of low levels, or excessive modulation products because of high levels. Secondly, at the receiving end of each switching section (nominally 100 miles or so in length) the difference in level between working line and spare must not exceed perhaps 1/2 decibel over the band, in order that disturbances in individual channels be kept within tolerable limits at the time of manual switches made in the course of normal maintenance routines.

For television service, the equalization requirements are more severe, and are much less readily expressed in terms of simple transmission measurements. Misalignment is important, of course, because of its effect on signal-to-noise ratio and modulation, and similarity of working line and spare in each switching section must be maintained within reason. However, a much more serious problem is that of providing satisfactory equalization on long television circuits, both over-all and for intermediate dropping points.

For telephone service, delay distortion requirements for the broad-band system are extremely lenient and present no serious problem, but delay equalization must, of couse, be provided for television service. Delay distortion within the transmitted band arises primarily from gain cut-off at the ends of the band, and thus for the most part is of a broad nature. Nevertheless, the magnitudes are such that a high degree of precision is needed to meet television requirements.

Repeaters and Line

Auxiliary Repeaters

From an equalization standpoint, the performance of the line repeater (herein called the auxiliary repeater) is of paramount importance. The coaxial line itself provides a transmission medium which is uniform to a high degree and stable in its transmission characteristics. The principal variation is a predictable one due to temperature changes. Since the cable is essentially all underground, even these changes are relatively slow. Thus, if the repeater could be made to compensate the loss characteristics of the cable with reasonable precision, and the deviations from a flat gain characteristic for the combination of repeater and associated line, including the effects of all variables, were known to a high degree of precision, the gain equalization of the system would be straightforward.

The magnitudes of the quantities involved in gain equalization are, however, rather large, since the variation in cable loss across the frequency band employed is some 45 decibels per repeater section and the corresponding seasonal variation at the upper end of the band is as much as 5 decibels. In terms of a 4,000-mile circuit, these figures represent an equalization across the band of over 20,000 decibels and a seasonal correction of perhaps 2,500 decibels.

The auxiliary repeater for the present coaxial system is shown in block form for one direction of transmission by Figure 1. Since our interest here is primarily in transmission features of the repeater, the power supply is shown in rather incomplete form, for the sake of simplicity. At the input and at the output, next to the line itself, power separation filters isolate the 60-cycle power from the signal band, which extends roughly from 60 to 3,100 kc. These filters, incidentally, provide some gain equalization at the lower end of the band. Between the power separation filter and the input of the amplifier is an equalizer, known as the "basic equalizer," which compensates for a substantial part of the loss-frequency characteristic of the cable. At the input and at the output of the amplifier, a variable capacity is provided which is adjusted to offset approximately the variations in impedance seen by the amplifier due to connecting cable, splices, and so forth.

The circuit diagram of the auxiliary amplifier itself is shown in Figure 2. This is a 3-stage cathode-feed-back amplifier in which the beta circuit has been designed to perform two important functions: (1) compensation for a considerable part of the loss-frequency characteristic of the cable, and (2) variation of the gain-frequency characteristic in a manner closely matching the change in cable loss with temperature. Since this latter change is approximately equivalent to a change in length of the cable, this regulating network is used also to take care of minor variations in cable length. More substantial variations in length, which are inevitable in any system layout, are roughly compensated by the introduction of a line building-out network ahead of the basic equalizer.

Two vacuum tubes are used in parallel in each stage for the sake of improved reliability, the failure of a single tube in any stage resulting only in somewhat impaired performance, particularly as to modulation, but not to such a degree as to seriously affect the coaxial system. An

---

Paper 49-248, recommended by the AIEE Communications Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio October 17–21, 1949. Manuscript submitted July 15, 1949; made available for printing September 6, 1949.

K. E. Gould is with the Bell Telephone Laboratories, Inc., New York, N. Y.

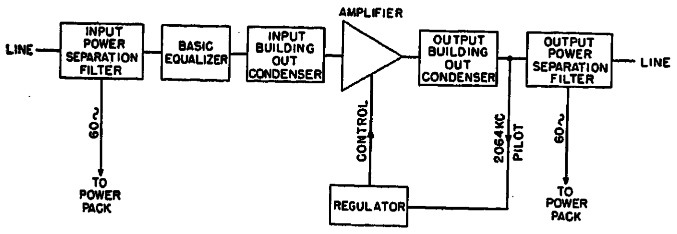

LINE
INPUT POWER SEPARATION FILTER
BASIC EQUALIZER
INPUT BUILDING OUT CONDENSER
AMPLIFIER
OUTPUT BUILDING OUT CONDENSER
OUTPUT POWER SEPARATION FILTER
LINE
60~
TO POWER PACK
CONTROL
REGULATOR
2064KC PILOT
60~
TO POWER PACK

Figure 1. Block diagram of auxiliary repeater

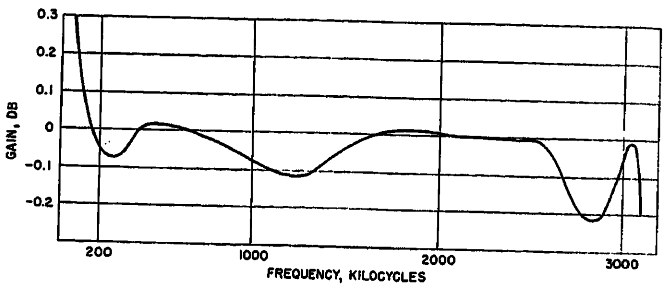


Figure 3. Gain of auxiliary amplifier section

additional cathode-feed-back network is incorporated in the output stage to improve the modulation performance of the high-level vacuum tubes.

The regulation of amplifier gain is accomplished by varying the impedance of the beta circuit, an indirectly-heated thermistor being incorporated for this purpose. At alternate repeaters, automatic regulation of gain is provided, which maintains the 2064-kc pilot at a prescribed power. This arrangement is shown in Figure 1. The behavior of this feed-back regulator system is described in a companion paper[4] and will be discussed here only insofar as it bears on the equalization problem.

The precision with which the over-all repeater gain should be made to match the cable loss is largely a question of economics. As long as the misalignment can be made small enough that signal-to-noise ratio and modulation performance are not jeopardized, it is obviously uneconomical to incorporate a large number of elements in the individual repeater to meet equalization requirements for a long system. A more economical arrangement would provide, at rather widely spaced intervals, equalizers to take care of accumulated gain deviations. Thus the gain of an "auxiliary repeater section" (auxiliary repeater combined with its associated length of cable) has been made only as flat as a modest number of circuit elements in the repeater will permit. The result is shown in Figure 3.

In manufacture and in the repair of the amplifier, the insertion gain is controlled to provide the auxiliary section gain of Figure 3 to within a few tenths of a decibel for individual amplifiers, with an average value for any considerable group of amplifiers within perhaps 0.05 decibel over-all of the transmitted band except the extreme upper frequencies, where the deviations may be two or three times as great. A high degree of manufacturing control must be maintained to obtain such performance, not only in the adjustment of the assembled amplifier, but also with regard to individual components, particularly in the input and output coupling networks. The gain deviations associated with changes in various elements must be known with considerable precision, not only in order to establish manufacturing requirements but also to guide the design of the adjustable features which are incorporated in the rather widely spaced gain equalizers mentioned previously. Figure 4 shows the amplifier gain-deviation characteristics for small changes in the most critical elements, leakage inductance of the input and output transformers, and inductance

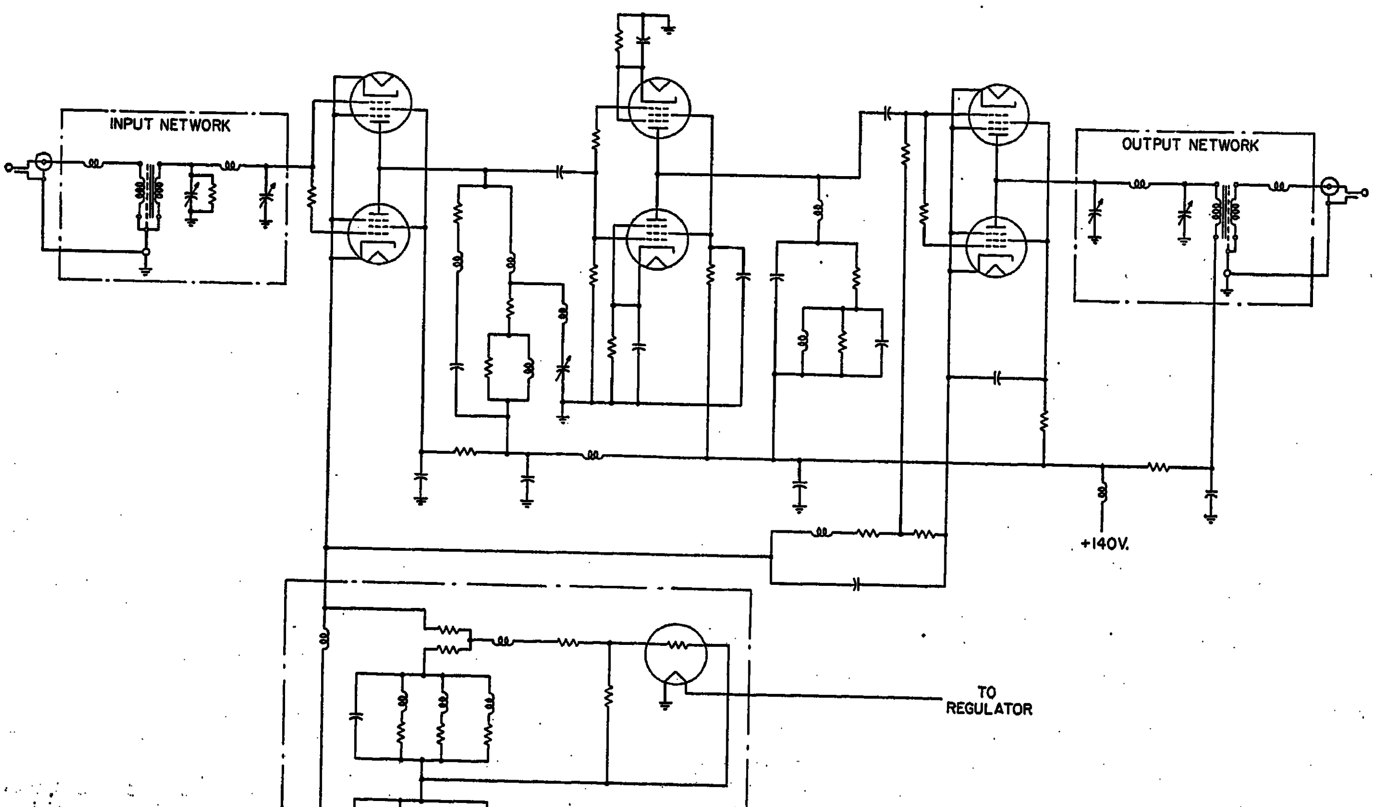


Figure 2. Schematic diagram of auxiliary amplifier

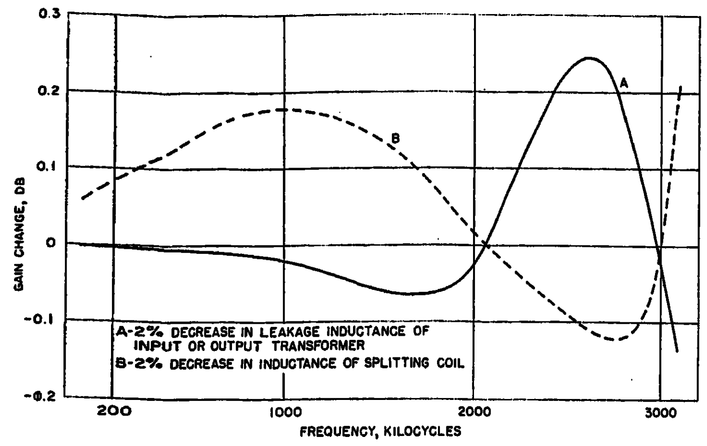


Figure 4. Effect of element deviations on amplifier gain

of the "splitting" coils in the input and output coupling networks.

For control of repeater insertion gain to the accuracy objectives adopted here, precise control of repeater panel impedances (that is, the impedances faced by the amplifier) is necessary. Figure 5 shows the effect on gain of 1 per cent changes in conductance and susceptance across the amplifier input and output respectively. For small changes, these effects are nearly independent, and represent gain-frequency characteristics which must be accommodated by the adjustable equalizers.

The general objective in establishing manufacturing control of the repeaters was to reduce uncertainties in the behavior of the repeater under operating conditions to the point where variations in circuit elements, made as small as good manufacturing practice will permit, are controlling. The reliability of repeater insertion gain as measured in the shop (or at amplifier maintenance points in the field) was checked by installing a group of carefully measured repeaters in a line and measuring the over-all transmission characteristics. This was done successively in various sections involving the several types of installed cable, and the degree of precision involved is probably best illustrated by the measures required to assure adequate accuracy with respect to cable loss as a function of frequency.

CABLE LOSS

There are four principal types of cable now in the plant: (1) rubber disk insulation, outer conductor 0.27-inch in diameter, (2) polyethylene disk insulation, outer conductor 0.27-inch in diameter, (3) similar to (2) but with outer conductor 0.375-inch in diameter, and (4) similar to (3) but slightly thicker outer conductor, 0.012 inch rather than the previous 0.010 inch. The last of these types is the present standard, and on the basis of appropriate choice of repeater spacing differences in performance of all but the rubber-disk type are very slight.

The most precise direct measurements of cable loss were made on the polyethylene-disk insulated cable with an outer conductor 0.375 inch in diameter and 0.010-inch thick. Even these measurements could profitably be smoothed by computing the loss as a function of frequency, using constants chosen to give a good general fit with the measured loss.

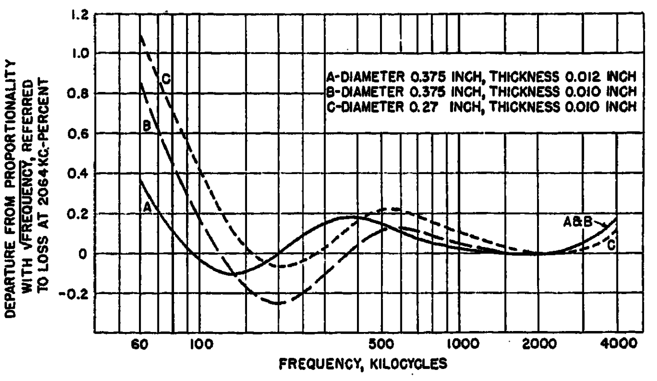


Figure 6. Cable loss—polyethylene disk insulation with various outer conductors

The result is shown in Figure 6 as departure from proportionality with the square root of the frequency. The refinement of these computations, which are due to J. P. Kinzer, is illustrated by the fact that it was necessary to consider the inner conductor as a soft copper core surrounded by a 0.00045-inch shell with a resistivity 4 per cent higher than that of the core, an hypothesis reasonably consistent with the method used in making the inner conductor.

Differences in loss due to changing the cable structure from that considered above to either of the other two with polyethylene disks can be computed with considerable precision, and Figure 6 shows the loss for these other two types of cable. It might be mentioned that the effect of temperature variation on cable loss can also be computed with precision, and correlated with measured temperature coefficients, to provide a high degree of accuracy in determining regulation error for the system. With regard to the loss of rubber-disk insulated cable, which is installed in present plant only in a few

Figure 5. Change in gain due to one per cent increase in admittances facing auxiliary amplifier

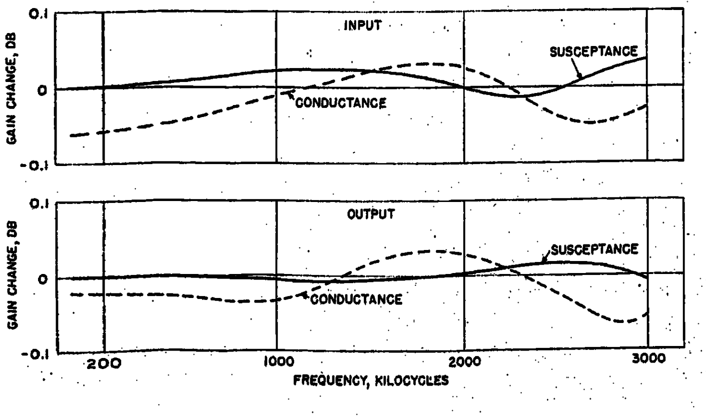
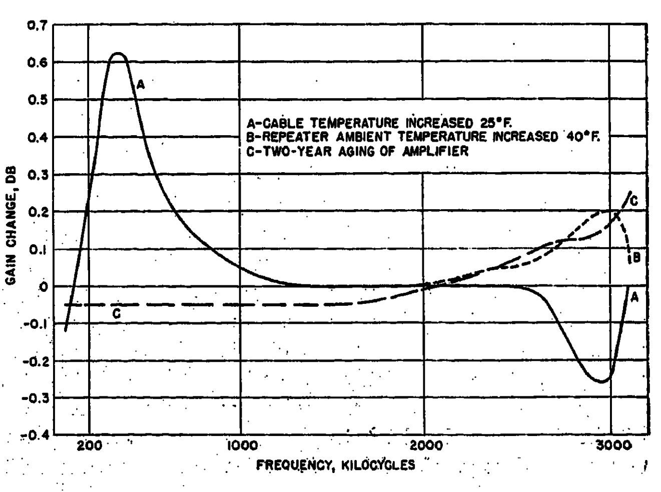


Figure 7. Change in gain of auxiliary repeater section due to temperature effects and aging



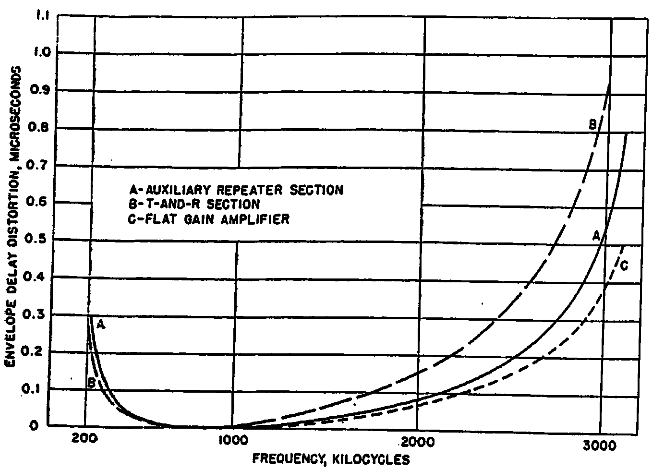


Figure 8. Envelope delay distortion

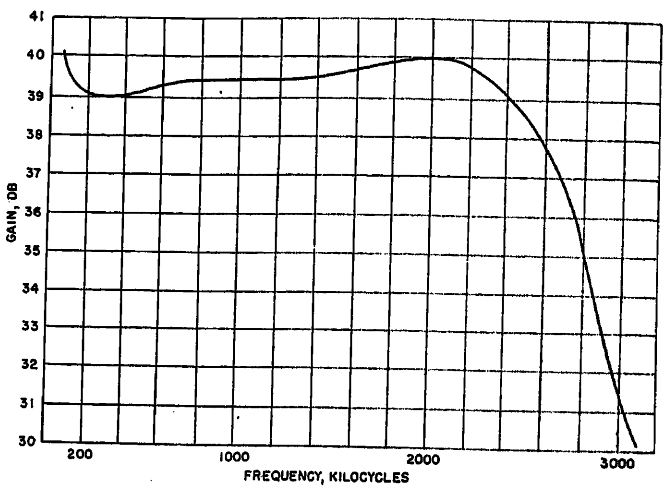


Figure 9. T-and-R section gain

sections, the loss is sufficiently different from that of cable with polyethylene insulation that even the basic equalizer must be different to avoid serious misalignment.

SYSTEMATIC DEVIATIONS

There are three principal sources for systematic deviations in auxiliary repeater section gain under operating conditions: (1) regulation error, (2) aging of vacuum tubes, and (3) repeater temperature effects. These gain changes must, of course, be taken care of by means of the adjustable equalizer, but since these changes occur slowly, they are readily handled by routine procedures. Regulation error is due to the lack of precise matching of the beta circuit gain control to change in cable loss. Aging of vacuum tubes results generally in a gradual reduction in transconductance, which in turn affects the gain most seriously where the feedback is lowest, that is, toward the upper end of the useful band. Repeater temperature effects, while a composite of many factors, are reasonably systematic. Figure 7 shows curves of these three effects for an auxiliary repeater section.

DELAY CHARACTERISTICS

The delay characteristics of the amplifier are controlled only insofar as the gain is controlled, and in this regard the behavior of the amplifier outside the transmitted band is, of course, important. The repeater with its associated line is essentially minimum phase in its behavior except for a constant delay, so that gain equalization with minimum phase networks anywhere in the over-all circuit will compensate the delay distortion arising from repeater gain deviations within the transmitted band. As mentioned previously, the delay distortion appearing in the transmitted band due to the gain characteristic outside the band, is quite broad. The envelope delay distortion for one auxiliary repeater section is shown in Figure 8. A companion paper[5] describes the method of analysis employed to match such a delay characteristic with an equalizer having an essentially constant loss over the entire transmitted band.

OFFICE REPEATERS

At spacings of perhaps 50 to 100 miles, gain equalization must be provided as mentioned previously, to restore the levels across the transmitted band. In highly built-up areas, the spacing may be dictated by the need for dropping circuits, but in other areas, equalization may be provided even though no circuits are dropped. This equalization may be a part of a main station, where power is supplied to the coaxial system, whether switching facilities are provided or not, or may appear in an equalizing auxiliary station, which replaces an ordinary auxiliary station in a long power feed section requiring additional equalization. Figure 2 of reference 3 shows these arrangements except for the equalizing auxiliary station, which is essentially the same as a nonswitching main station as far as the transmission circuits are concerned.

In any case, the amplifiers at this point provide an impedance match with the associated "office" equipment, such as equalizers, dropping-point filters, and so forth. Thus the transmitting amplifier, which is connected to the sending end of the coaxial cable, has an output circuit similar to that of the auxiliary amplifier, but an input circuit which presents an impedance of approximately 75 ohms over the useful band. Similarly, the input of the receiving amplifier is like that of the auxiliary amplifier, but its output impedance is 75 ohms. The receiving amplifier includes a variable beta circuit like that in the auxiliary amplifier, so that variations in loss of the cable associated with the combination of transmitting and receiving amplifiers may be compensated.

It is convenient to consider this combination of transmitting amplifier, receiving amplifier, and associated section of coaxial cable as a unit, and this has been designated a T-and-R section. Figure 9 shows the gain of a T-and-R section. The droop in this gain characteristic at the upper end of the frequency range is an incidental result of level allocation in the system for optimum modulation performance, and is not important from the standpoint of the present paper. In brief, it is more economical to incorporate the equalization of this characteristic in the *A* equalizer, which will be discussed later, than to design the receiving repeater with the additional gain at the upper frequencies.

To offset the losses of such equipment as the *A* equalizer, one or more flat-gain amplifiers are provided. These amplifiers have a gain of approximately 40 decibels, and contribute but little to the gain equalization problem. They do introduce appreciable delay distortion, which must be included in the requirements for delay equalizers. Figure 8 shows the envelope delay distortion of a T-and-R section and of a flat-gain amplifier, in addition to the auxiliary-section characteristic discussed previously.

Gain Equalization

A EQUALIZER

This designation has been applied to the gain equalizer referred to before, which is always required following a receiving amplifier, to equalize a T-and-R section.

:hus an *A* equalizer is always available o adjust the gain over the band at any erminal, dropping point, or switching oint. Reference 5 shows the gain djustment provided by the *A* equalizer. s implied earlier, these adjustments ere designed to compensate the principal known gain deviations and in addition ɔ provide a degree of flexibility to take are of a wide variety of minor deviations ith moderate precision.

The function of the *A* equalizer is wofold. In the first place it provides eans for manually adjusting the over-all ain of the system throughout the useful and. In addition, three automatically gulated loss-frequency characteristics e provided to reduce the gain deviations ising in the course of time, thus peritting less frequent manual realignment. hese automatic controls are in some spects similar to the regulation facilies, described earlier, for equalizing e major change in the system, variation cable loss with temperature. In each se a pilot signal is employed, the level this pilot being controlled by a regular. In the *A* equalizer, thermistors are pplied with heater current from regulars operating from pilots at frequencies 64, 556 and 3,096 kc, respectively. hese thermistors produce loss-frequency aracteristics, as described in reference with a maximum in each case in the cinity of the pilot frequency. For levision transmission, the 556-kc pilot ntrol is particularly important since it nds to stabilize the level of the 311-kc levision carrier.[6]

For telephone service, the *A* equalizer ovides adequate performance for all t the very longest spans between terinals, that is, between points at which channels are brought down to voice quencies.[7] For television service, it is nerally necessary to provide additional ualization, which will be described in e following section.

EQUALIZER

As mentioned in the preceding section, ere the length of system between terinals exceeds that which the *A* equalizer capable of adjusting adequately, or ere television service is to be proied, supplementary adjustment is reired and is provided by a unit known as e *B* equalizer. Reference 5 shows the s-frequency shapes provided in this it. These shapes are largely complentary to those incorporated in the equalizer, and in combination the two ualizers provide a rather powerful tool adjusting the gain throughout the tire band.

The usefulness of such devices obviously depends upon the availability of adequate measuring equipment and test procedures. In general, a schedule of point-by-point gain measurements has been followed, and this has proved fairly adequate, although slower than would be desired. Automatic recording of the gain-frequency characteristic is being incorporated in test equipment now under development, and visual indication of the gain-frequency characteristic on a cathode-ray tube has been used successfully on an experimental basis.

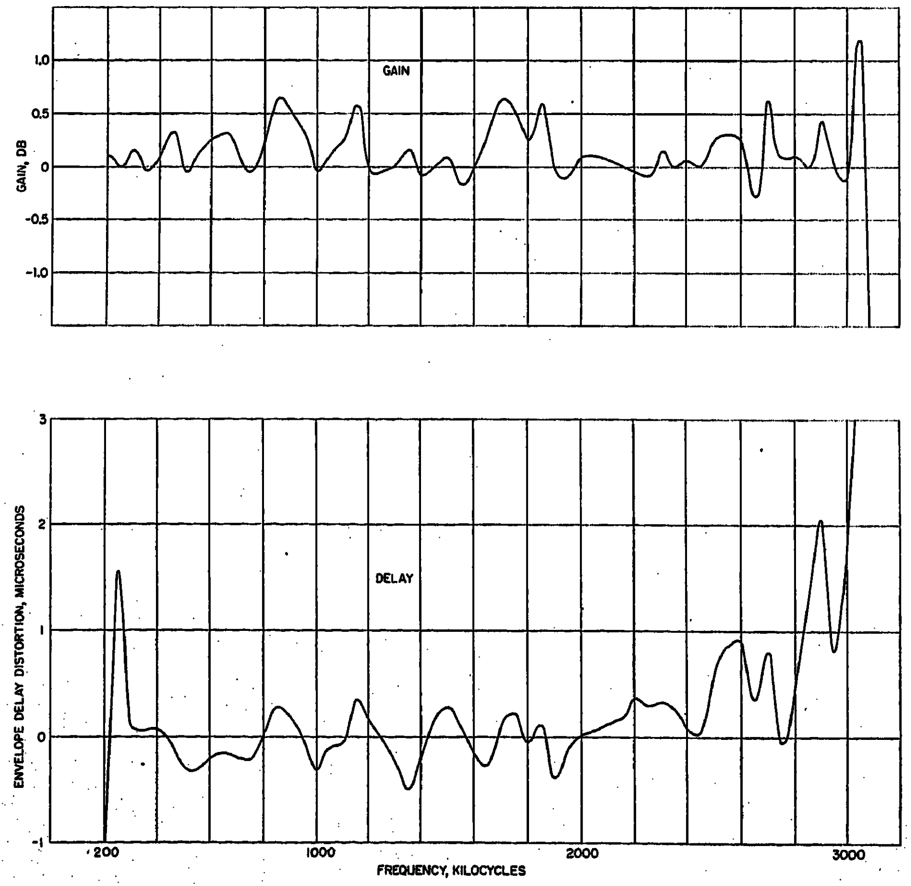
Delay Equalization

The envelope delay distortion of an auxiliary repeater section, of a T-and-R section and of a flat-gain amplifier are shown in Figure 8. A representative television link, say 100 miles in length, might include 12 auxiliary repeater sections, one T-and-R section, and two flat-gain amplifiers. Thus the envelope delay distortion for this section would be perhaps 4 microseconds at the lower end of the television band[6] and some 13 microseconds at the upper end. Delay equalization must, of course, be provided by increasing the delay at other parts of the band to the system delay at the upper edge of the band, the maximum correction thus being of the order of 13 microseconds per 100 miles.

The bulk of the delay equalization required for a length of the order of a 100 miles is provided at the transmitting end, this being a convenient and economical choice from the standpoint of accommodating the loss of the delay equalizers. A certain amount of adjustable delay equalization is provided, to take care of variations in delay due to such things as temperature effects, vacuum tube aging, and deviations in amplifier performance, the adjustable units being placed at the receiving end for ease of establishing the correct settings. Since adjustable delay is more easily provided in this form, the variable units have delay characteristics roughly like those of a section of line. The receiving end also includes some smaller delay equalizers, particularly those associated with dropping point equipment and thus needed at only one point in any over-all television circuit.

Since the system layout varies from place to place, so that various numbers of repeater sections and so forth must be accommodated, considerable flexibility is required in the delay equalizers available. This has been accomplished by

Figure 10. Transmission characteristics—Washington-Chicago circuit



providing a series of "building blocks" of delay equalization beginning with a large unit which will equalize a representative long section between television dropping points and progressing through successively smaller units until the residual interpolation can be made with adjustable units. With this approach the latter type of phase equalizer thus performs a dual purpose, and reduces the number of types of fixed units required. Since there are moderate variations in the proportions of auxiliary repeater sections, T-and-R sections and flat gain amplifiers, for each of which the delay distortion characteristic is different, additional flexibility is required in delay equalizers beyond simply a scale-factor change. This is accomplished by incorporating minor changes in the delay-frequency characteristics of some of the smaller units, and more particularly by providing supplementary units which bridge the gaps between the various system layouts, that is, between the various extremes in proportions of the several components which contribute to delay distortion. The design features incorporated in the delay equalizers are described in reference 5.

As mentioned before, adjustable delay equalization has been provided not only to ease the problem of accommodating various system layouts but also to take care of gradual changes in the system. As described in reference 5, adjustments may be made independently at each end of the band. So far, a single shape has been provided on keys, with merely an adjustable scale factor, but there is no reason why a more flexible arrangement could not be adopted if this should be needed.

As in the case of gain equalization, adjustments must be based on transmission measurements, and this calls for adequate test equipment. Two types of delay measuring equipment are being made available, one involving point-by-point measurements of envelope delay distortion with a rather wide frequency interval and the other automatic recording simultaneously with the gain, using a narrow interval for measuring envelope delay distortion. Further experience with television transmission on coaxial systems will be required before a sound decision can be made on the choice of delay measuring equipment for this system.

System Performance

As an example of coaxial system equalization for television transmission, Figure 10 has been prepared showing the measured gain and envelope delay distortion for a circuit from Washington to Chicago. This is representative of the performance presently obtainable with these facilities, although perhaps not as good as that which might be realized from an installation incorporating recent improvements introduced specifically because of television requirements.

References

1. Television Transmission Over Wire Lines, M. E. Strieby, J. F. Wentz. *Bell System Technical Journal* (New York, N. Y.) volume XX, January 1941, pages 62-81.

2. Television Network Facilities, L. G. Abraham, H. I. Romnes. *AIEE Transactions*, volume 66, 1947, pages 541-48.

3. Progress in Coaxial Telephone and Television Systems, L. G. Abraham. *AIEE Transactions*, volume 67, part II, 1948, pages 1520-27.

4. Stability of Tandem Regulators in the L-1 Carrier System, J. P. Kinzer. *AIEE Transactions*, volume 68, part II, 1949, pages 1179-86.

5. Attenuation and Delay Equalizers for Coaxial Lines, W. R. Lundry. *AIEE Transactions*, volume 68, part II, 1949, pages 1174-79.

6. Television Terminals for Coaxial Systems L. W. Morrison, Jr. *AIEE Transactions*, volume 68, part II, 1949, pages 1193-99.

7. Frequency Division Techniques for a Coaxial Cable Network, R. E. Crane, J. T. Dixon, G. H. Huber. *AIEE Transactions*, volume 66, 1947, pages 1451-59.

No Discussion

Television Terminals for Coaxial Systems

L. W. MORRISON, JR.
NONMEMBER AIEE

THE broad features of operation of the *L-1* coaxial system for the transmission of television have been discussed in a recent paper.[1] It is the purpose of this paper to describe in somewhat more detail the factors influencing the design of the coaxial television terminals and the features of the equipment now in service in the Bell Systems' television network. The television terminals here described were placed into network service in 1947, but in basic form are similar to experimental models developed prior to the war and used in early television transmission studies over the coaxial cable.[2,3]

Frequency Allocation Considerations

The nominal transmission band of the *L-1* coaxial line extends from 64 to 3,100 kc. In contrast to this available band, the television video signal nominally occupies the frequency region extending from a few cycles per second upwards to 4 megacycles. It is the function of the television terminals to properly translate this video band of frequencies upward into the transmission region of the coaxial line and, after transmission over the system, to restore the television signal to its original video form for use by the broadcaster.

In addition to the necessity for frequency translation of the video signal, it is apparent that the over-all available band width for television transmission is somewhat less than 3 megacycles. Numerous experiments have been conducted with a view toward evaluating the subjective loss of sharpness of a television image with restricted transmission bands.[4] It is sufficient to say here that a definite visible effect requires a change in band width somewhat in excess of 1 megacycle in a 4-megacycle system, which is the increment we are here concerned with. The practical operation of the *L-1* coaxial system for television service during the past several years appears to confirm this conclusion.

The relative positions of the transmission band of the *L-1* coaxial system and the original video signal requires that frequency translation methods be employed. To preserve the greatest possible net band width it is apparent that double side-band transmission cannot be considered here. Single side-band transmission, however, involves great difficulties in achievement in a case such as this where the signal contains energy at frequencies of the order of a few cycles per second.

The vestigial side-band transmission scheme here selected permits a relatively efficient utilization of the available band width and is practically realizable with presently available design techniques. The cost of frequency discrimination networks required to limit the extent of the vestigial sideband is inversely related to the vestigial band width. Certain transmission distortions increase inherently as the vestigial side-band width is reduced. These two factors, which will be discussed in more detail later, must be balanced against the desirability of utilizing the available band width to the fullest extent to achieve optimum transmission performance consistent with economic considerations. A vestigial-to-main side-band width ratio of approximately 4 per cent is here selected as representing a satisfactory condition for the *L-1* coaxial system.

The delay distortion of a long coaxial system tends to increase rapidly at frequencies approaching the lower cut-off. While this is unimportant for telephone application, for television use precise delay equalization over the entire band is required. A study of the *L-1* coaxial system indicates that in consideration of the extent of delay equalization required for television a lower practical limit of 200 kc is advisable. This and the preceding considerations result in a main side-band width of approximately 2,800 kc with the vestigial sideband occupying approximately 100 kc.

The repeaters for the *L-1* coaxial line involve feed-back amplifiers whose stability and modulation performance are allowed to decrease for frequencies exceeding 2 megacycles. Also, the noise tends to increase above 2 megacycles because of the higher coaxial cable loss. Since, in general, television transmission requirements are more lenient at the higher video frequencies, it is logical that the carrier be located at the lower end of the frequency band with the main sideband extending upward to 3,100 kc. These considerations then place the optimum location of the television carrier frequency in the region of 300 kc with the vestigial sideband extending downward to 200 kc and the main sideband upward to 3,100 kc. The choice of exact location of the television signal in the available *L-1* coaxial band is made to take greatest advantage of certain characteristics of the television

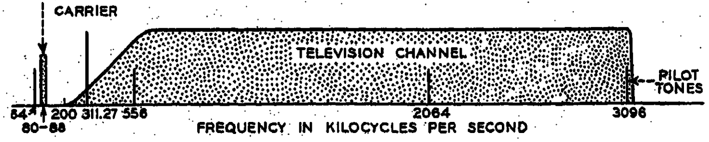


Figure 1. Television allocation for *L-1* coaxial system

Paper 49-249, recommended by the AIEE Communication Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 19, 1949; made available for printing September 6, 1949.

L. W. MORRISON, JR., is with the Bell Telephone Laboratories, Inc., New York, N. Y.

The design and development of the television terminals for the *L-1* coaxial system represents the contributions and efforts of a considerable number of individuals at the Bell Telephone Laboratories. The author has been privileged to represent them in this presentation.

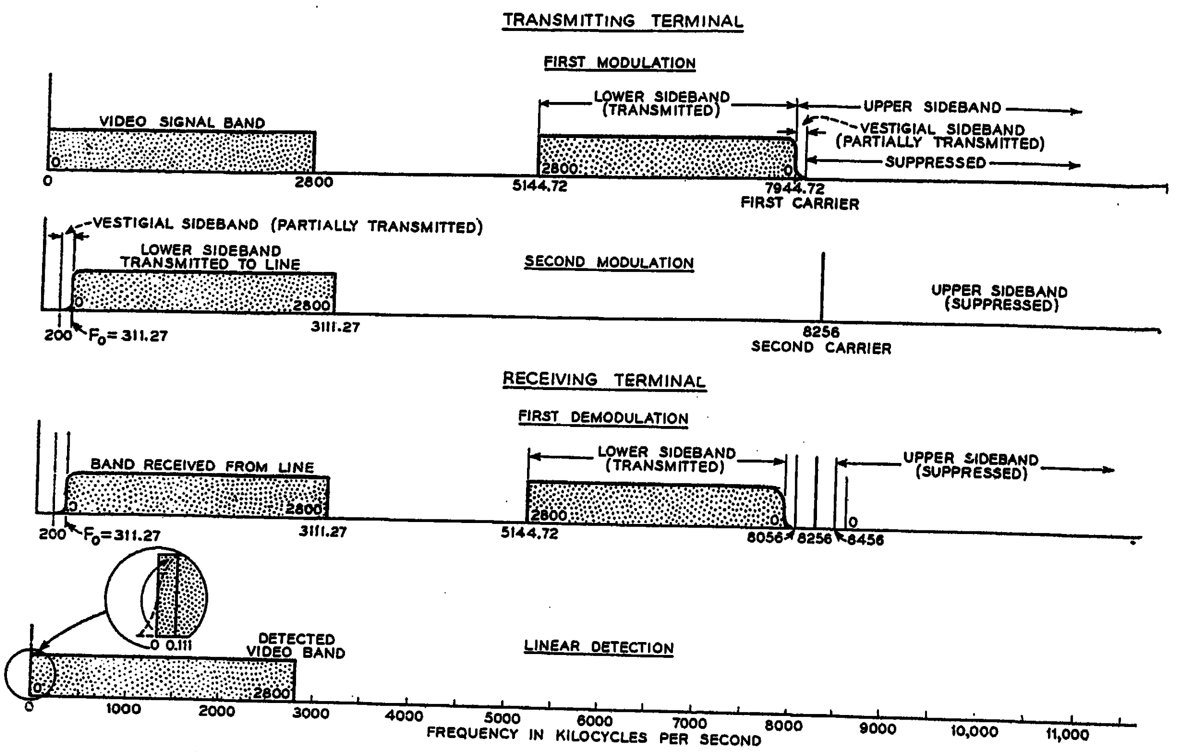


Figure 2. Modulation diagram for television carrier terminals

signal, the transmission properties of the coaxial line, and to permit simplification of operating and maintenance practices.

As described in a previous paper,[5] the television signal produced by scanning is composed of concentrations of energy located in frequency regions related to the line and field scanning frequencies and harmonics thereof. The alternate regions of low energy content may be considered as related to specific forms of complex detail rarely present in television scenes. In a converse fashion, the introduction of extraneous energy into these idle regions has been found to produce complex visual effects of which, in general, the viewer is very tolerant. In the television terminal designed for the *L-1* coaxial system, this phenomenon is utilized where possible by locating the exact position of the transmitted television band so that existing unavoidable interferences fall within these regions of comparative insensitivity. For example, three of the pilot frequencies employed for gain equalization purposes are located at 556, 2,064, and 3,096 kc.[6] The effective interference produced by these control tones with the transmitted television carrier signal is reduced in this manner.

Another characteristic of the energy-frequency distribution of the television signal which is of importance here is the relatively high concentration of energy at the lower video frequencies and the usual diminution at the higher frequencies. It is possible to encounter variations in this characteristic when unusual specific scenes or test patterns are considered, but in the large majority of cases a considerable reduction of energy with frequency is the rule. By means of predistorting methods this broad energy-frequency characteristic is altered for transmission over the coaxial line resulting in improved transmission performance as discussed in more detail later.

Another factor which influences the exact position of the transmitted television signal on the coaxial line is the practical problem of maintenance of these precise carrier frequencies over a nationwide network. In the terminal design which will be described later, the highly stable coaxial line pilot frequencies which are always available are used as frequency reference standards in maintenance of this equipment.

The exact location of the modulated

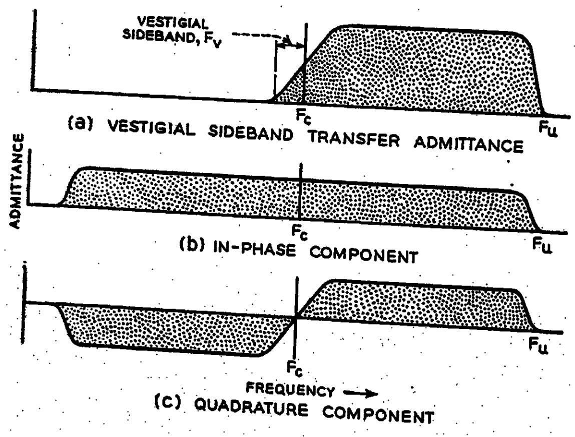


Figure 3. Transfer admittance of a vestigial sideband carrier system

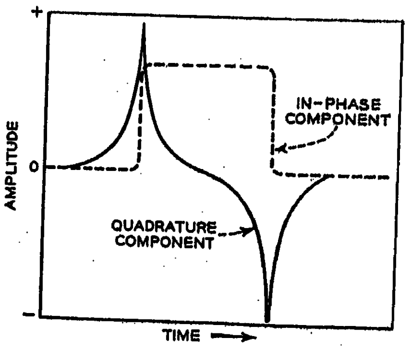


Figure 4. In-phase and quadrature components of a rectangular pulse

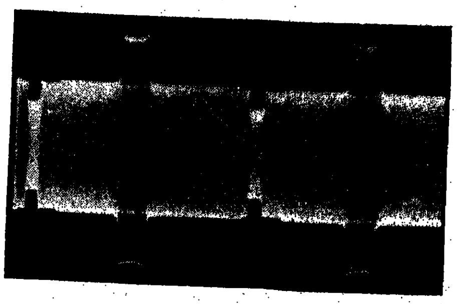


Figure 5. Modulated television carrier signal prior to detection

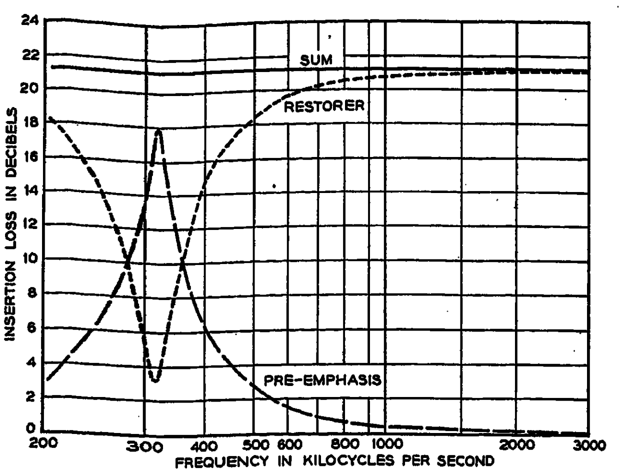


Figure 6 (left). Pre-emphasis and restorer characteristics

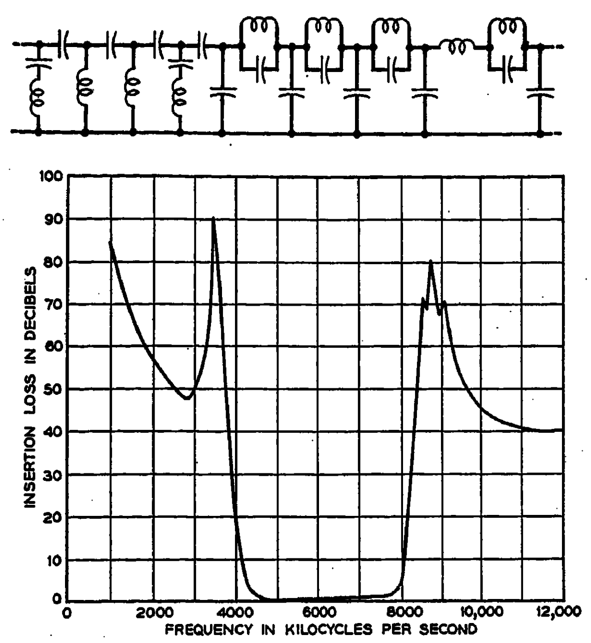


Figure 9 (right). Band-pass filter—configuration and loss characteristics

television carrier signal as transmitted over the *L-1* coaxial system is shown in Figure 1. The television carrier is located at 311.27 kc. Provision also is made for transmission of the accompanying sound program by single side-band carrier means in the region of 80–88 kc. The equipment and methods employed here have been described recently.[7]

The Terminal Modulation Process

To achieve the translation of a band of frequencies by an amount small as compared to the band width, multiple modulation steps must be employed. Figure 2 illustrates the features of the double modulation process employed here.

The original video signal extending from a few cycles per second upwards to 3 megacycles is modulated with a carrier frequency of 7.944 megacycles. The resultant lower sideband together with a vestige of the upper sideband as selected by the following band filter is then modulated again, this time with a carrier located at 8.256 megacycles. The lower sideband of this second modulation step is selected by a following low-pass filter and now lies between 200 and 3,100 kc with the original zero video frequency now located at 311.27 kc. This frequency position is the one desired for transmission over the coaxial line. It should be noted that the vestigial shaping process is only partially completed in the transmitting terminal, the remainder of this process being provided within the receiving terminal.

After transmission over the *L-1* coaxial line, a similar double-demodulation process is employed to restore the signal to its original video form. The incoming carrier signal at the receiving terminal first is modulated with a carrier frequency of 8.256 kc. Following this, a band filter similar to that in the transmitting terminal selects the lower sideband and introduces the remainder of the vestigial side-band shaping. The resulting television carrier signal now falls in the region of 5 to 8 megacycles and at this point envelope detection is employed to recover the video signal for transmission to the broadcaster.

The choice of final demodulation by envelope detection rather than through the use of a local carrier demodulator results in a considerable simplification of the terminal equipment. For satisfactory operation of product demodulation in a television system such as this, it has been found necessary to maintain the relative phase between the incoming carrier of the television signal and the local demodulating carrier to within one degree for satisfactory transmission performance. The realization of such phase precision throughout a nation-wide network would require extremely costly and complex terminal equipment. The satisfactory operation of a carrier television system which employs envelope detection, however, necessitates certain restrictions as to the form of the modulated signal envelope. However, in this system it was considered advantageous to accept these restrictions and employ envelope detection.

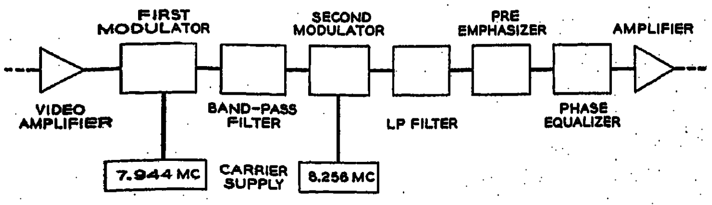
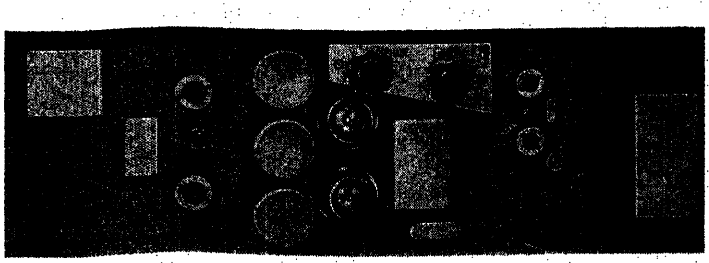
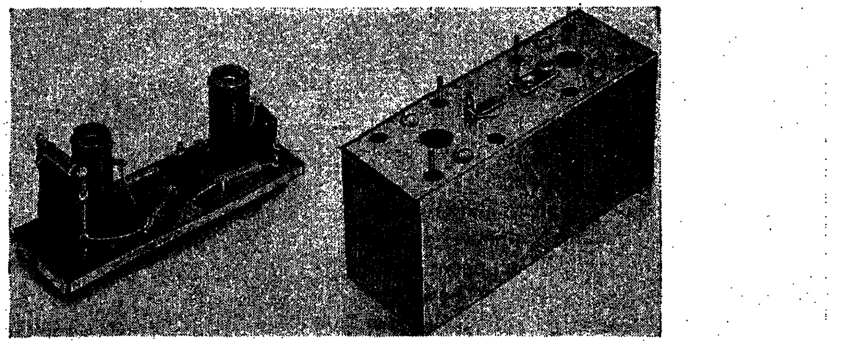


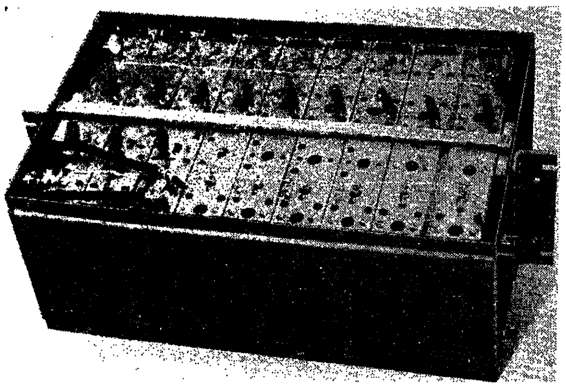
Figure 7 (left). *L-1* coaxial television transmitting terminal—block schematic

Figure 8 (below). First modulator—equipment layout

Figure 10 (right). Typical equalizer section—mechanical arrangement







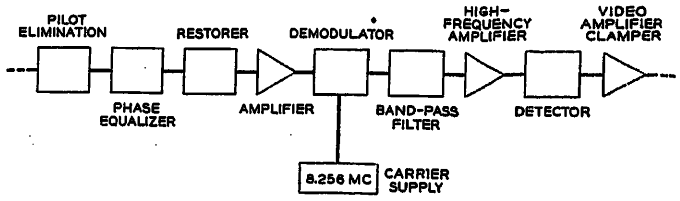


Figure 11 (left). Carrier terminal network assembly—mechanical arrangement

Figure 13 (right). *L-1* coaxial television receiving terminal—block schematic

Television Carrier Transmission Considerations

It is convenient in a discussion of vestigial side-band methods of carrier transmission to consider the resultant transmission effects as made up of contributions from two components, an in-phase or real component and a quadrature or distortion component. This method of analysis was described originally by Nyquist.[8] The process is illustrated in Figure 3. Here the transfer admittance shown in (A) represents the idealized conditions for vestigial side-band transmission. The main side band is shown extending from the carrier frequency location F_c upwards to F_u, the cut-off of the system. A vestige of the lower sideband extends from F_c downward as shown. In the region $F_c \pm F_t$ the admittance characteristic is so chosen that the sum of the admittances offered to any upper side-band frequency and the corresponding lower side-band frequency is a constant. In this idealized view a linear phase with frequency characteristic over the entire frequency band, that is, constant envelope delay, is assumed.

The vestigial side-band system admittance function shown in Figure 3 (A) may be considered to be equivalent to the sum of two other admittance forms illustrated in (B) and (C). Both of the latter functions are of a double side-band form and may be treated as such. The admittance component shown in (B) represents a double side-band characteristic, where the carrier is in phase with the original signal and is capable of faithful reproduction of the original signal except for an amplitude constant. The second component shown in (C) represents a double side-band characteristic, where the carrier is in quadrature with that of (B) and where each signal component also has undergone a 90-degree phase shift from that of Figure 3(B). This component is referred to as the quadrature component and represents a distortion term. The complete vestigial characteristic is formed by the vector addition of the in-phase and quadrature components. Figure 4 illustrates the form of the in-phase and quadrature component of an idealized rectangular video wave form as obtained with the admittance functions of Figure 3. The form of the quadrature component is a function of the band width of the vestigial sideband, and its peak amplitude is inversely related to this band width. The contribution of this distortion term to the resultant output of a vestigial transmission system can be made as small as desired by providing an excess of in-phase carrier, supplied by either local generators or contained in the transmitted carrier signal.

As discussed previously, the *L-1* television terminals employ envelope detection and here it is required to transmit an excess of in-phase carrier, that is, restrict the degree of modulation to maintain the quadrature distortion term to desired limits. Figure 5 illustrates the appearance of a television carrier signal of a rectangular video pulse within the *L-1* terminals just before final detection. The quadrature effects are visible here in the envelope.

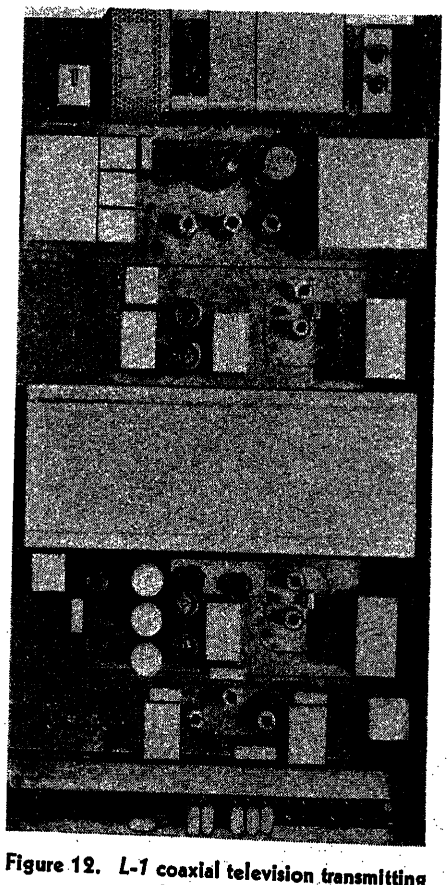
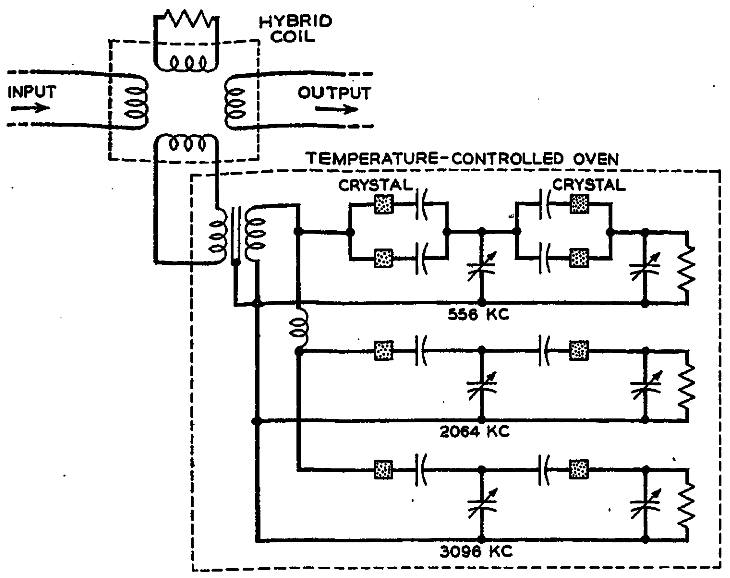


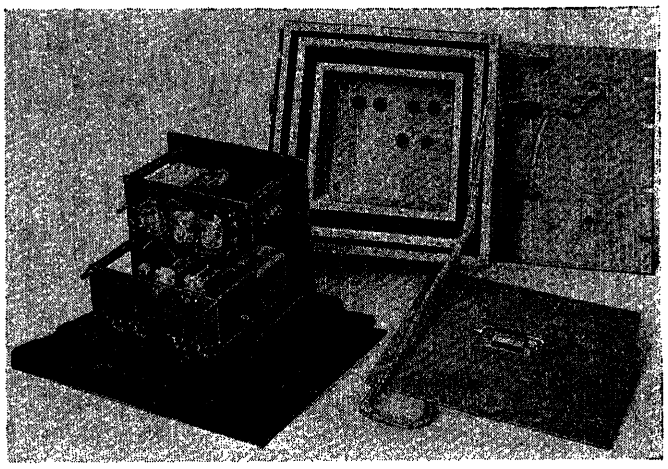
Figure 12. *L-1* coaxial television transmitting terminal—bay arrangement

The signal-to-noise performance of a carrier transmission system is directly proportional to the modulated amplitude of the transmitted signal. Given an upper peak amplitude limit which may be transmitted satisfactorily over such a system, it follows that an increase in the excess carrier ratio must be accompanied by a reduction in the signal modulation with a resultant poorer signal-to-noise performance. The excess carrier condition shown in Figure 5 is employed presently in the *L-1* system and represents an economical balance between quadrature distortion and signal-to-noise performance.

It is of interest to consider the resultant effects of unwanted modulation distortion of the *L-1* carrier television signal introduced during transmission over the coaxial line amplifiers. In the case of transmission of a television signal in the original video form, all important energy components of the signal are harmonically related to the scanning frequencies. Under these conditions, the introduction of modulation distortions also of a harmonic nature results only in a modification of the amplitude and phase of the original signal components and the visual resultant is a distortion of the signal wave shape in a geometric sense. In the usual video case these effects are considered as a nonlinearity function applied to portions of the brightness range of the received picture. The eye is quite tolerant in this respect so that quite lenient video signal modulation requirements result.

In the case of carrier transmission of television signals, the regions of concentrated energy are not harmonically related by virtue of their displaced frequency position. Harmonic modulation products of this carrier signal are no longer simply related to the original signal components and extraneous frequency components are produced. As might be expected, the modulation requirements





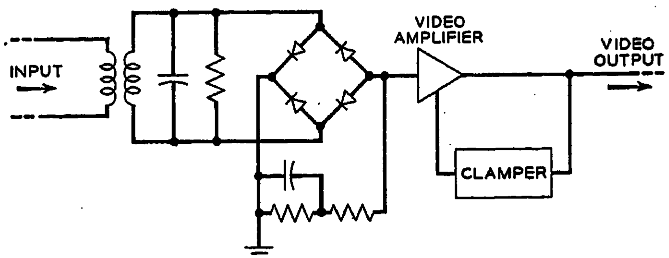


Figure 14 (above). Pilot elimination filter—simplified schematic

Figure 15 (top right). Pilot elimination filter—details of construction

Figure 16 (right). Envelope detector and video amplifier-clamper—simplified schematic

under these conditions of carrier transmission are considerably more severe than in the case of the video form of transmission.

In the present system the greatest amplitude modulation products are of a second and third harmonic nature and are formed by the carrier frequency and the high energy signal components in the immediate vicinity of the carrier frequency. The second harmonic of the carrier frequency will fall at 622 kc and the third harmonic at 933 kc. After final detection these extraneous distortion products will appear in the 311 and 622 kc regions respectively, where they produce extremely objectionable visual effects. There is, however, an advantage obtained in arrangement of the allocation as mentioned previously such that these products occur in the idle regions of the signal spectrum.

Pre-emphasis of the upper frequency components of the television signal is employed according to the frequency characteristic shown in Figure 6. By this means it is possible to introduce a complementary restoring characteristic as indicated in the receiving terminal which effectively reduces any extraneous interference, including modulation effects, above 400 or 500 kc on the coaxial line. Since the restorer characteristic is the exact complement of the pre-emphasis characteristic, the over-all transmission characteristic is unaffected. In this manner a theoretical reduction of 16 decibels of the second harmonic and 17 decibels of the third harmonic is effected. It should be noted here that in the process of pre-emphasis, the peak factor of the carrier television signal is altered and therefore the extent of modulation improvement indicated is not completely realized in practice.

Transmitting Terminal Equipment

A block schematic of the elements of the *L-1* coaxial television transmitting terminal is shown in Figure 7. This terminal provides for the translation of the video signal upward into the transmission band of the *L-1* coaxial line. The double-modulation method has been shown previously in Figure 2.

The video signal to be transmitted over the *L-1* coaxial system is received from the network operating center over a balanced 110-ohm video transmission circuit. The first modulator unit consists of a video amplifier, d-c inserter, modulator, and associated carrier supply.

The d-c inserter consists of a germanium crystal element connected so that, in conjunction with the coupling capacitors, the extremes of the standard television synchronizing pulses are always maintained at zero potential with respect to the modulator grids regardless of the picture content of the video signal. In this manner the peaks of the synchronizing pulses correspond to a fixed amplitude of excess carrier which, as discussed previously, is desirable when envelope detection is employed.

The video signal, which now contains the d-c component, is impressed upon the grids of a balanced modulator while the carrier voltage is introduced in a conjugate manner into the cathode circuit. The excess carrier to be transmitted is provided by unbalancing the modulator by differentially varying the screen voltage of each modulator tube by the desired amount. To assure that this excess carrier component is in exact phase relation with that modulated by the input video signal, an additional phase or quadrature balance control is provided. This circuit arrangement allows any carrier leak which may be present in the modulator plate circuit, due to transmission through the tube elements or other parasitic reactances, to be independently balanced out.

The carrier frequency of 7.944 megacycles is generated in a crystal-controlled oscillator and provides, through a buffer amplifier stage, approximately 5 volts of carrier to the modulator proper.

Figure 8 is a first modulator unit as mounted on the transmitting terminal equipment bay. The video amplifier, d-c inserter, modulator, and carrier supply oscillator are included on this panel. The carrier balance controls may be noted.

Figure 9 indicates the network configuration and the frequency-loss characteristic of the band filter which selects the lower and vestigial sideband of the first modulation step. This network is formed of low-pass and high-pass sections. The particular configuration here results in an economy of coils, desirable because of the

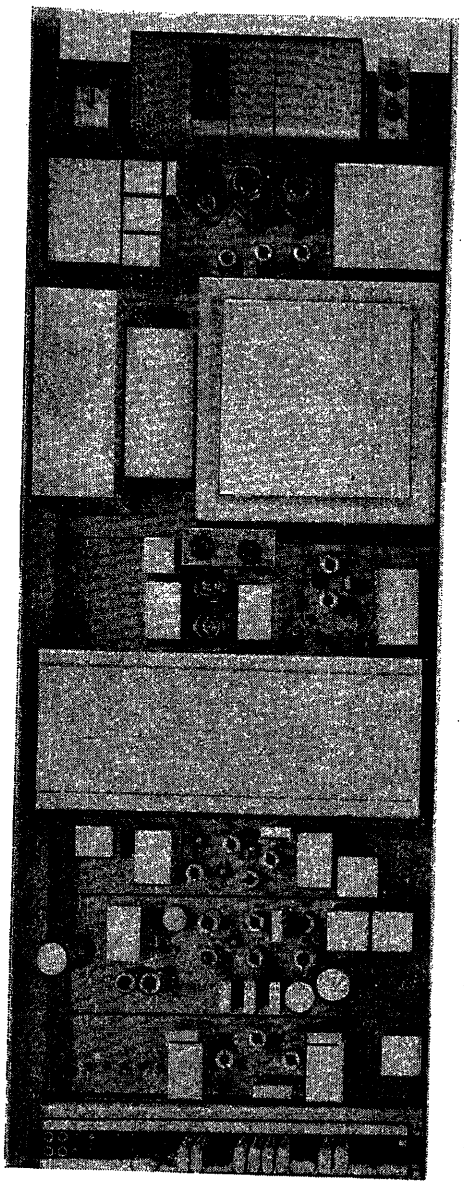


Figure 17. *L-1* coaxial television receiving terminal—bay arrangement

practical difficulties of such elements with respect to parasitic capacities. Small compact assemblies also result with this design as well as certain advantages in control of cut-off characteristics in design and manufacture. At this point only partial shaping of the vestigial sideband is accomplished. Further networks in the transmitting and receiving terminals also contribute to this total shaping.

The second step of modulation follows the band filter selection of the signal in the 5- to 8-megacycle region. The second modulator is supplied with a carrier frequency of 8.256 megacycles. The balanced modulator and carrier supply oscillator is similar in circuit form to that described for the first modulation step. By the use of a balanced modulator, certain second order unwanted modulation products are reduced to noninterfering levels. Following the second modulation process, a low-pass filter assures that unwanted 5- to 8-megacycles signal energy and the upper sideband of the second modulation are adequately suppressed. Pre-emphasis of the carrier signal according to the characteristic shown in Figure 6 now is introduced.

To guard against television signal energy, which may be present at the extreme of the partially shaped vestigial region, from interfering with the 64-kc pilot regulation channel and the 80–88-kc sound program channel, a band elimination filter is provided.

The considerable phase distortion, which is inherent in the vestigial shaping process as well as the sum of small residuals present throughout the transmitting terminal, is equalized in the following phase equalizer as indicated.

To assure an adequate level of the carrier signal for application to the *L-1* coaxial line, an amplifier finally is provided. This amplifier is of a 3-stage feed-back type employing common cathode impedance of a form such that the feedback is maintained at a high value for the lower frequencies where the higher signal energy condition obtains. This type of feed-back amplifier design exhibits improved stability under conditions of variations in internal gain, such as caused by aging of vacuum tubes and variation of other elements.

The networks previously referred to, that is, band-filter, low-pass filter, pre-emphasis network, band elimination filter, and phase and attenuation equalizers, represent the major contribution to the shaping and control of the transmission band characteristic of this vestigial side-band carrier terminal. Accordingly, to adequately meet the severe television transmission requirements, these networks must embody not only precise design techniques but also must be capable of exact control and adjustment during manufacture and must maintain their performance over the normal life of the terminal equipment under all operating conditions. To achieve this exacting performance standard, it has been found necessary to develop some new type network elements as well as more precise methods of construction and assembly. A new series of retardation coil types is employed, featuring metallic slugs for inductance adjustment. These elements are available in a continuous range of inductance from 0.22 to 220 microhenries. The form of these coils is designed for ease in mechanical assembly, and to assure a minimum of parasitic effects. Figure 10 shows the mechanical features of a typical constant resistance equalizer section employing these elements. Other elements shown include silvered mica-type capacitors and carbon deposited on glass-type resistors.

All networks in the transmitting terminal are enclosed in a single hermetically-sealed case. Figure 11 indicates the method of assembly of the individual network sections into the complete circuit. In this particular unit, 43 coils, 68 capacitors, and 41 resistors are involved.

Figure 12 is a complete television transmitting terminal for the *L-1* coaxial system. This terminal occupies approximately three feet of relay rack space. Included here is a regulated rectifier-type power supply, which operates from 115-volt 60-cycle power to furnish necessary d-c plate voltages for entire terminal.

Receiving Terminal Equipment

A block schematic of the elements of the *L-1* coaxial television receiving terminal is shown in Figure 13. The receiving terminal recovers the video signal from the received carrier signal as transmitted over the coaxial line. The modulation methods involved were discussed previously and are shown in Figure 2.

The pilot frequencies at 556, 2,064, and 3,096 kc which are transmitted over the coaxial line for gain regulation purposes

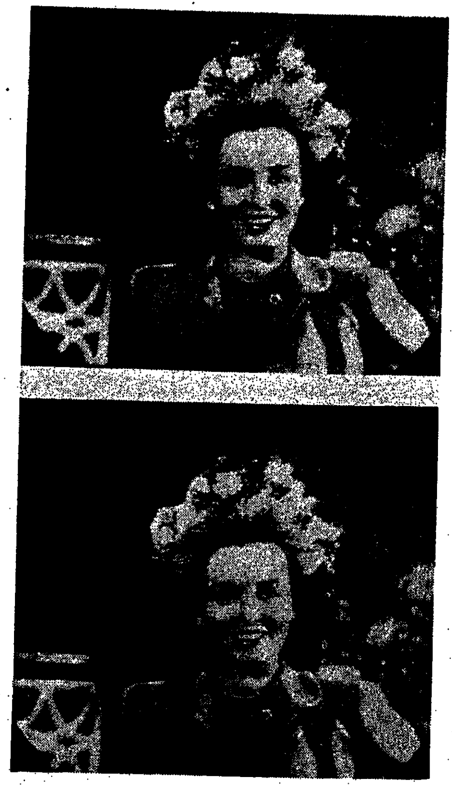


Figure 18. Television transmission performance of *L-1* carrier terminals

Top—Video Transmission
Bottom—Terminal Transmission

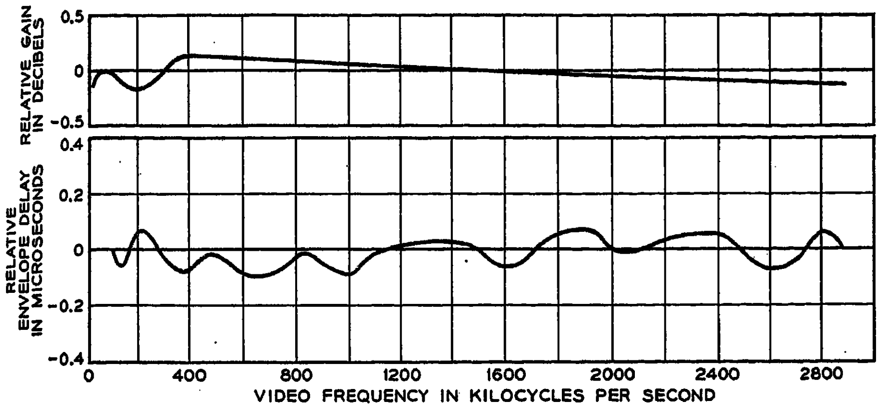


Figure 19. Over-all transmission characteristic for *L-1* coaxial television carrier terminals

must here be reduced in amplitude to assure noninterference with the television signal. A simplified schematic of the pilot elimination network is given in Figure 14. The operation of this circuit is based upon the condition of infinite attenuation presented between two windings of a hybrid coil when the remaining two windings are terminated in equal impedances. In this instance, one winding of the hybrid coil is terminated in a resistance. The associated winding is terminated by a transformer and three crystal filter networks as shown. At the resonance frequency of each crystal network, the impedance of the array, as presented at the terminals of the hybrid coil, is adjusted to be equal to the resistance termination of the associated winding. In these three very narrow frequency regions, located to coincide with the pilot tones, high attenuation is introduced within the signal branch of the hybrid coil. To assure coincidence of the resultant attenuation band and the pilot tone, the elements of the crystal networks are maintained at a constant temperature by means of a thermostatically controlled oven. The effective band elimination width is approximately 20 cycles at 556 kc, 60 cycles at 2,064 kc, and 100 cycles at 3,096 kc. Figure 15 shows some of the constructional features of the pilot elimination network.

The function of the phase equalizer following the pilot elimination network as shown in Figure 13 is similar to that discussed previously in the transmitting terminal. The band elimination filter here provides the necessary discrimination to the 64-kc pilot tone and to the 80–88-kc carrier sound channel. The restorer network characteristic has been previously indicated in Figure 6.

The following amplifier is identical to that described in the transmitting terminal. The demodulator which follows is supplied with a carrier frequency of 8.256 megacycles and provides the method by which the carrier signal as received from the coaxial line is again modulated into the 5- to 8- megacycle region. This demodulator is of the balanced type with conjugate carrier excitation as in the transmitting terminal. This is required to reduce the carrier leak and unwanted second order modulation products to noninterfering levels.

The lower sideband of this modulation process is selected by the following band filter where the remainder of the vestigial sideband shaping is accomplished. This filter is identical to the band filter similarly employed in the transmitting terminal. To provide an adequate level of this carrier signal for final envelope detection, an amplifier is provided furnishing approximately 31 decibels of gain in the 5- to 8-megacycle frequency band. This amplifier is of a 3-stage feed-back type.

The simplified schematic of the envelope detection circuit is shown in Figure 16. The modulated carrier signal with the carrier located at 7.944 megacycles, with the main sideband extending downward to approximately 5.1 megacycles, and with the vestigial sideband extending upward to approximately 8.056 megacycles, is impressed upon a bridge rectifier circuit. The nonlinear elements of this detector are germanium crystals. The rectified video signal output then is amplified and clamped prior to delivery to the receiving network operating center.

The theory of design and operation of clampers in television transmission has been presented recently. It is sufficient here to relate that the clamper, as employed in television transmission, periodically restores the video signal to a desired constant reference value. In this case, this is done upon the arrival of each horizontal synchronizing pulse. In this manner very low-frequency distortion or interference is effectively attenuated, the amount of attenuation being directly related to the ratio of the fundamental horizontal synchronizing frequency to the distortion frequency. A theoretical reduction of 31 decibes in a 60-cycle interference voltage may be obtained by this means. In the case of the *L-1* coaxial system, this clamping action reduces the effects of any power frequency modulation of the carrier which may have been introduced during transmission over the line. The arrangement of the equipment comprising an *L-1* coaxial television receiving terminal is shown in Figure 17. This terminal equipment occupies approximately 4 feet of relay rack space and operates from 115-volt 60-cycle power.

The *L-1* coaxial television terminal equipment which has been described here developed following basic design principles established in connection with experimental television transmission studies over the coaxial cable prior to the last war. The present type of equipment was first placed in commercial service in 1947, and at this time approximately 30 transmitting and 40 receiving terminals are in daily operation in the Bell System's television network.

Figure 18 presents a comparison between television signals before and after transmission through these *L-1* coaxial television terminals. The video circuit employed had a band width in excess of 4 megacycles, while in the case of the terminal transmission a low-pass filter simulating the cut-off characteristics of the coaxial line was included. The phase and attenuation characteristic for this back-to-back condition is shown in Figure 19.

References

1. PROGRESS IN COAXIAL TELEPHONE AND TELEVISION SYSTEMS, L. G. Abraham. *AIEE Transactions*, volume 67, part II, 1948, pages 1520–27.

2. COAXIAL CABLE SYSTEM FOR TELEVISION TRANSMISSION, M. E. Strieby. *Bell System Technical Journal* (New York, N. Y.), volume 17, July 1938, pages 438–59.

3. TELEVISION TRANSMISSION, M. E. Strieby, C. L. Weis. *Proceedings*, Institute of Radio Engineers (New York, N. Y.), volume 29, July 1941, pages 371–81.

4. SUBJECTIVE SHARPNESS OF SIMULATED TELEVISION IMAGES, M. W. Baldwin, Jr. *Proceedings*, Institute of Radio Engineers, (New York, N. Y.), volume 28, October 1940, pages 458–68.

5. THEORY OF SCANNING, Pierre Mertz, Frank Gray. *Bell System Technical Journal* (New York, N. Y.), volume 13, July 1934, pages 464–515.

6. EQUALIZATION OF COAXIAL LINES, K. E. Gould. *AIEE Transactions*, volume 68, part II, 1949, pages 1187–92.

7. A CARRIER SYSTEM FOR 8,000-CYCLE PROGRAM TRANSMISSION, R. A. Leconte, D. B. Penick, C. W. Schramm, A. J. Wier. *Bell System Technical Journal* (New York, N. Y.), volume 28, April 1949, pages 165–80.

8. CERTAIN TOPICS IN TELEGRAPH TRANSMISSION THEORY, H. Nyquist. *AIEE Transactions*, volume 47, February 1928, pages 617–44.

No Discussion

Report of a Survey on the Performance of Shunt Capacitors

AN AIEE COMMITTEE REPORT

IN November 1947, a Capacitor Subcommittee was formed under the AIEE Transmission and Distribution Committee. This Subcommittee was charged with the responsibility of studying capacitor performance, revising existing capacitor standards, and so forth.

A project, suggested at the first meeting of the newly formed Subcommittee, was that of accumulating operational information on the application of shunt capacitors to power systems, with particular regard to capacitor unit failures or operating difficulties experienced by operating engineers. A working group was formed to study the problem. It was soon realized that this subject was very broad in scope and the decision was made to invite the Transmission and Distribution Committee of the Edison Electric Institute (EEI) to participate in the study.

The report as presented is therefore the result of close co-operation between working groups appointed by the AIEE and the EEI and represents the results of the accumulation of considerable data by members of both organizations.

In order to obtain information to prepare a report on experience with the operation of shunt capacitors, a questionnaire was formulated and sent to electric operating companies in this country and Canada.

A copy of the questionnaire is included in this report. Interest in the work of the two subcommittees was evidenced by the excellent response to the questionnaires.

The application of shunt capacitors on power systems received great impetus shortly preceding and during the war years. Added current carrying capacity of lines, transformer banks, and other equipment was to be gained by their use and power system operating personnel became acutely aware that the application of capacitors was a great boon to the industry at a time when other electric equipment was extremely difficult to obtain in time to meet rapidly growing demands for power.

With the shunt capacitor now firmly established as a valuable tool of the industry, it became evident, through miscellaneous but minor difficulties experienced in capacitor use, that there existed a need for accumulation of information pertaining to the operation of this equipment. This report endeavors to present this information in a concise and impartial form, without embarrassing any manufacturer or utility.

Scope of Distribution of the Questionnaire

The questionnaire forms were sent to 118 operating utilities in all portions of the United States and Canada. All companies represented on the EEI Transmission and Distribution Committee received a copy and all other operating companies not associated with EEI but having a maximum system load peak of at least 50,000 kw for 1947 were included in the mailing.

A total of 88 replies was received from those companies contacted. In that some companies contacted had few or no capacitors installed, the replies of 83 companies (42 replies from companies represented on the EEI Transmission and Distribution Committee and 41 replies from other companies) were included in the data for the report.

Of the 83 companies returning the questionnaire, 66 classified themselves as supplying energy to urban and rural load, while 17 classified themselves as supplying predominantly metropolitan areas.

Size of Systems Studied

The total combined 1947 peak loads of all companies responding with data was 28,147.2 megawatts. It is felt that these companies represent a very fair segment of the power industry in the country. The size of individual systems reporting varied from 24.0 megawatt demand to 1,455.0 megawatt demand. One larger utility answered the questionnaire but its ratio of capacitor capacity installed was so small compared to the system size that the statistical data furnished was not included in the report since it was considered as not being representative of the group.

Capacitor Installations—Total

The total capacity of the shunt capacitor installations on the systems reported was 2,849.7 megavars (mvar). This figure represents 10.1 per cent of the total system demand of all reporting. Thus, for each 1,000 kw of system demand, the reporting companies had installed an approximate 101 kilovars (kvar) of capacitors as an average.

Type of Capacitor Installations

Most capacitor installations fall under four general classes with regard to mounting or housing. The questionnaire listed these, and the resulting replies are shown in Table I.

Paper 49-251, recommended by the AIEE Transmission and Distribution Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 26, 1949; made available for printing September 6, 1949.

Prepared by the Joint Working Groups of the Capacitor Subcommittee of the AIEE Transmission and Distribution Committee and the Edison Electric Institute Transmission and Distribution Committee.

Personnel of the AIEE Capacitor Subcommittee Working Group: C. E. Baugh, E. W. Dillard, K. E. Hapgood, E. R. Hendrickson, R. B. Miller, C. E. Parks (Co-Sponsor).

Personnel of the EEI Transmission and Distribution Committee Working Group: H. E. Stites (Co-Sponsor).

Table I

Type of Mounting	Megavars Installed	Per Cent of Total Installed Megavars
Pole-type installations of outdoor units	1,738.3	61.0
Open rack installations of outdoor units	410.0	14.4
Pole-type housed installations	110.8	3.9
Substation housed installations	590.6	20.7

Voltage Rating of Installed Capacitors

In order to obtain data on the voltage class of the systems to which capacitors were applied, the questionnaire listed four general ranges of system voltages. Data furnished by those answering questionnaire is summarized in Table II.

Table II

Voltage Rating	Megavars Installed	Per Cent of Total Installed Megavars
600 volts or less	49.1	1.7
2.3–4.8 kv	2,165.5	76.1
6.9–12.5 kv	519.7	18.2
Over 12.5 kv	115.4	4.0

Method of Control of Capacitors or Capacitor Banks

To determine the method by which capacitors or capacitor banks were placed in service or removed from service, the questionnaire requested a breakdown of information on the installations. Data was requested for a summary of the equipment which was normally switched by one method or another. It is assumed that the individual company's capacitors which are not switched by the methods listed remain on the systems permanently a majority of the time. The results of the data obtained from information furnished are as follows: capacitors regularly switched, either manual or automatic, total 1,002.9 mvar or 35.2 per cent of all capacitors installed, see Table III. Capacitors not regularly switched total 1,846.8 mvar or 64.8 per cent of all units installed.

Table III

Method of Control	Capacitors Controlled, Megavars	Per Cent of Total of Switched Units
Voltage control	330.5	33.0
Kilovar control	7.4	0.7
Current control	11.1	1.1
Time-switch control	110.9	11.0
Manual control	509.1	50.8
Other methods	33.9	3.4

Type of Protection of Capacitor Units

In order to determine present practices in the application of protection to capacitor installations, data was secured on the several present methods of protection employed. Since in numerous cases, the protection was overlapping, the total megavars of units protected is greater than the total installed capacity. The results of data secured are shown in Table IV.

Table IV

Method of Protection	Megavars of Units Thus Protected	Per Cent of Total Installed Capacitor Megavars
Individual unit fusing	493.0	17.3
Group fusing	714.2	25.1
Total bank fusing	1,269.9	44.5
Relay protection on oil circuit breaker	715.2	25.0
Other types of protection	8.2	0.3
	3,200.5*	112.2

* Thus, approximately 350.8 mvar of capacitor installations were protected by overlapping of the several methods listed. This represents 12.2 per cent of total installed capacitor units.

Lightning Arrester Protection of Capacitors

The question was asked concerning lightning arrester application in connection with capacitor installations. The results obtained are as follows:

65 companies out of 83 generally use lightning arrester protection. This represents 78 per cent of all companies reporting.

18 companies did not apply lightning arrester protection. This represents 22 per cent of all companies reporting.

Choice of Protection of Capacitors

Data was further requested as to whether voltage class of system determined the type of fuse or relay protection of capacitors or capacitor banks.

58 companies reported that voltage class did not determine type of protection—70 per cent.

5 companies reported that voltage class determined type of protection—6 per cent.

20 companies did not report on the question—24 per cent.

Capacitor Failures

The primary purpose of this investigation was the determination of the cause for capacitor failures that have occurred within the past several years with the hope that this information will serve as a guide to those contemplating additional installations of such equipment. The subject of capacitor failures on the questionnaire was treated in two ways. Several of the most common causes for failure were listed and it was asked that these failures be further differentiated as to the period within the last several years in which they occurred. It is realized that many more capacitors have been installed in the last six years than were installed previously, hence it would be reasonable to conclude that more failures should have taken place in this period. The collected data bears out this conclusion.

The tabulation of the data reported is made in several different manners for purposes of comparison of one type of failure against another, one voltage class of capacitor against another class, one period of failure against another, and so forth.

Of the 2,849.7 mvar of capacitors installed, 34.160 mvar have failed or had to be removed from service for one reason or another. This represents a failure of only 1.2 per cent of all installed units.

Reported causes of failure for any reason are shown in Table V.

Table V

Cause	Kilovars of Units Removed from Service	Per Cent of Total Kilovars of Units Removed from Service
Internal failure	20,735	60.7
Bushing failure or breakage*	1,600	4.7
Leaks in case or bushing seal	1,235	3.6
Mechanical defects or damage	5,540	16.2
Misapplication		
Overvoltage	150	0.4
Bad waveform	30	0.1
High ambient (lack of ventilation)	385	1.1
Low ambient (units de-energized)	815	2.4
Lack of adequate lightning protection*	1,225	3.6
Other causes**	2,445	7.2

* Some failures listed in these two classifications were attributed to direct lightning strokes.

** Under this classification were listed manufacturers' construction difficulties, wrong connections upon installation, gun shot, switching, and so forth.

A breakdown of the capacitor failures from all causes into number of years in service before failure is as follows:

8,610 kvar or 25.2 per cent failed within one year after installation.

18,215 kvar or 53.3 per cent failed after having been installed 1 to 5 years.

7,335 kvar or 20.5 per cent failed after having been installed over 5 years.

A further breakdown of total capacitor failures by voltage class is as follows:

3,525 kvar or 10.3 per cent were in the

6.9-kv class or higher, representing 0.55 per cent of total installed in this class.
30,575 kvar or 89.5 per cent were in the 2.3–6.8-kv class, representing 1.41 per cent of total installed in this class.
60 kvar or 0.2 per cent were in the 600 volts or less class, representing 0.12 per cent of total installed in this class.

There follows a listing by the particular cause of trouble experienced which caused the capacitor units to be removed from service. Each of these causes is tabulated in the same manner as were the total failures.

INTERNAL FAILURES

Failures from this cause may be attributed to many reasons. Usually the evidence of the cause was destroyed in the resulting failure.

Of the total of 20,735 kvar of internal failures:

5,235 kvar or 25.2 per cent were caused to units in service 1 year or less.
9,630 kvar or 46.5 per cent were caused to units in service 1 to 5 years.
5,870 kvar or 28.3 per cent were caused to units in service over 5 years.

Of the 5,235 kvar of units failing during the first year of service:

1,025 kvar or 19.6 per cent were in the 6.9-kv class or higher.
4,180 kvar or 79.8 per cent were in the 2.3–6.8-kv class.
30 kvar or 0.6 per cent were in the 600 volts or less class.

Of the 9,630 kvar of units failing during 1 to 5 years of service:

905 kvar or 9.4 per cent were in the 6.9-kv class or higher.
8,725 kvar or 90.6 per cent were in the 2.3–6.8-kv class.

Of the 5,870 kvar of units failing after service over 5 years:

90 kvar or 1.5 per cent were in the 6.9-kv class or higher.
5,780 kvar or 98.5 per cent were in the 2.3–6.8-kv class

Of the total of 20,735 kvar of internal failures:

2,020 kvar or 9.7 per cent were in the 6.9-kv class or higher.
18,685 kvar or 90.2 per cent were in the 2.3–6.8-kv class.
30 kvar or 0.1 per cent were in the 600 volt or less class.

BUSHING FAILURES OR BREAKAGE

Failures from this cause may have been caused by lightning, bad porcelain, poor handling, or poor packing at the factory.

Of the total of 1,600 kvar of units failing due to bushing failures or breakage:

445 kvar or 27.8 per cent were caused to units in service 1 year or less.
870 kvar or 54.3 per cent were caused to units in service 1 to 5 years.
285 kvar or 17.7 per cent were caused to units in service over 5 years.

Of the 445 kvar of units which failed during the first year of service:

230 kvar or 51.7 per cent were in the 6.9-kv class or higher.
215 kvar or 48.3 per cent were in the 2.3–6.8-kv class.

Of the 870 kvar of units after 1 to 5 years of service:

240 kvar or 27.6 per cent were in the 6.9-kv class or higher.
630 kvar or 72.4 per cent were in the 2.3–6.8-kv class.

Of the 285 kvar of units failing after having been in service over 5 years:

285 kvar or 100 per cent were in the 2.3-6.8-kv class.

Of the total of 1,600 kvar of units failing because of bushing trouble:

470 kvar or 29.4 per cent were in the 6.9-kv class or higher.
1,130 kvar or 70.6 per cent were in the 2.3–6.8-kv class.

LEAKS IN CASE OR AROUND BUSHINGS

Failures attributed to "leaks" were described as seam welding defects in the case, bushing gasket difficulty, and so forth:

Of the total of 1,235 kvar of units failing because of leaks:

360 kvar or 29.1 per cent were caused to units in service 1 year or less.
550 kvar or 44.6 per cent were caused to units in service 1 to 5 years.
325 kvar or 26.3 per cent were caused to units in service over 5 years.

Of the 360 kvar of units failing during the first year of service:

15 kvar or 4.2 per cent were in the 6.9-kv class or higher.
345 kvar or 95.8 per cent were in the 2.3–6.8-kv class.

Of the 550 kvar of units failing during 1 to 5 years of service:

30 kvar or 5.5 per cent were in the 6.9-kv class or higher.
490 kvar or 89.0 per cent were in the 2.3–6.8-kv class.
30 kvar or 5.5 per cent were in the 600-volt class or less.

Of the 325 kvar of units failing after over 5 years of service:

325 kvar or 100 per cent were in the 2.3–6.8-kv class.

Of the total of 1,235 kvar of units failing because of leaks:

45 kvar or 3.6 per cent were in the 6.9-kv class or higher.
1,160 kvar or 94.0 per cent were in the 2.3–6.8-kv class.
30 kvar or 2.4 per cent were in the 600-volt or less class.

MECHANICAL DEFECTS OR DAMAGE

Failures falling under this heading were attributed to hangar welding breakage, case damage in shipment, and so forth.

Of the total of 5,540 kvar of unit failures caused by mechanical defects or damage:

1,030 kvar or 18.6 per cent were caused to units in service 1 year or less.
4,330 kvar or 78.2-per cent were caused to units in service 1 to 5 years.
180 kvar or 3.2 per cent were caused to units in service over 5 years.

Of the 1,030 kvar of units failing during the 1st year of service:

120 kvar or 11.7 per cent were in the 6.9-kv class or higher.
910 kvar or 88.3 per cent were in the 2.3–6.8-kv class.

Of the 4,300 kvar of units failing during 1 to 5 years of service:

540 kvar or 12.5 per cent were in the 6.9-kv class or higher.
3,790 kvar or 87.5 per cent were in the 2.3–6.8-kv class.

Of the 180 kvar of units failing in service over 5 years:

180 kvar or 100 per cent were in the 2.3-6.8-kv class.

Of the total of 5,540 kvar of units failing because of mechanical defects or damage:

660 kvar or 11.9 per cent were in the 6.9-kv class or higher.
4,880 kvar or 88.1 per cent were in the 2.3–6.8-kv class.

Numerous failures were attributed to "misapplication" of the units. Under the following headings will be discussed the results of replies received covering these difficulties.

OVERVOLTAGE

Failures which were caused by applying units on systems which were operated at voltages above the permissible voltage ratings on the capacitor units are described in this category:

Of the total of 150 kvar of units failing because of overvoltage:

90 kvar or 60.0 per cent were caused to units in service 1 year or less.
60 kvar or 40.0 per cent were caused to units in service over 5 years.

Of the total of 150 kvar of units failing because of overvoltage:

150 kvar or 100 per cent were in the 2.3–6.8-kv class.

Questionnaire on Shunt Capacitor Operation and Experienced Failures or Difficulties

Joint Working Group on Performance of Shunt Capacitors of the AIEE and EEI Transmission and Distribution Committees

---

Please answer the following questions regarding troubles experienced with shunt capacitors installed or maintained by your company:

1. Size of system on which capacitors are installed in terms of peak megawatts for 1947 .... Megawatts
   - (a) System predominantly metropolitan.........or integrated urban and rural..........
2. Approximate total shunt capacitors installed on December 31, 1947 .... Megavars
   - (a) Pole-type installations of outdoor units .... Megavars
   - (b) Open rack installations of outdoor units .... Megavars
   - (c) Pole-type housed installations .... Megavars
   - (d) Substation housed installations .... Megavars
3. Total megavars of capacitors installed by voltage class on December 31, 1947
   - (a) On systems rated 600 volts or lower .... Per Cent
   - (b) On systems rated 4.1 kv or 2.3 kv .... Per Cent
   - (c) On systems rated 11 kv or 6.9 kv .... Per Cent
   - (d) On systems rated over 11 kv .... Per Cent
4. Automatically or manually switched per cent of total megavars .... Per Cent
   - (a) Voltage controlled ....% of total megavars
   - (b) Kilovars controlled ....% of total megavars
   - (c) Current controlled ....% of total megavars
   - (d) Time-switch controlled ....% of total megavars
   - (e) Manually controlled ....% of total megavars
   - (f) Controlled otherwise ....% of total megavars. Explain:
     ....
     ....
     ....
5. Type of protection generally used for capacitors in per cent of total megavars:
   - (a) Fused individually ....%, by groups ....%, or by total banks ....%
   - (b) Protected by circuit breakers with overcurrent relays ....%
   - (c) Is lightning arrester protection generally used? .... If method of connection of capacitors or grounding of capacitor unit cases governs this, please explain: ....
     ....
     ....
   - (d) Other methods of protection, explain: ....
     ....
   - (e) Does voltage class determine type of protection? Explain: ....
     ....
6. How many capacitor units (or what total megavars of units) have been removed from service because of any failure or defect, approximately how many years were the units in service before removal, what voltage class were units which were removed? Explain in following tabulation.

	Years Service					
	Under 1 Year		1 to 5 Years		Over 5 Years	
	Mvar	Kv	Mvar	Kv	Mvar	Kv
(a) Due to internal failure	......	......	......	......	......	......
(b) Due to bushing failure or breakage	......	......	......	......	......	......
(c) Due to leaks	......	......	......	......	......	......
(d) Due to mechanical defects of damage	......	......	......	......	......	......
(e) Due to misapplication:						
(1) Overvoltage	......	......	......	......	......	......
(2) Bad Waveform	......	......	......	......	......	......
(3) High ambient temperature or lack of ventilation	......	......	......	......	......	......
(4) Low ambient temperature with units de-energized	......	......	......	......	......	......
(5) Lack of adequate lightning protection	......	......	......	......	......	......
(6) Other reasons (explain)	......	......	......	......	......	......

....
....

   - (f) Other reasons for failure which are not described above (explain in detail) ....
     ....

   Note: The Committee would appreciate detailed reports on each case of failure to the extent available.

7. In the application of capacitors to the system, have any of the following troubles been experienced? If these difficulties have beencorrected by changes in connection of capacitor units or by other methods, please explain remedial measures taken.
   - (a) Due to telephone interference ....
   - (b) Due to harmonic overcurrent ....
   - (c) Due to carrier current interference ....
   - (d) Due to radio interference ....
   - (e) Due to inrush or discharge current ....
   - (f) Other causes, describe. ....
8. Inspection and Maintenance.

	Pole Type Outdoor	Open Rack Outdoor	Pole Type Housed	Substation Housed
(a) Do you have periodic inspection?	......	......	......	......
(b) If so, at what intervals	......	......	......	......

   - (c) Describe procedure of inspection and maintenance..
     ....

General Remarks and Recommendations: ....
....
....
....
....

Signed..

Name of Company .... Location ....

BAD WAVEFORM

Failures attributed to bad waveform were described as mostly caused by prevalent fifth harmonics in fundamental frequency on the system to which the capacitors were applied.

Of the total of 30 kvar of units failing due to bad waveform:

30 kvar or 100 per cent failed during the first year of service. These units were all in the 2.3–6.8-kv class.

HIGH AMBIENT TEMPERATURE (LACK OF VENTILATION)

Units which failed due to high ambient temperature were described as being located where ventilation was poor.

Of the total of 385 kvar of units failing because of high ambient temperature:

325 kvar or 84.5 per cent failed during the first year of service.
60 kvar or 15.5 per cent failed after 1 to 5 years of service.

Of the total of 385 kvar of units failing because of high ambient temperature:

385 kvar or 100 per cent were in the 2.3–6.8-kv class.

LOW AMBIENT (UNITS DE-ENERGIZED)

Failures for this reason were attributed to the low temperatures producing lowered dielectric strength of insulating medium or production of voids in units which punctured when units were energized after having been out of service for some time during low temperature periods.

Of the total of 815 kvar of units failing because of low temperature:

215 kvar or 26.4 per cent were caused to units in service 1 year or less.
540 kvar or 66.3 per cent were caused to units in service 1 to 5 years.
60 kvar or 7.3 per cent were caused to units in service over 5 years.

Of the total of 815 kvar of units failing in all years of service:

815 kvar or 100 per cent were in the 2.3–6.8-kv class.

LACK OF ADEQUATE LIGHTNING PROTECTION

Failures attributed to this cause were described as either being brought about by direct lightning strokes or by the fact that no lightning arresters were applied on installation.

Of the total of 1,225 kvar of units failing due to lack of adequate lightning protection:

220 kvar or 17.9 per cent were caused to units in service 1 year or less.
720 kvar or 58.8 per cent were caused to units in service 1 to 5 years.

285 kvar or 23.3 per cent were caused to units in service over 5 years.

Of the 220 kvar of units failing during the first year of service:

220 kvar or 100 per cent were in the 2.3–6.8-kv class.

Of the 720 kvar of units failing during 1 to 5 years service:

105 kvar or 14.6 per cent were in the 6.9-kv class or higher.
615 kvar or 85.4 per cent were in the 2.3–6.8-kv class.

Of the 285 kvar of units failing after being in service over 5 years:

285 kvar or 100 per cent were in the 2.3–6.8-kv class.

Of the total of 1,225 kvar of units failing due to lack of adequate lightning arrester protection:

105 kvar or 8.6 per cent were in the 6.9-kv class or higher.
1,120 kvar or 91.4 per cent were in the 2.3–6.8-kv class.

Other Causes for Failure

Under this classification are grouped those failures which occurred from miscellaneous causes such as wrong connections of units at time of installation, gun shot damage, improper switching which caused failure, and so forth.

Of the total of 2,445 kvar of units which failed from other causes:

690 kvar or 28.2 per cent were caused to units in service 1 year or less.
1,485 kvar or 60.7 per cent were caused to units in service 1 to 5 years.
270 kvar or 11.1 per cent were caused to units in service over 5 years.

Of the 690 kvar of units failing during the first year of service:

180 kvar or 26.1 per cent were in the 6.9-kv class or higher.
510 kvar or 73.9 per cent were in the 2.3–6.8-kv class.

Of the 1,485 kvar of units failing after being in service 1 to 5 years:

30 kvar or 2.0 per cent were in the 6.9-kv class or higher.
1,455 kvar or 98.0 per cent were in the 2.3–6.8-kv class.

Of the 270 kvar of units failing after being in service over 5 years:

270 kvar or 100 per cent were in the 2.3–6.8-kv class.

Of the total of 2,445 kvar of units which failed from other causes:

210 kvar or 8.6 per cent were in the 6.9-kv class or higher.
2,235 kvar or 91.4 per cent were in the 2.3–6.8-kv class.

Application Difficulties

In the application of capacitors, numerous troubles of minor nature have been encountered. The questionnaire listed the most prevalent causes of such difficulty and the information below summarizes the data furnished.

Telephone Interference

Out of the 83 companies reporting, 41 stated that trouble had been experienced with telephone interference caused by capacitor installations and 42 replied that no trouble had been encountered.

This trouble had generally been corrected by proper transposition, by the moving of the capacitor bank, by removing grounds on Y-connected capacitor banks, by relocating distribution lines, by installing filters, by using neutral reactor in capacitor bank neutral, by solidly grounding capacitor cases, and by the telephone company making changes in their installations.

Harmonic Overcurrent

Fourteen companies reported difficulty due to harmonic overcurrent on capacitor installations and 69 reported no trouble.

This trouble generally was corrected by reducing size of capacitor banks, switching banks out during off-peak conditions, removing Y-ground, changing system set-up, and so forth. It was noted that this is a condition which must be closely checked, particularly at times system load is light. On one remote installation, a recording ammeter installed indicated a serious harmonic condition existing which was not appreciated and units had to be removed from service to correct this.

Carrier Current Interference

Only one company reported having any difficulty which was credited to this condition. No information was supplied but it was stated that interference was caused to "carrier telephone and telegraph."

Radio Interference

Five companies indicated that they had experienced radio interference upon the installation of capacitors. By relocating the capacitor banks or removing pole grounds, this trouble was usually corrected.

Inrush Current

A total of 6 companies indicated that trouble had been encountered due to the heavy inrush currents experienced by the energizing of capacitor banks. Usually this resulted in the blowing of fuses. Such difficulty was corrected by the installation of a larger fuse.

It was reported that the paralleling of large banks of capacitors on the same bus aggravates this condition under some conditions.

Other Reasons

Five companies reported miscellaneous troubles encountered, not listed above. Resonance was caused in one case and the removing of the capacitor bank Y-ground corrected it. Serious system unbalance resulted when capacitors were connected to a circuit supplied by an open-delta bank. The closing of the bank delta corrected this trouble.

Inspection and Maintenance

Questions were asked to determine the extent of inspection and maintenance which is being carried out by the companies reporting.

Periodic Inspection

About 60 per cent of the reporting companies indicated that they maintain periodic inspection on their capacitor installations. The extent of this inspection varied greatly, but most companies reported only a visual check of case condition, fuse position, and general over-all appearance.

Interval Inspection

The answers to this question also were widely divergent, varying from three per day (for substation installations) to every five years. The average period was from one to three months, inspection made by line maintenance, service, substation maintenance, and line patrol personnel.

Procedure on Inspection and Maintenance

The standard procedure for inspection is by visual means only. Numerous companies reported that split-core ammeter checks were made occasionally. Inspections of paint, connections, mounting, and so forth, were reported by some, and one company reported that it regularly had the bushings cleaned. Evidently the maintenance problem of capacitor units must not be serious in that no reports were received of an extensive maintenance program being required to maintain good capacitor performance.

By far the most interesting aspect of this report, in the minds of the working group, were the comments given by the reporting companies regarding their experience with capacitor installations. It is felt that much good information, from an operating engineers' standpoint, is contained in the remarks made. For this reason, the reporting of the data received from the utilities will be in the most part as taken directly from the letters received and from comments noted on the questionnaire. These comments follow:

Two companies report failure of gas-filled capacitors. One company reports that of 720 kva of gas-filled capacitors installed in 1937 and 1938, only 45 kva remain in service.

Several companies report large capacitor installation programs just starting.

Several companies report a power factor clause in their rate schedule and for this reason the majority of their larger customers have installed their own power factor correction.

Company number 1 reports that their substation housed capacitors are power-factor tested once a year.

Company number 2 reports difficulty in correctly adjusting feeder voltage regulators on 4-kv circuits having capacitors.

Company number 3 has a mobile capacitor trailer of 1,080 kva capacity. One-half of its capacity is controlled manually and the other half, either manually or automatically by voltage. Each 15-kva single-phase unit is fused individually.

Company number 4 reports that they found it impossible to install capacitors on circuits supplying resonant relay controlled street lamps due to improper relay operation.

Company number 5 reports: "Two cases of trouble were experienced in the same location where 2 180-kvar capacitor banks were located on the load side of sectionalizing fuses in a branch circuit. The capacitor units were 4.16-kv units, delta connected, on a 4.0-kv Y primary system with a 120/240-volt delta secondary system. Both open delta and closed delta transformer banks are in service, serving an area predominately industrial. On two occasions, the blowing of one of the sectionalizing fuses caused the capacitors to be fed back through rotating equipment and distribution transformers, which in turn, it is thought, caused saturation of the iron cores, and nonlinear resonance with resulting extremely high voltages. On both occasions, failure of both line and transformer lightning arresters occurred, transformer fuses in the area blew and the failure of one transformer is thought attributable to this. In this case, it was not desirable to remove sectionalizing fuses, so the capacitors were relocated, but as a general rule, when capacitors are installed, any single pole disconnects or fuses are removed or relocated beyond the capacitors."

Company number 6 reports: "The individual capacitors in the station-type installations are 2,400-volt units connected in parallel, and these parallel groups are connected in series from line to line to make a 3-phase delta bank. This makes possible the use of moderate capacity fuses for individual fuse protection, even though the bank may be connected to a bus with high fault duty."

Company number 7 reports that where pole-type housed units were used, the cases were made of ordinary steel and have rusted badly and require frequent painting.

Company number 8 reports: "Failure of 180 kvar (12–15-kvar capacitors) 2,400-volt automatically-switched capacitor units which were in service on a part of our system where long periods of 0 degrees Fahrenheit to −40 degrees Fahrenheit ambient temperatures are not unusual. These units are voltage controlled and are normally in service five days a week from 7:00 a.m. to 11:00 a.m. and from 12:00 noon to 4:00 p.m. Investigation revealed that failure was caused by solidification of thin columns of insulating liquid between layers in the capacitor unit; this solidification occasionally caused separation of the column and on energizing resulted in a phase-to-phase internal failure across the void. The manufacturer has developed a unit filled with "low temperature" liquid and we are in the process of replacing all individual capacitors in switched service which are subject to these extreme low ambient temperatures for long periods."

Company number 9 reports voltage relays in ungrounded neutral ties between groups of a switched capacitor bank. These relays protect against over-voltage on units in series-parallel combinations of 2,400-and 460-volt units making up Y-connected groups on an ungrounded 13.8-kv system.

Company number 10 reports failure of some ten units last February during very cold weather: "By this time you may be familiar with the cause of this trouble. We were just informed of it by the manufacturer last week. We are told that the insulating medium used in these capacitors, and in fact all capacitors up to the present time is found to be not suitable for cold weather. During temperatures in the order of 10 to 15 degrees below zero we can expect failures. The failures occur when the capacitor has been disconnected and is, therefore, cold and is energized under those circumstances.

"We have put in service three other 2,520-kvar banks of the same type, and we can expect the same kind of trouble on these if something is not done. The manufacturer has proposed as a remedy that heaters be installed in the housings. If they could be kept energized at all times during extremely cold weather we would expect to avoid trouble because the internal heat generated by the losses in the capacitor are sufficient to prevent a drop in temperature low enough to cause trouble."

Company number 11 has used relay protection for capacitor bursting protection on those large capacitor banks of 1,200 kvar and greater where capacitors are Y-connected. "The capacitor bank is divided into two equal kilovar sections and the Y points of the two sections are connected together through either a current or a potential transformer and the secondaries connected to an appropriate relay. Although both methods of relaying have proved satisfactory, the current protection is preferred as this allows enough time in case of fault in a capacitor for the case to bulge and to find it on a visual inspection."

Company number 12 reports that they have had a comparatively great number of failures of 15-kva 2,400-volt capacitor units due, the manufacturer believes, to accidental breaks in vacuum during the process of installing the bushings and terminals, and not due to electrical operating conditions.

"We have already recommended to manufacturers that, in capacitor assemblies, greater current carrying capacity be provided throughout to permit these assemblies to operate satisfactorily on rated voltage containing a 10 per cent fifth harmonic voltage. On our system troubles arising from this source have been met by switching the capacitors out of service during period of light load when our system operated with a maximum fifth harmonic voltage. Furthermore, we recommend that the units themselves be designed to operate at the standard temperature rise with this order of fifth harmonic voltage."

Company number 13 reports that all units are power-factor tested and most defects have been located by this test. Any units having power factor over 1 per cent are considered defective.

Company number 14 reports: "Our capacitor units are really doing a job in giving dependable automatic corrective. Our capacitors, along with a 20,000-kva synchronous condenser give a very flexible corrective arrangement."

Company number 15 reports: "All failures from leaks were new capacitors."

Company number 16 reports: "Prior to 1939, we had installed a few of the gas-filled type on which we have had some trouble due to loss of gas."

Company number 17 reports: "High voltage resulted when one line fuse blew ahead of a line section having a 4,160-volt delta capacitor bank and several transformer banks connected. Condition corrected by moving line fuses to load side of the capacitor bank."

Company number 18 reports: "From what information we have, it appears that the great majority of failures have been during electrical storms."

Company number 19 reports: "We recently experienced a failure of 13 units constituting one of the groups in a bank of 4,000-volt units connected directly to the 22-kv system. No cause has been found for the failure. The unbalanced voltage protection was out of service at the time of failure and it is felt that had this been in service, the bank would have been taken off the line with the damage confined to fewer units.

Company number 20 reports: "Resonance causing high voltage has resulted at light load when primary fuse opened toward source. Remedy is to use units connected solidly from phase to neutral. Capacitor units are failing at a much higher rate than transformers. One manufacturer used inadequate internal clearances and excessive volts per mil stress on paper resulting in high percentage of failure in first five years."

Company number 21 reports: "Capacitor cases of units located near water front and subjected to salt spray rusted out in one year."

Company number 22 reports: "On distribution system being supplied from substation transformer banks connected open-delta, capacitors caused serious unbalance in system voltages. Adding third transformer to substation remedied this condition."

Company number 23 reports: "Defective welding of the seams or repairing by welding of a leak was the primary cause of several failures. Upon opening the capacitor cases on several occasions, hair-like projections, due to improper welding, were found to be the cause of insulation failure."

Company number 24 reports: "Due to inrush currents arcing at current transformer secondary links at time of circuit breaker closing. Corrected by shunting current transformer primaries with 1/10-ohm resistor. The manufacturer investigated some units which failed and suggested that the trouble was due to the improper storage of the paper used in their manufacture."

Company number 25 reports: "The 11.5 mvar of pole-type installations were mostly installed in 1941–42. Of these, a total of 0.036 mvar or 0.314 per cent have failed in service in 6 or 7 years. As none are protected by lightning arresters, this indicates no need for such protection."

Company number 26 reports: "On the 12-kv system, we have two Y-connected banks of 153-2.4-kv units each. Difference in potential between the floating neutral points of the two banks of capacitors is depended upon to trip the main oil circuit breaker in event of failure of any one unit. A problem in connection with low-voltage industrial installations is matter of exceeding the allowable voltage limits of the 230- and 460-volt rated units on the nominal 240-and 480-volt secondaries."

Conclusion

It is hoped by all members of the working group, both EEI and AIEE, that the analysis of the reports given on the questionnaire forms received will be enlightening and helpful to all in their application problems in connection with the installation of capacitors. Certainly, individual ideas of the engineers answering the questionnaire are to be expected. These ideas must be weighed in light of the fact that systems vary widely, both in scope and location.

It is quite evident that the manufacturers of capacitor units are continually striving to improve their product through the use of new materials, new designs and by profiting from the operational experience of utilities who use their products. The steadily improving performance of capacitors over the past two decades is indicative that the capacitor art is advancing rapidly.

Discussion

I. W. Gross (American Gas and Electric Company, New York, N. Y.): This is an excellent report on the field performance of shunt capacitors and represents a vast amount of work by the Subcommittee which prepared it, not only in obtaining information, but in summarizing it in condensed and usable form to bring out the major points of significance in capacitor operation.

I am particularly interested in the lightning protection aspects where these capacitor banks are used in large sizes. The report states that 65 companies use lightning arrester protection for the capacitor and 18 companies do not. Some 20 years ago the theory was advanced that the capacitance of high-voltage bushings, as a sort of lightning arrester, should supply some degree of lightning protection to equipment. The capacitance of such bushings was in the order of 0.0003 microfarads. As more was learned about natural lightning phenomena, this theory was soon exploded as the capacitance was too small to be at all significant. However, the capacitor banks dealt with in this report range in the general order of up to 12 microfarads, and therefore present a different picture.

In the protection of d-c equipment, capacitance elements only are used as so-called lightning arresters and have capacitance in the order of 5 microfarads. The question therefore arises, are not capacitor banks of the larger sizes mentioned in the report self-protecting against lightning due to their inherent high capacitance?

We encountered a problem of this type recently in the installation of a 10,000-kva capacitor bank on a 44-kv system. The conclusion we reached was that the inherent capacitance of the capacitors supplied the required protection. As this bank was installed at a substation, the question arose as to how other equipment would be protected against lightning, in view of the fact that this bank was of a switched type (on and off during periods of heavy and light load). The station already had lightning protection before the capacitors were installed, and we questioned the thermal ability of the lightning arresters to function properly with the capacitor bank connected. For example, when a lightning arrester on a transmission line is required to function, it may be called on to discharge the entire stored energy of the line, $1/2\ CE^2$. When a lightning arrester at a large capacitor bank functions, it may be called upon to discharge, in addition, a large part of the stored energy of the capacitor bank at an elevated voltage of perhaps double-line voltage. Since the lightning arrester when operating is essentially a resistance, there is an energy dissipation in the lightning arrester blocks proportional to the square of the current through the lightning arrester and its time duration. This may exceed the ability of even modern lightning arresters to function properly without overheating and failing. The grounding of transformer neutrals in the station, if feasible, is, of course, helpful in reducing the thermal duty on the lightning arrester, but this grounding condition cannot always be assumed.

In the case cited in the previous paragraph, we have left the lightning arresters in service at the station and equipped them with surge crest ammeter links to measure lightning arrester discharge currents in normal operation. Since this installation is comparatively new, we have no information to report at this time. We now are attempting to learn by experience what it has so far been impossible to predict by theory, that is, whether the lightning arresters are capable of performing satisfactorily where large capacitor banks are located.

Two questions are offered for consideration: 1. Are large capacitor banks self-protecting against lightning? 2. Does the use of large capacitor banks impose a higher duty on present-day lightning arresters than they can safely discharge, and if so, what is to be done about it?

M. E. Scoville (General Electric Company, Pittsfield, Mass.): The Subcommittee is to be commended for such a factual report on the service experience with shunt capacitors. The report is valuable to the user, pointing up the effects of abnormal service conditions and establishing a normal of experience for individual comparison. It is valuable to the manufacturer, pointing up opportunity for improvements and establishing more accurate data on service conditions.

The report arbitrarily divides time into three periods with a breakdown of failures divided; 25.2 per cent in one year or less, 53.3 per cent in one to five years, and 20.5 per cent after five years. At first glance these percentages seem to indicate about 1/4 of the failures the first year and about 1/10 or less per year for the next four years A more detailed consideration of the basis

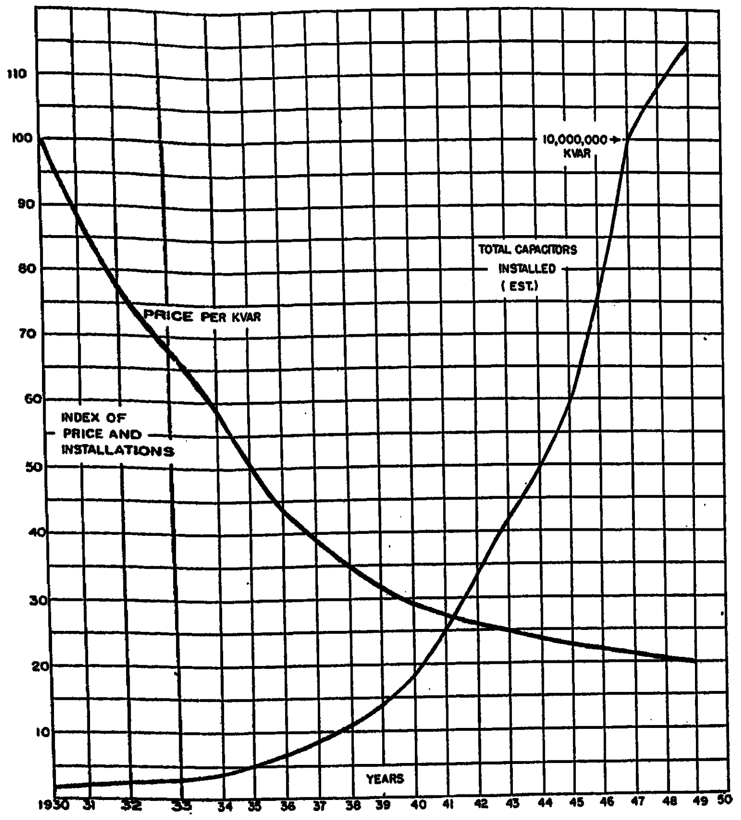


Figure 1. Curves showing price reduction on individual 2,400-volt capacitor units versus years since the introduction of Pyranol and the estimated total reactive kilovolt-amperes of installed capacitors for power factor correction in the United States

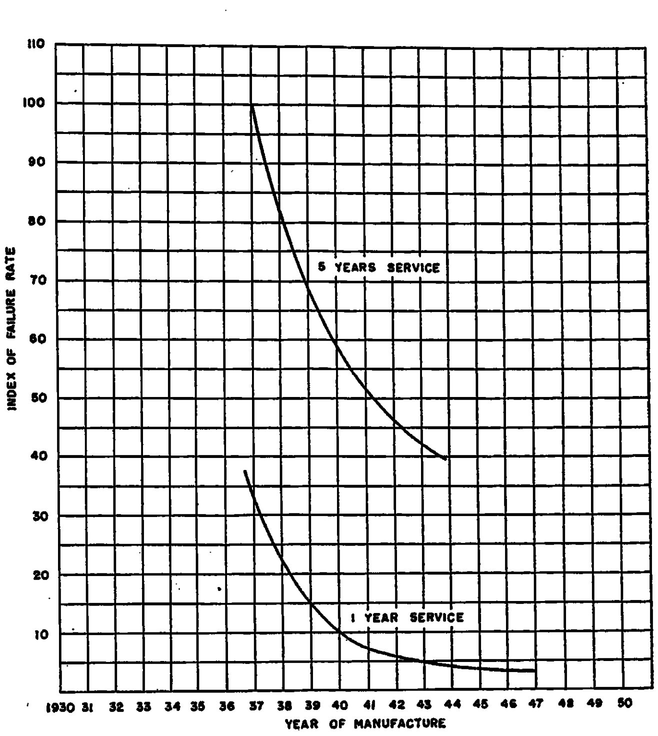


Figure 2. Median curves showing one- and five-year accumulated internal dielectric failure rates for outdoor pole-type capacitors. The number of failures in five years from 1937 installations is assigned an index figure of 100 and the records for the succeeding ten years are plotted in relative values to show a significant trend

for these percentages is interesting. The data from the questionnaire does not disclose the time of installation, thus permitting us to determine the effect of time on the percentage of failures. The report indicates that the rate of installations has increased in the last six years. This indication may be amplified by showing the yearly increase in installations for the period since 1930 as shown by Figure 1 of the discussion. Note that about 3,300,000 reactive kilovolt-amperes was installed by the end of 1942 and 7,500,000 reactive kilovolt-amperes by the end of 1946. If we can assume a corresponding increase in installations for the utilities reporting to the Subcommittee, then the total reactive kilovolt-amperes from which the 1-year failures occurred was in the order of twice that from which the 1- to 5-year failures occurred. I suggest, therefore, that these 1-year and 5-year failure percentages may not be rigorously applied to a single given installation.

Also, another factor adds complexity to our consideration because the record of failures probably was accumulated over an extended period of time. We know the rate of failures is decreasing in recent years. Supporting this fact I have accumulated detail records on internal dielectric failures in outdoor pole-type capacitor units since their introduction in 1937. For each year of manufacture and assumed installation I have plotted curves to show the relationship of failure rate trends as shown by Figure 2 of the discussion. We note that the failure rate within a year after installation has decreased by ten-to-one in ten years. The failure rate within five years has decreased at a slower rate. The fact that failure rates are undoubtedly decreasing with time indicates futility in using a past record of failure rates to predict future trends.

Comparing the slope of these failure rate trends to the price trend in Figure 1 of the discussion, it is interesting to note that the relative decrease in failure rates exceeds that of price trend. Although prices have been decreased, the quality has been increased to insure less and less failures. I agree enthusiastically with the second conclusion in the Subcommittee report, that the manufacturers are striving to improve their product and that the capacitor art is advancing rapidly.

C. E. Parks: The members of the Working Groups who assembled the information included in the report wish to express their appreciation for the fine response and interest which the report promoted. This interest indicated the evident need for such a survey and justifies the Capacitor Subcommittee's decision to proceed with the project.

It was the intent of the Working Groups to obtain information on the subject in a form as complete as possible in order that the data could be further analyzed, segregated, and studied with the thought that those who were interested in certain phases of the problem would be in a position to separate the data required and to draw upon this data for conclusions. It was not the intent of the Working Groups to analyze the collected data further than that required to place it in convenient table form.

Discussions presented at the time of the report's presentation generally indicated the availability of the necessary information in the report and the several discussors added greatly to its value by elaborating on certain portions of the collected data. Of particular interest were the discussions given by Mr. Scoville and Mr. Gross.

With the present rapid development in the field, it is likely that another such survey will be made in the future, possibly within five years.

Report on the Operation and Maintenance of Shunt Capacitors

AN AIEE COMMITTEE REPORT

THIS report is limited to a discussion of shunt capacitors as used on electric power distribution systems for power factor correction and/or voltage improvement. It was the objective of the committee to provide a résumé of the experience and investigational work on this subject which has been made available through various published articles. They hope that this will be helpful to the users of such capacitors in providing them with a relatively short pamphlet giving the pertinent factors which should be considered in their operation and maintenance.

This is intended to be a continuation of a report on shunt capacitors prepared by the General Systems Subcommittee of the AIEE Committee on Power Transmission and Distribution,[1] presented in 1943. It describes the use of shunt capacitors as a means of providing increased capacity of transmission and distribution facilities. The economics of shunt capacitors versus synchronous condensers is explained. For these phases of the problem, the reader is referred to this previous report.

It is intended that this present report provide a résumé of the best current practice in the industry with respect to the operation and maintenance of shunt capacitors. It is not intended to provide sufficient information to properly design and apply capacitor installations. It outlines the basic considerations in operating and maintaining such installations and points the way to other items which the user may wish to investigate in detail.

Many of the problems arising out of capacitor operation are functions of local conditions; such as, the characteristics of the circuits, specific location of the capacitors, proximity and type of communication circuits, and other similar items. These factors, however, frequently have less effect on an individual application than is indicated by theoretical considerations.

Where a specific operating and maintenance practice appears to have general acceptance, this fact has been mentioned. Where there is a clear divergence of opinion among substantial numbers of capacitor users regarding any specific practice, this difference has been stated, and the relative advantages and disadvantages discussed.

This report does not replace instructions and other information provided by capacitor manufacturers. It is suggested that users obtain such information.

Type of Connection

Capacitor units may be installed in 3-phase banks connected in delta or star. For a balanced 3-phase load either connection may be used. The choice usually is determined by other factors, such as size of bank, wave form, and so forth. The relative installed costs of capacitor units and their relative kilovar ratings also will influence the type of connection used.

DELTA CONNECTION

Capacitor units for 2,400-volt circuits are generally delta connected, see Figure 5. Lower voltage units for star connection on 2,400-volt circuits are not standard.

When capacitor units are connected in delta, a faulted unit may be subjected to a high fault current. This may generate sufficient gas to rupture the case before the arc is interrupted. Case rupture usually is prevented when capacitor units are individually fused. Such individual fuses should have interrupting capacity sufficient to clear a phase-to-phase fault.

With a fault in one unit of a capacitor bank and with high arc currents within the unit, the short circuit in the capacitor element may burn clear and permit recovery voltage each half cycle, followed by breakdown at the fault. This charging and discharging of the adjacent capacitor units causes large currents to flow through the terminal leads and current carrying connections. The damage that may result to adjacent units depends upon the restriking nature of the arc and its duration. Individual fuses limit arcing time in the faulty unit thus minimizing the probability of damage to other units in the bank.

In high-voltage delta banks, where each phase is made up of several groups of capacitors in series, the fault currents associated with a unit failure are limited. The use of individual fuses is desirable. The number of units in parallel in a single group should be such as to give satisfactory operation of fuses and avoid excessive voltage across any capacitor unit following the operation of a fuse. This is accomplished by selecting capacitor units with a voltage rating such that there will be a minimum number of groups in series and a maximum number of units in parallel.

STAR CONNECTION

For circuits of 4,160 volts and above, it is usually possible and desirable to star connect the capacitor units, see Figure 6. This permits using capacitor units with the same voltage rating in 3-phase banks and in single-phase installations.

When the neutral of a bank of capacitors is effectively grounded and fault currents are large, restriking arcs within a

---

Paper 49-252, recommended by the AIEE Transmission and Distribution Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 26, 1949; made available for printing September 6, 1949.

Personnel of the working group of the AIEE Subcommittee on Capacitors: E. R. Hendrickson, *Chairman;* Glen Appleman, R. E. Marbury, R. B. Miller, M. B. Scoville, R. A. Wood.

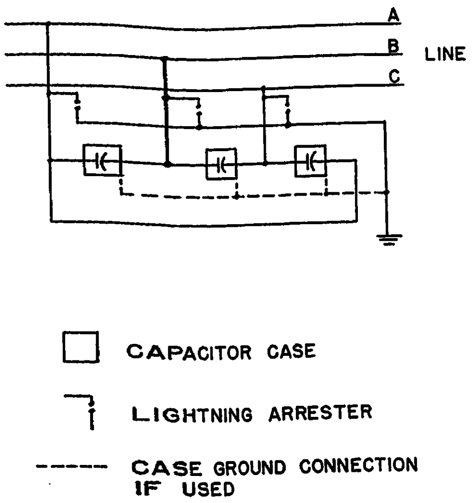


Figure 1. Delta connection diagram

faulty unit can damage adjacent units, as in the case of delta-connected units. This is avoided by the use of individual fuses or by leaving the neutral ungrounded. However, there are installations where grounding the neutral has distinct advantages. These are as follows:

1. In small banks, individual fuses are usually not used. If the neutral is not grounded, a fault in a capacitor unit would not blow the main bank fuses and the resulting unbalanced voltages would be excessive.
2. Capacitor units are designed to be operated with a grounded neutral when star connected. This frequently permits using units having a lower insulation level than that corresponding to the circuit voltage. When the neutral is not grounded the insulation level of the capacitor units should correspond to the circuit voltage.
3. The operation of an individual fuse in one phase of small bank will not cause a serious voltage unbalance when the neutral is solidly grounded.
4. In some cases it is desirable to provide a low impedance path for the control of harmonic voltage levels on the system. A capacitor bank with neutral grounded provides this path.

If the number of units per phase in a large capacitor bank is sufficient to insure satisfactory operation of individual fuses, it is preferable to star connect the capacitor units and isolate the neutral. This has several advantages as follows:

1. The fault current within a faulty unit is limited thus reducing the possibility of rupturing the unit.
2. Fuses with low rupturing capacity can be used.
3. In the event of failure of a capacitor unit, damage to adjacent units is minimized.
4. The ground return path for harmonic currents is eliminated. This usually reduces inductive interference.

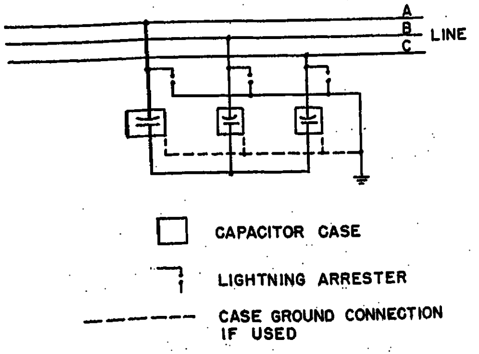


Figure 2. Star connection-floating neutral diagram

Factors Influencing Choice of Connection

In ungrounded star connected banks where each phase consists of several groups of units in series, it is desirable to choose capacitor units with voltage ratings so that each parallel group contains a large number of units. A bank consisting of three groups in series per phase, each group consisting of 18 7,200-volt units in parallel, is far more desirable than nine groups in series with each group consisting of 6 2,400-volt units in parallel. The latter arrangement results in too few units in parallel for proper functioning of individual fuses. Also the voltage across the remaining units of a group in which a fuse has blown will become excessive. These relations are more completely covered under Protection of Distribution Systems Against Capacitor Failures.

Operating and Ambient Temperatures

Effect of Temperature on Capacitor Ratings

Capacitors have exceptionally low losses when compared with other electric equipment with equal kilovolt-ampere rating. The losses of modern capacitors do not exceed 3.3 watts per kilovar of rating. Low losses give low operating temperatures, which are desirable for the following reasons:

1. Life in service is a function of temperature and voltage stress.
2. Capacitors when energized at rated voltage are always operating at full load. System load cycles have no effect on capacitor losses.
3. Capacitors are designed to operate with a maximum internal temperature near their temperature stability limit. It is imperative that there be no occasional conditions of operation above this internal temperature stability limit.

Capacitor units differ from most other electric equipment in that the container is permanently sealed and there is no easy way of measuring internal operating temperatures in service. Because kilovar loading is dependent on circuit voltage, an operator has no control of capacitor loading.

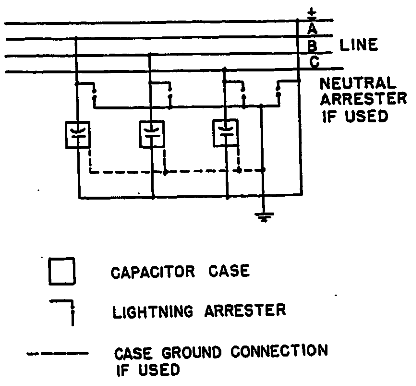


Figure 3. Star connection-ungrounded neutral diagram

The maximum operating temperature rise of capacitor units can be divided into two components. The external surface of the container operates with a rise above ambient which can be measured readily in service. The internal dielectric operates with a temperature rise above case surface. This cannot be measured in service. The dissipation of losses through a large number of layers of paper and foil is slow. Therefore there is an appreciable temperature gradient between the internal parts of a unit and the case.

Following are several of the factors which affect operating temperatures:

1. The maximum allowable kilovar loading (AIEE Standard) is 135 per cent of rating, including the effects of both fundamental and harmonic voltages. Temperature rise increases with kilovar loading.
2. Mounting arrangements and spacings between capacitor units affect their temperature rise. When units are mounted in rows, side by side, the heat dissipation is restricted, thus causing an increase in the temperature rise. When rows of units are mounted one over another, the air from a lower tier is heated before passing into an upper tier. Even with unrestricted natural circulation of air higher tiers will operate at

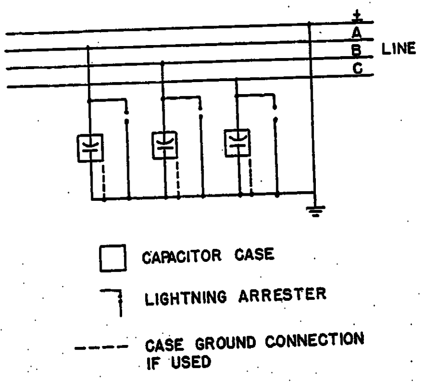


Figure 4. Star connection-multigrounded neutral diagram

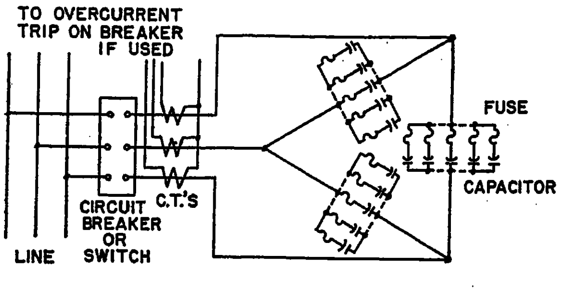
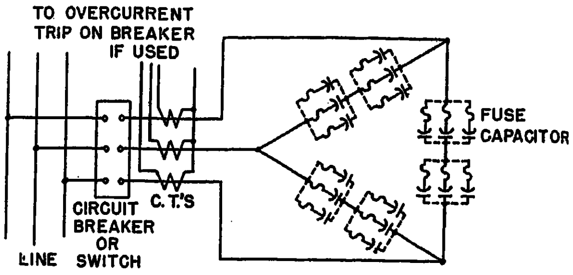


Figure 5 (left). Capacitor protection by individual fuses-delta connection

Figure 7 (right). Series banks of capacitor units protected by individual fuses-delta connection



higher temperatures than lower tiers. The effect of unit spacing, tier stacking, and enclosures as determined by one manufacturer is shown in Table I. These values are in terms of per cent case temperature rise. The 100 per cent case is a single isolated unit operating at nominal rating in a large room. The arrangement assumes three tiers of units, two rows wide in a sheet-metal enclosure with top and bottom ventilation openings to provide natural circulation of air.

Table I

Spacing Between Case Sides, Inches	Per Cent Case Temperature Rise of Units		
	Bottom Tier	Middle Tier	Top Tier
0.5	191	216	233
1	161	181	198
2	136	155	170
4	123	140	153
8	118	135	148

3. Radiation from the sun and other bodies operating at higher temperatures than the surrounding ambient increases the temperature rise in a capacitor. For example, one manufacturer has determined that the internal temperature of a single isolated capacitor unit is increased three degrees centigrade when it is subjected to the direct rays of a bright sun.

4. Enclosures such as housings or vaults may increase the temperature rise over that obtained with open-type installations due to reduction in radiant heat dissipation.

5. Restriction of natural circulation of air such as by housings increase the capacitor unit temperature rise.

Normally the maximum surface temperature rise above ambient of a capacitor unit indicates its operating condition. AIEE Standard number 18[2] specifies that the case temperature rise should not exceed 25 degrees centigrade above an ambient of 40 degrees centigrade thus giving a maximum permissible case temperature of 65 degrees centigrade. For the usual operating conditions it is ordinarily desirable that the maximum case temperature does not exceed 55 degrees centigrade and for abnormal or emergency conditions that it does not exceed 70 degrees centigrade.

Experience as reported by one manufacturer indicates that capacitor units may be operated at the maximum ambient temperatures shown in Table II without incurring an inordinate decrease in capacitor life.

Table II

Arrangement	Maximum Ambient Temperature, Degrees Centigrade	
	15 Kvar Units	25 Kvar Units
Outdoor, single row, single tier, unrestricted ventilation	50	46
Outdoor, two rows, two or three tiers, unrestricted ventilation	46	40
Indoor, two rows, single tier, unrestricted ventilation	40	40
Indoor or enclosed, two rows, two or three tiers, unrestricted ventilation	40	40*
Enclosed outdoor or indoor, multiple rows and tiers with forced ventilation and forced circulation about units**	46	40

* Forced circulation of air about unit surfaces is necessary.

** The forced ventilation is considered to be eight cubic feet per minute, per kilovar, per degree centigrade rise with a 50 degree centigrade maximum air outlet temperature.

These ambient temperatures are based on a minimum center to center horizontal spacing between units of approximately $7^{1}/_{2}$ inches.

Steps to Correct Over-Temperature Operation in the Field

High operating temperatures may be decreased by one or more of the following:

1. Increase spacing between units.
2. Reduce operating voltage.
3. Reduce harmonic voltages.
4. Use forced air cooling.
5. Move capacitor bank to a location of lower ambient temperature.
6. Install capacitor units having a higher ambient rating.

Extremely Low Ambient Temperatures

It is important to recognize that the electrical and mechanical properties of capacitor materials may change at low temperatures. The synthetic liquids commonly used in capacitors at the present time become practically solid at temperatures below approximately −10 degrees centigrade. The solid state alone does not adversely affect the electrical properties. However, very recent experience has shown that such liquids do become brittle enough to fracture at temperatures in the range of −20 degrees centigrade, and below. Low temperatures combined with mechanical shock and stress increases the probability of electrical failure. Experience has shown that a few failures have occurred soon after the capacitors were energized after being out of service overnight, or extended periods, at low ambient temperatures.

A synthetic liquid-filled capacitor has a capacitance about 70 per cent of normal when de-energized for several hours in an ambient temperature of −15 degrees

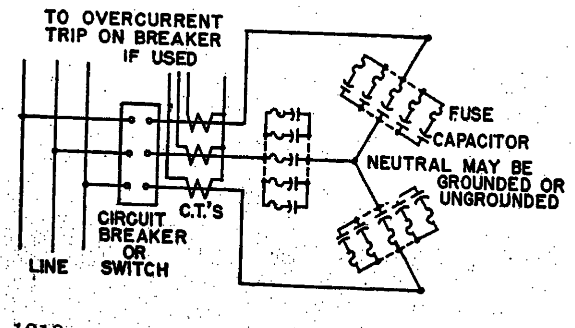
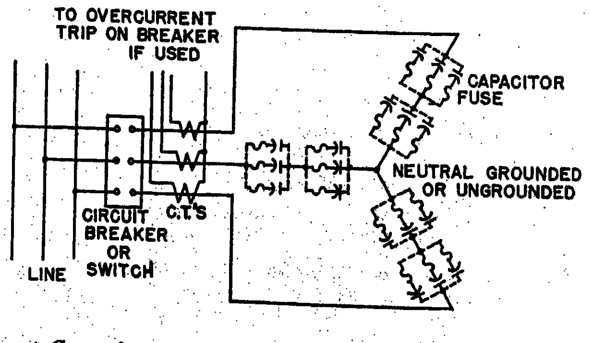


Figure 6 (left). Capacitor protection by individual fuses-star connection

Figure 8 (right). Series banks of capacitor units protected by individual fuses-star connection



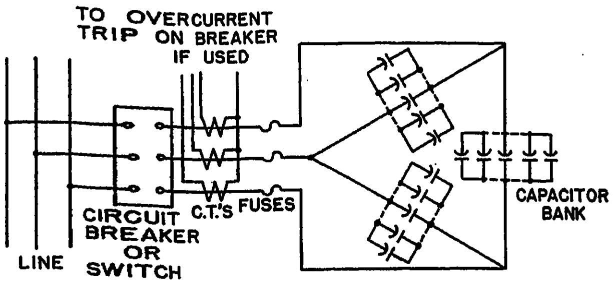
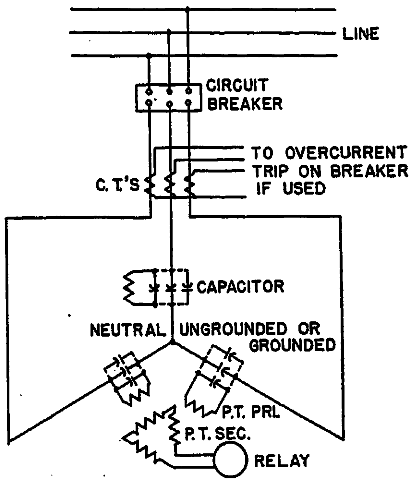

TO OVERCURRENT TRIP ON BREAKER IF USED
CIRCUIT BREAKER OR SWITCH
C.T.'S
FUSES
LINE
CAPACITOR BANK

Figure 9 (left). Capacitor protection by group fuses-delta connection

Figure 11 (right). Capacitor protection by voltage unbalance between phases


LINE
CIRCUIT BREAKER
C.T.'S
TO OVERCURRENT TRIP ON BREAKER IF USED
CAPACITOR
NEUTRAL UNGROUNDED OR GROUNDED
P.T. PRI.
P.T. SEC.
RELAY

centigrade or below. The kilovar output will be correspondingly low immediately after the capacitor is energized. The losses of a capacitor go through a very sharp peak at approximately −4 degrees centigrade. As soon as a cold capacitor is energized, its internal temperature rises rapidly until it reaches a stable value corresponding to some point on the downward slope of the loss curve as shown by Figure 18. The capacitance and kilovar output are normal at this stabilized operating point. The kilovar output will not drop below the rated value when the capacitor is in continuous operation at temperatures as low as −40 degrees centigrade.

It is, therefore, to be noted that continuous operation at low temperatures is not objectionable. However, intermittent operation in which capacitors are frequently energized at temperatures below −20 degrees centigrade may give trouble unless special precautions are taken.

Maximum Voltage Limitations

EFFECT OF OVER-VOLTAGE WAVE FORM

Standard capacitor units are designed with a 35 per cent thermal margin. Thus the maximum allowable kilovar loading is 135 per cent of nameplate rating. The operating kilovar of a capacitor increases as the square of the voltage and directly with frequency.

Capacitor units are designed to operate at an average voltage (over any 24-hour period) not exceeding 105 per cent of their rating. The average operating kilovar on this basis will not be over 110 per cent of their rating. The remaining 25 per cent thermal margin is available for excess operating kilovar due to harmonic currents. The total margin of 35 per cent may be made up of both fundamental and harmonic components in any proportion so long as the measured root-mean-square voltage does not exceed 105 per cent on the basis of a 24-hour average, and 110 per cent on a short time basis and the limit of 135 per cent output kilovar is not exceeded.

Since capacitor current depends upon frequency as well as voltage, merely measuring the line voltage and current with commercial instruments and multiplying these values together may not give the actual operating kilovar unless the voltage follows a sine wave.

Where the voltage contains one or more harmonic components, the actual kilovar at which the capacitor is operating must be calculated separately for each harmonic, and these kilovar values added to obtain the total operating kilovar.

Applications of most concern are those where resonance does exist for some particular harmonic. If the apparent kilovar determined by multiplying the total voltage and current is within the 135 per cent limit, and the average voltage is within 105 per cent, it may safely be concluded that the capacitor is not overloaded thermally because such a procedure is in error in the conservative direction. Where thermal overload is indicated, it then is advisable to actually determine the current and voltage for the predominating harmonic, and actually compute the operating kilovars at both the harmonic and fundamental frequency, and determine the total operating kilovars.

The harmonic frequencies usually encountered are the third and fifth. It can be shown that when the operating voltage is 105 per cent and the third harmonic is present and predominating, the total current may reach 145 per cent of normal before exceeding the 135 per cent thermal limit. On the same basis with the fifth predominating, the total current may reach 160 per cent of normal before exceeding the 135 per cent thermal limit.

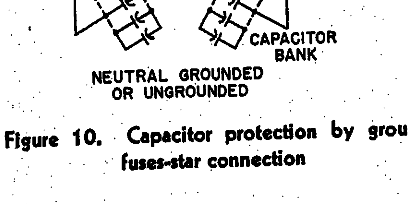

LINE
CIRCUIT BREAKER OR SWITCH
C.T.'S
TO OVERCURRENT TRIP ON BREAKER IF USED
FUSES
CAPACITOR BANK
NEUTRAL GROUNDED OR UNGROUNDED

Figure 10. Capacitor protection by group fuses-star connection

There are relatively few cases where harmonic currents interfere with the use of shunt capacitors. It is the usual practice to ignore these currents when installing capacitors, taking corrective steps only if trouble is encountered. When it is known that the wave form is bad, care must be taken in planning the location of the capacitor units on the system, and in choosing the size of bank for a given location. A particular capacitor bank may be deliberately planned to operate in a predetermined manner with respect to harmonics so as to bring about a change in the level of harmonic voltage components on the system. A superimposed high-frequency current on the system is sometimes used to operate relays which control street lighting circuits, water heaters, and so forth. While this current is not harmful to the capacitors due to its short duration, it may occasionally blow capacitor fuses.

When a particular installation of capacitors is overloaded due to harmonic currents, the overload can be reduced by:

1. Changing the size or relocating the bank to offer increased impedance to the flow of the harmonic currents.
2. Adding reactance to the circuit supplying the capacitor.
3. Adding a device offering high impedance to the harmonic, yet low impedance to the fundamental such as a wave trap.

It should be recognized that the installation of a few banks of capacitors here and there on a system having poor wave form may introduce some problems due to thermal overloading of capacitors. However, as more and more capacitors are added, the thermal overload on any single bank may become insignificant. Remedial measures should be considered only in connection with the entire capacitor program.

OTHER EFFECTS OF OVER-VOLTAGE

Although short time over-voltage transients are objectionable, sustained over-voltages are more serious because they affect the operating kilovar of a capacitor unit and increase its operating temperature. When capacitors are connected to a system they tend to raise the system voltage. This increased voltage is superimposed on that voltage which would exist if the capacitor were not connected. Since the level of system voltage may rise when load is decreased, this addition may carry the actual voltage to a point higher than the limitations given in the previous section.

EFFECT OF CAPACITORS ON SYSTEM VOLTAGE

The rise in circuit voltage when a capacitor is connected to a circuit is dependent on the capacitor kilovar and the circuit reactance. The rise through each transformer may be computed by multiplying the transformer per cent reactance by the ratio of capacitor kilovar to transformer kilovolt-amperes.

The rise in circuit voltage due to feeder reactance on ordinary types of open-wire distribution can be approximated as follows:

$$\text{Per cent rise in voltage} = \frac{CKVAR \times FT \times F}{1{,}000{,}000}$$

$CKVAR$ = total capacitor kilovar in bank
FT = feet distance from substation bus to point on the feeder where the capacitor is connected
F = factor shown in Table III which is based on average circuit characteristics.

When capacitors are installed with a total kilovar equal to the minimum reactive requirements, it usually is practical to leave them permanently connected without encountering objectionable light load voltages. When maximum possible benefits are being obtained and sufficient kilovar of capacitors is being used to meet heavy load reactive requirements, it usually is necessary to switch the capacitors in relation to changes in load conditions or system voltage. In some cases, the voltage on the system is controlled by switching capacitors. It may be necessary to switch a large bank of capacitors in two or three steps with changing load to prevent excessive voltage variation.

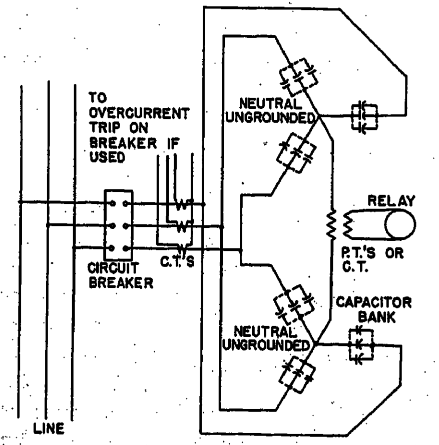


Figure 12. Capacitor protection by unbalance between neutrals

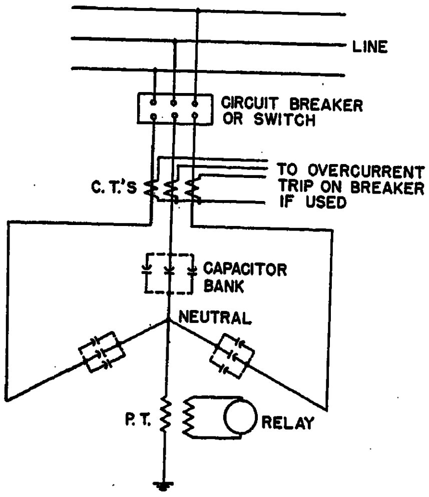

LINE
CIRCUIT BREAKER OR SWITCH
C.T.'S
TO OVERCURRENT TRIP ON BREAKER IF USED
CAPACITOR BANK
NEUTRAL
P.T.
RELAY

Figure 13. Capacitor protection by voltage unbalance between neutral and ground

When the circuit load follows a definite cycle each day, time- or manually-operated switches are frequently used to switch capacitors. For automatic switching of capacitors, voltage sensitive devices are usually used, although current and kilovar sensitive devices also are used.

With carrier current in a circuit, reactors must be used in series with shunt capacitors. These reactors limit the flow of carrier current through the capacitors and also may increase the voltage across the capacitor. In such cases it may be necessary to use capacitors having a higher normal voltage rating. The use of a wave trap in place of a reactor does not result in appreciable boost in voltage on the capacitors and is preferable for large banks.

Table III

Feeder Voltage	Factor F for Open Wire Feeders Three Phase	Two Phase Four Wire	Two Phase Three Wire	Single Phase
2,400	2.24	2.24	2.24	4.48
4,160	0.75	0.75	0.75	1.50
4,800	0.56	0.56	0.56	1.12
7,200	0.25	0.25	0.25	0.50

Abnormal Currents

CAUSES

Capacitors are rated in fundamental frequency, voltage, and kilovars. Harmonic voltages occur in most power systems and, although usually small, cause harmonic currents to flow. Therefore, when capacitor currents are measured under service conditions, the value read is usually in excess of the nominal value obtained from rated kilovar and rated voltage. The excess current may be due to:

1. The plus capacitance tolerance provided by the manufacturer.
2. Application of more than rated voltage at fundamental frequency.
3. Harmonic voltages.
4. Over-currents such as those caused by switching, arcing in supply circuit, and other system disturbances.

REMEDIAL MEASURES

1. Plus capacitance tolerance provided by the manufacturer will produce over-currents. Although there are no remedial measures, these over-currents are usually small enough to be of little consequence.

2. The application of more than rated voltage at fundamental frequency will result in over-currents which will not be excessive if established limits for over-voltage operation are followed (see Maximum Voltage Limitation). Excessive over-voltage at fundamental frequency is generally the result of improper setting of transformer taps or voltage regulators. Remedial measures include adjustment of transformer taps or voltage regulator settings. If the capacitors themselves are contributing to the over-voltage due to the rise at light load, it may be necessary to switch part or all of the capacitors. Relocation of a capacitor bank also may alleviate over-voltage conditions where the system will permit such relocation.

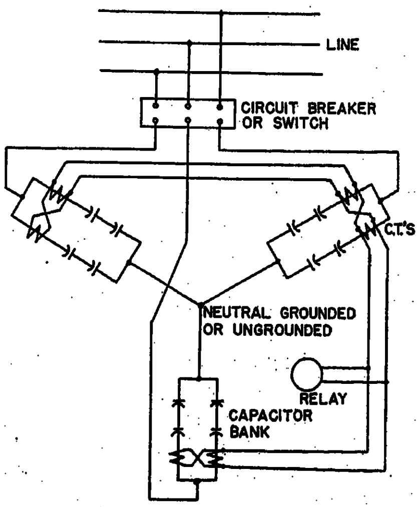


Figure 14. Capacitor protection by differential current transformers in parallel

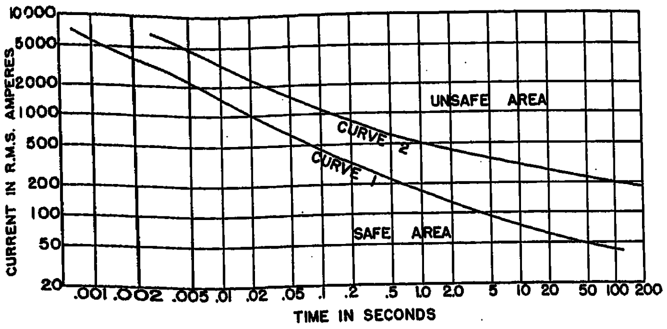


Figure 15. Current versus time for a short-circuited capacitor unit to rupture due to gas pressure after an internal short circuit occurs

Area below curve 1 is safe with no damage expected beyond slight swelling of case. Area between curve 1 and curve 2 is safe practically for many conditions of installation, although occasional crack in a seam or bushing seal may occur. Area above curve 2 is unsafe due to rupture occurring frequently with sufficient violence to damage the adjacent units

3. Harmonic voltages produce harmonic over-currents. Several things may cause harmonic voltages. Their order of importance, with suggested remedies where applicable, is as follows:

(a). Transformer magnetizing currents cause a distortion of the impressed voltage wave due to the saturation of the magnetic core. The third harmonic is the predominant component in this magnetizing current. Delta-connected transformer windings circulate these third harmonic currents and prevent them from getting out on the system. These delta windings also reduce the multiples of the third harmonics such as the 9th, 15th, and so forth. Therefore, odd harmonics such as the 5th, 7th, 11th, and so forth, are usually those which appear on the system. The harmonic currents flowing through the circuit reactance produce harmonic voltages on the line. The voltages are equal to the harmonic current times the circuit reactance at the frequency of the harmonic current. These harmonic voltages appear at various parts of the system, depending on the system reactance and magnitude of current flowing. The most direct and simple method of reducing harmonic voltages arising from transformer magnetizing current is to reduce any over-excitation of the transformers in the affected areas.

(b). Rectifiers cause harmonic voltages and currents to appear in the supply circuit. They usually cause inductive interference with communication circuits instead of overloading capacitors. Resonant shunts usually will eliminate these harmonic currents.

(c). Generators and motors do not generate a perfect sine voltage wave. This source of trouble is not serious in rotating equipment of modern design. It has been encountered with very old equipment, particularly water wheel generators.

(d). Insulator leakage and corona causes intermittent currents to flow to ground. This intermittent flow of current distorts the voltage and current waves of the circuit and is manifested as harmonics.

(e). Arc lamps and arc furnaces may distort the voltage wave shape due to their change in impedance during various parts of the voltage cycle.

(f). Transformer banks operated in conjunction with capacitors may cause harmonic resonance. The capacitor kilovar which will produce this condition when operated with transformer banks of equal rating is three times as large for delta as for open delta transformer banks. Therefore, it may be necessary to limit the size of capacitor banks operated adjacent to open delta transformer banks.

Harmonic over-currents due to resonance or partial resonance to a harmonic frequency, irrespective of cause, can be reduced by relocation of the capacitors to eliminate or reduce the resonant condition.

When harmonic over-currents exist at light load periods, they can be eliminated by switching the capacitors.

Harmonic over-currents which cannot be satisfactorily reduced by other means may require suppression with reactors or wave traps. Since harmonic voltages rarely overstress a capacitor, it is only necessary to slightly alter the circuit constants to disturb the resonant condition. This may be done by adding reactors in series with part or all of the capacitor bank. The frequency and magnitude of the offending harmonic must be known in order to apply the proper remedial measures.

4. The arcs resulting from switching or other system disturbances may produce over-currents at frequencies above the fundamental. If the circuit is resonant at this frequency capacitor over-currents may result. These usually may be reduced by moving the capacitors to eliminate the resonant condition.

Ability of Capacitors to Carry Over-Currents

Capacitor units are designed with ample latitude for over-currents encountered in service consistent with the over-voltage limitations. (See section on Maximum Voltage Limitations.) Since capacitor impedance varies inversely with frequency, the allowable current may be comparatively large at higher harmonics. Circuit breakers, current transformers, and other equipment in series with capacitors must therefore have a current carrying capacity in excess of the nominal capacitor current. Experience has shown that for ordinary operating conditions such equipment is usually satisfactory if it has a current carrying capacity equal to 135 per cent of the capacitor rating.

Unless history of previous installations indicate a disturbing harmonic condition, advance study usually is not warranted. Excess harmonic currents are frequently less troublesome in actual practice than may be indicated by a theoretical study. In those rare instances where such difficulties are encountered, the trouble usually can be readily corrected.

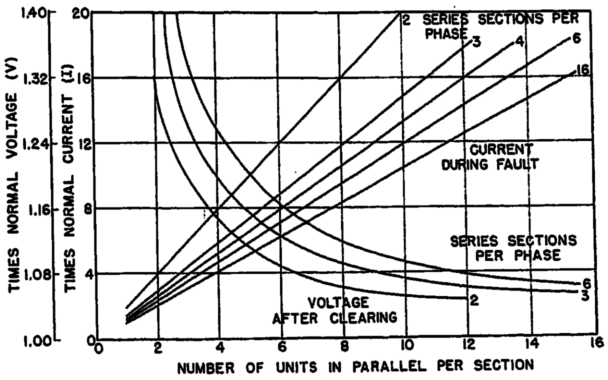


Figure 16. Current and voltage during or after failure of a capacitor unit in series parallel bank-delta or grounded star connection

Inductive Co-ordination

Causes of Inductive Interference

Shunt capacitors usually are installed in balanced 3-phase banks. The connection may be line-to-ground, line-to-line, or ungrounded star. Capacitors connected line-to-line or in ungrounded star can in no way affect any residual (neutral to ground) voltage that may be on the system. They can only affect the balanced harmonics, and these are less important.

When capacitors are connected line-to-ground, the capacitive and inductive reactances for the existing residual voltage may amplify the residual voltage and current. Even this condition may not cause trouble for the following reasons:

1. Modern communication equipment is not sensitive to 180-cycle current which is the third harmonic residual current of the usual power system. The telephone interference factor for 180 cycles is 15, compared with 205 for 300 cycles and 4,400 for 1,500 cycles.

2. Even though circuit constants are such as to produce resonance, the third harmonic voltages usually existing in a system are so small that trouble would not result.

The approximate computed rise of the fundamental voltage when capacitors produce resonance are as follows:

Order of Harmonic	Approximate Per Cent Voltage Rise
3	5.0
5	4.0
7	2.0
9	0.6
11	0.8

The capacitors applied usually will give at least a 2 per cent voltage rise which corresponds to a resonant condition for the seventh harmonic. This is a balanced component and rarely causes trouble.

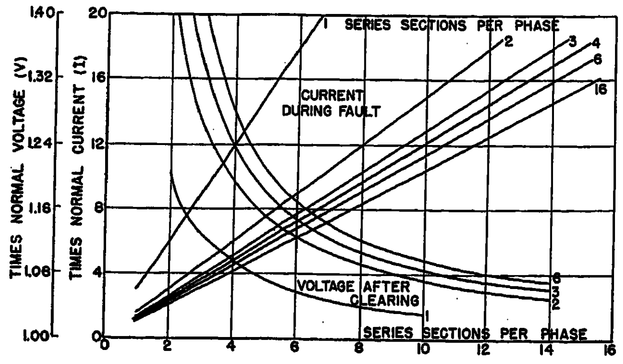


Figure 17. Current and voltage during or after failure of a capacitor unit in series parallel bank-ungrounded star connection

The ninth harmonic current circulates in the transformer delta windings and therefore does not cause trouble. The amplitudes of higher harmonic voltages are small and may be neglected.

When capacitors are connected line-to-ground on a 4-wire 3-phase distribution system, the fifth harmonic voltage is usually responsible for the few instances where inductive interference is experienced. This is caused by unequal amount of transformer kilovolt-amperes per phase which allows some residual fifth harmonic magnetizing current to appear. Since residual currents cause most telephone interference, resonance with the fifth harmonic current is a potential source of trouble.

The communication industry has co-operated in solving these problems and they recognize that capacitors represent an advance in the art of power distribution. In the last decade, the communication equipment has been improved so that it is very insensitive to the lower harmonics. This has further minimized inductive interference.

Remedial Measures

The general solution of inductive interference is one of co-ordination between the types of circuits and equipment involved.

Some of the more common remedial measures for solving these problems are as follows:

1. Relocate the capacitors.
2. On a multigrounded system where the neutrals of the capacitor banks are normally connected to ground, some of these ground connections may be removed and the capacitor neutrals allowed to float. This reduces the magnitude of the residual ground return current.
3. Capacitor banks may be connected delta in place of star on multigrounded circuits, thereby limiting the flow of harmonic current in the ground return.
4. Gaps or reactors may be placed between the neutral connection of the capacitor bank and ground to prevent the flow of harmonic ground return currents.
5. Wave traps or filters may be used to block residual harmonic currents. The nature of exposure, source and magnitude of the harmonic current, and type of power circuit must be considered in the design of the filter or wave trap.
6. The type of construction of communication circuits determines their susceptibility to inductive interference. Cables with grounded sheaths are practically free from interference. Two-wire communication circuits are very much less susceptible to interference than those using one wire and a ground return. Communication circuits with high-impedance ringers are much less susceptible to interference than are those with low-impedance ringers.

The probability of capacitors increasing the inductive interference on communication circuits is small and if it occurs, well-known remedial measures can be taken. Full co-operation with communication groups is recommended. Communication engineers are well informed on the best methods of minimizing inductive interference.

System Stability

Usually shunt capacitors may be added to a system without affecting system stability. When capacitors supply kilovars they reduce the excitation required on the system generators to maintain the voltage level. The system stability margin is reduced with lower generator excitation. This usually happens during periods of light load especially when large numbers of capacitors have been gradually added to the system.

The number of fixed capacitors which may be applied to a system is limited by other capacitive kilovar on the system and the minimum allowable excitation of the generators. Other sources of capacitive kilovar may be shunt capacitors already in use, synchronous condensers and motors, and high-voltage circuits of the system.

The algebraic difference between inductive and capacitive kilovars in conjunction with the kilowatt load on the generators determines the power factor at which they operate. The power factor and kilowatt load at which a generator operates determines its excitation and hence its stability limit.

No generally applicable limits can be given of permissible generator power factors because local conditions determine the required margin of generator pull-out torque. Operation of machines up to 95–98 per cent lag usually has been satisfactory, although under some conditions lower power factors may be necessary for stable operation. However, higher power factors may be feasible on some systems.

All generators should be operated with a pull-out torque margin sufficient to allow them to pick up their share of sudden load increases, such as might occur when a major source of power is lost. The pull-out torque margins also must be large enough to maintain stability during reductions in system voltage produced by large increases in reactive requirements. This might be caused by the loss of excitation on a large generator unless "loss of field" relays are provided. Reasonably fast automatic regulation of the excitation will in many cases act to increase generator pull-out torque during emergency conditions.

Generators of old design may be subject to an excessive temperature rise in the end structure of the stator if operated with approximately rated stator current and low field current. Most modern gen-

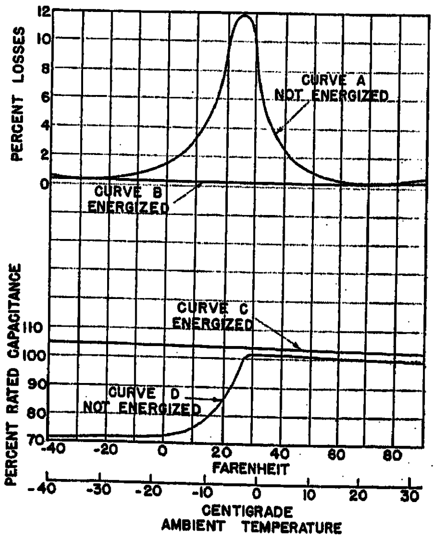


Figure 18. Typical loss and capacitance variations in synthetic liquid filled capacitors

Curve A shows the per cent losses of a capacitor upon energizing after being out of operation for an extended period. Curve B shows the per cent losses of a capacitor unit in continuous operation. Curve C shows the per cent of rated capacitance of a capacitor in continuous operation. Curve D shows the per cent of rated capacitance of a capacitor not energized for an extended period erators may be operated without excessive heating of stator iron at loads of approximately 60 per cent rated kilovolt-amperes at 0 per cent leading power factor; 93 per cent rated kilovolt-amperes at 90 per cent leading power factor; and 100 per cent rated kilovolt-amperes at unity power factor. If operation near these limits is expected, it is advisable to check the characteristics of the individual generators.

On some systems it may be necessary to switch part of the capacitors off at light load to maintain the required margin of system stability. Usually when shunt capacitors introduce a stability problem, other problems, such as high voltage on station busses or circuits, will be encountered. The solution of these other problems will usually correct the stability problem.

Protection

LIGHTNING

Capacitor units are vulnerable to lightning disturbances. These disturbances may cause bushing flashover or breakdown the insulation between the internal elements and the case. Lightning arresters are usually installed when capacitor units are connected phase-to-phase; line-to-ungrounded-neutral; and line-to-unigrounded-neutral. If the case is grounded, the lightning arrester ground should be connected to the case with the shortest possible lead.

Capacitor units connected line-to-neutral on a multigrounded neutral system provide a low impedance path for lightning surge current and some protection from over-voltage due to lightning. Star connected capacitor banks with the neutral point ungrounded provide some degree of protection when a suitable lightning arrester is placed between the neutral point of the bank and ground. However a capacitor unit operated in either of the above arrangements may be vulnerable to damage by lightning when the magnitude and duration of lightning surge is sufficient to charge the capacitor to a high voltage which will damage the insulation, or when a steep front high magnitude voltage wave is not absorbed by the capacitor in sufficient time to prevent damage to the insulation.

Therefore, it is good practice to install lightning arresters on capacitors connected phase-to-multigrounded-neutral. However, advantage is taken of this inherent self-protective feature and capacitors connected phase to multigrounded neutral are sometimes operated without lightning arresters.

Diagrams of typical connections are shown in Figures 1, 2, 3, and 4.

TRANSIENT OVER-VOLTAGE

Transient over-voltages may be caused by switching operations, arcing grounds, discharge of lightning arresters, resonance or near resonance caused by motors while starting, and other similar causes. Such voltage disturbances are of short duration and usually are of lower magnitude than those caused by lightning. Lightning arresters will ordinarily provide adequate protection against such transient disturbances. Unusual cases may require special remedies.

Protection of Distribution Systems Against Capacitor Failures

In case of capacitor failures, it is desirable to isolate the damaged units from the circuit without interrupting service. This requires co-ordination between the capacitor protective equipment and other protective equipment. For most installations, it is possible for the power system to continue in operation with part or all of a capacitor bank out of service.

CIRCUITS PROTECTION REQUIREMENTS AGAINST CAPACITOR FAILURES

Properly co-ordinated protective device will:

1. Disconnect a short-circuited capacitor before the case ruptures, damaging adjacent equipment or causing personal injury.
2. Disconnect a damaged capacitor without causing operation of other circuit protective devices.
3. Disconnect a damaged capacitor unit or group of units with minimum loss of kilovars.
4. Not disconnect a capacitor when subjected to non-damaging system disturbances.
5. Indicate the location of a failed unit in a large bank.
6. Disconnect a capacitor unit which is arcing internally in order to minimize damage to adjacent units due to their repetitive charging and discharging.

TYPES OF PROTECTION

The more common types of protection in approximate order of their importance are:

1. Fuses for individual units, see Figures 5, 6, 7, and 8.

Table IV. Summary of the Discussion of the Types of System Protection Against Capacitor Failures

Type of Protection	Figure Number	Function of Type of Protection May Prevent Case Rupture*	No Affect on Other System Prot. Devices	Limits Kvar Loss	Requires Immediate Maintenance	Insensitive to External Disturbances	Indicated Failed Unit Location
1. Individual fuses	5–6–7–8	Yes	Yes	Yes	No	Yes	Yes
2. Group or bank fuses	9–10	Yes	Yes	Yes	Yes	Yes	No†
3. Line current transformers and overcurrent relays		Yes	Yes	No	Yes	Yes	No
4. Voltage unbalance between phases	11	Yes	Yes	No	Yes	Yes**	No
5. Voltage unbalance between neutrals of double star bank	12	Yes	Yes	No	Yes	Yes	No
6. Voltage unbalance between ungrounded neutral of star bank and ground (usually permissible only on grounded system)	13	Yes	Yes	No	Yes	No	No
7. Differential current transformers in parallel	14	Yes	Yes	No	Yes	Yes	No
8. Reduced conductor size—limited to 240 volts maximum		No	No‡	Yes	Yes§	Yes	No

* Depends on time current curve of protective device which should be co-ordinated with the curve of Figure 15.

† Group fusing will not indicate individual unit failure.

‡ Depends on co-ordination with other system protective equipment.

§ Depends on extent of damage if any due to case rupture.

** Except when neutral is grounded.

2. Fuses for groups or banks of units, see Figures 9 and 10.

3. Line current transformers operating direct trip coils or over-current relays to trip circuit breaker.

4. Potential transformers connected across each phase or part of each phase to trip a circuit breaker on phase voltage unbalance, see Figure 11 for star ungrounded neutral banks. This arrangement also may be used for delta or star grounded neutral banks which contain two or more series sections per phase.

5. Potential or current transformers connected between ungrounded neutrals of two star banks operating a relay to trip a circuit breaker, see Figure 12.

6. Potential transformer connected between neutral and ground of a star ungrounded neutral bank operating a relay to trip a circuit breaker, see Figure 13.

7. Differentially-connected current transformers in parallel capacitor groups, operating a relay to trip a circuit breaker.

8. Reduced conductor section to burn open with over-current.

COMPARISON OF VARIOUS TYPES OF PROTECTION

Table IV is a summary comparing the eight types of protection previously mentioned. A detailed discussion of each follows:

1. Fuses on individual units are used most extensively because they provide the maximum number of desirable features. When fault currents through shorted capacitors are excessive, individual fuses are desirable to avoid case rupture and also to avoid possible damage to adjacent units due to their repetitive charging and discharging.

In addition to the normal considerations followed in the general application of fuses (see section on Abnormal Currents) they should have a time current characteristic to successfully withstand the high charge and discharge currents associated with switching transients.

The interrupting duty of individual fuses may be reduced by the use of parallel groups of capacitor units connected in series between lines or the use of star connected units with the bank neutral ungrounded. In case of unit failure, these arrangements provide other units in series with the faulted unit and thereby limit the fault current. Properly selected capacitor units with individual fuses may be connected in series providing the following conditions are met.

(a). The number of capacitor units in parallel contribute to the fault current which will flow through a fuse connected to a shorted unit. Sufficient units should be used to provide a fault current which will clear the fuse before the case ruptures. This will usually require a fault current several times the current rating of the fuse. Figure 15 shows the relationship between current and the time required to rupture the case as determined by one manufacturer. The number of units in each parallel group must be increased when the number of parallel groups in series is increased. Figures 16 and 17 show the fault current through a failed unit for various series parallel combinations. These curves also show the maximum voltage that will exist across the remaining units of a parallel group after a failure in this group.

(b). When one unit of a parallel group fails the remaining units of this group will be subjected to an over-voltage due to the change in circuit impedance. A sufficient number of units must be operated in parallel to limit this voltage to 110 per cent of rated capacitor voltage until the unbalance is corrected.

Table V, which was prepared from Figures 16 and 17, shows the minimum number of units which may be operated in parallel after the operation of an individual fuse without exceeding 110 per cent of operating voltage. This is based upon normal voltage at the time of fuse operation. For this type of protection, it is assumed that not more than one unit in a series group will fail at one time and that it will be cleared by its individual fuse before the backup protective equipment operates. On the rare possibility of two simultaneous series failures, it is expected that some damage might occur before the backup protection for the circuit could clear the fault. Experience has shown this assumption to be valid. Generally, more units connected in parallel give a greater factor of safety for the operation of fuses, and minimum over-voltage after a fuse has blown. Capacitor banks for circuit voltages above 15 kv are usually built up in series parallel connected groups.

2. Fuses for banks or groups of capacitor units are subjected to the same limitations in application as are those for individual units. The fusing of banks or groups requires larger fuses. This limits their use since a fuse of high current carrying capacity allows considerable energy to be dissipated in the fault before circuit interruption. The fuse must clear the circuit before the energy dissipated in the fault can cause case rupture, see Figure 15.

3. Line current transformers used with auxiliary equipment to trip circuit breakers can be used for capacitor bank protection. The time required to clear a faulted capacitor unit by over-current tripping of a circuit breaker is approximately 1/10 of a second. This may cause case rupture if the fault current exceeds a few hundred amperes. Therefore, this arrangement is usually used as backup protection to group or individual fuses.

4. The use of unbalanced phase voltages on a capacitor bank to tip a circuit breaker as shown in Figure 11 is applicable where the possible loss of the entire bank is permissible in case of a unit failure.

5. Circuit breakers tripped by voltage or current sensitive relays connected between the ungrounded neutrals of two star banks as shown in Figure 12 is an inexpensive means of protection. Failure of a unit allows current to flow between the neutral points, operating the relay and circuit breaker. This type of protection may be used when the loss of the banks, in case of a unit failure, is not serious. The use of this arrangement is limited by the time required for circuit breaker operation.

6. Potential or current transformers may be connected between the neutral and ground of an ungrounded neutral star connected bank as shown in Figure 13. Failure of a capacitor unit causes bank unbalance and shifts the neutral. The resulting current flow to ground or voltage between neutral and ground operates the relay and circuit breaker. This type of protection has the same limitations as the arrangement described previously. Circuit disturbances, external to this capacitor bank, also may cause this protective equipment to operate. This may usually be prevented by incorporating time delay equipment. This type of protection usually can be used only on a grounded system.

7. When banks of equal parallel groups of capacitor units are used, they may be protected by circuit breakers operated by differentially connected current transformers as shown in Figure 14. Failure of a capacitor unit causes a current unbalance between the parallel group of the phase involved. This means of protection has the same limitations as described in paragraph 4 (Figure 11). It may be used when the fault current is not sufficient to rupture the capacitor case. Because the normal relay current is zero this arrangement is very sensitive to capacitor unit failures.

8. Reduced conductor sections that are melted and burned open by fault currents may be used to protect capacitors. Their use is limited to circuits of 240 volts and below where sufficient fault current is available. This means of protection may be used when capacitor units are in housings or vaults where case rupture is not objectionable. This means of protection is not considered satisfactory for circuits above 240 volts, since the melting of conductor section may be followed by continued arcing and much damage before fault is cleared.

Table V

Grounded Star Banks			Ungrounded Star Banks		
Number of Groups in Series	Minimum Units Per Group	Total Units Per Phase	Number of Goups in Series	Minimum Units Per Group	Total Units Per Phase
2	6	12	2	8	16
3	8	24	3	9	27
4	9	36	4	9	36
5	9	45	5	10	50
6	9	54	6	10	60
7	10	70	7	10	70
8	10	80	8	10	80
9	11	99	9	11	99
10	11	110	10	11	110
11	11	121	11	11	121
12	11	132	12	11	132
13	11	143	13	11	143
14	11	154	14	11	154
15	11	165	15	11	165
16	11	176	16	11	176

The foregoing discussion applies to methods of protection which operate after capacitor unit failures. It is more important to prevent failure of a unit by protecting it against abnormal operating conditions. A more complete discussion of some of the means used to help mitigate these abnormal operating conditions is given in other sections of this report.

Grounding Cases of Capacitor Units

General

Single-phase capacitor units are built with either one or two bushings. In the former, one of the terminals is a stud electrically-connected to the case.

The 2-bushing unit may be used for any type of connection. Its case may be grounded, provided it has sufficient terminal to case insulation. Two bushing units have approximately 0.002 microfarad capacitance between terminals and case. This allows a few milliamperes of charging current to flow to ground when a case to ground connection is made.

Single bushing units have the case grounded when connected to a circuit having one conductor grounded, such as a 3-phase 4-wire multigrounded neutral system. When the terminal common to the case cannot be connected to ground, the capacitor case must be insulated from ground. Under this condition the case floats at some potential above ground and may become a hazard. Therefore barriers are usually provided.

Economics determine whether the cases should be insulated from ground or whether this insulation should be built into the capacitor unit. When capacitor units are installed on ungrounded systems of 12 kv and above, and on all systems where units are connected in series, it is usually more economical to insulate the cases from ground. On lower voltage circuits, it is usually more economical to standardize on two bushing units having the insulation built into the capacitor. These have a more universal application.

Pole Mounted Capacitor Units

Two-bushing units may be mounted on poles without cases being grounded. Under this condition the wood poles or structures provide some insulation between case and ground. Two bushing units must be used on multigrounded neutral systems when necessary to operate with cases ungrounded. Pole mounted single bushing units should only be used on multigrounded neutral systems with the case connected to the neutral. Single bushing units are not usually used for installations where wood supports furnish the full line-to-ground insulation.

The advantages of grounding cases of pole mounted units are:

1. Prevents the case from becoming a hazard by providing a path to ground for terminal to case charging current thus keeping the case at ground potential.
2. Prevents the case from becoming a hazard by keeping it at ground potential in event of a terminal to case insulation failure.
3. Makes it possible to improve lightning protection by interconnecting the capacitor case and lightning arrester ground.

The disadvantages of the grounding cases of pole mounted units are:

1. The necessity of obtaining a satisfactory ground.
2. The expense of providing ground leads.
3. The capacitor unit is more vulnerable to lightning damage when the lightning arresters are not used.
4. The hazard of a large grounded surface on the pole.

This subject is closely related to the general subject of transformer and other case grounding on poles. It has been the object of considerable controversy for several years.

Ground Level Capacitor Installations

Unless cases are grounded, ground installations without housings usually will require barriers or other means of preventing accidental contact. Single bushing capacitor units should be connected to the multigrounded neutral unless they are mounted on insulators. When ground connections are used, they must be adequate to dissipate fault current with no hazardous rise of voltage.

Article 460 of the National Electrical Code [3] outlines the code requirements for indoor capacitor installations. Among these are those on grounding and isolation which state that "capacitor cases shall be grounded in accordance with Article 250" and "all live parts of capacitors which are connected to circuits of more than 600 volts between conductors and are accessible to unqualified persons, shall be enclosed or isolated."

The relative advantages and disadvantages of grounding cases and housings for ground level installations are similar to those for pole mounted installations.

Safety Precautions in Handling Capacitor Units

The same precautions must be taken with capacitor units as with other electric equipment when they are taken out of service for repair or maintenance. A capacitor is unique in that it may retain a hazardous charge for an indefinite period after being disconnected. This requires extra precaution.

Generally, discharge resistors are built into each capacitor unit. When a unit is disconnected from its power supply, these discharge devices will reduce the terminal voltage of the unit to 50 volts or less in one minute for units rated at 600 volts and lower, and in five minutes for units rated above 600 volts. It is not advisable to put complete dependence on such devices.

Before capacitor units are handled, it is good practice to short circuit and ground the terminals. As a precaution, these short circuit and grounding provisions should be left intact until all work on an installation is finished. Capacitors not equipped with discharge resistors may build up a small charge after having been discharged. Therefore it is considered good practice to connect all capacitor terminals together and to ground before making bodily contact with such parts.

Short circuiting and grounding equipment may be temporary portable conductors and clamps, or built-in short circuiting devices, depending on how often they may be used.

Capacitor cases, unless grounded, may pick up a residual charge. Although this is not dangerous, it can cause discomfort when the case is contacted. This creates a hazard and might cause an accident.

When cases are operated grounded, special attention should be given to the condition of the ground before working on the equipment. The same precaution should be followed as those for working near any other large grounded surface.

Maintenance and Periodic Inspection of Capacitor Installations

General

The maintenance requirements of a capacitor installation are relatively few and, for that reason, are frequently overlooked. However, certain phases of capacitor installations do require attention. Since all modern capacitors have sealed cases, the only parts requiring physical maintenance are the bushings, terminals, and the case.

Items to be Checked

The following items should be periodically checked:

1. Bushings for contamination and breakage.

2. Case for rust and leaks.
3. Terminals for corrosion and breakage.
4. Ambient operating temperature.
5. Operating voltage.
6. Protective and other auxiliary equipment.

Frequency of Inspections

The frequency with which inspections are made depends upon local conditions and past experience. Manufacturers recommend that the voltage on the capacitor units be checked during light system load within 24 hours after installation.

Small Installations

Small capacitor banks installed on distribution circuits can be inspected along with the routine checking of other equipment during periodic load checks. Experience has shown that line crews and servicemen can check capacitors for blown fuses and other troubles, while they are in the area. This check will usually be adequate. Periodic customer voltage checks will usually indicate the maximum voltage appearing on the capacitors.

Large Installations—Indoor and Outdoor

Large capacitor banks installed near substations or other terminal points will require more frequent inspection. In these installations, the failure of capacitor units may affect the system voltage and current flow. A sustained fault in one unit may damage adjacent units and cause additional failures later. The use of individual fuses will usually minimize this hazard.

Inspection of large installations for capacitor unit failures, blowing of fuses, and so forth, may be put on the same schedule as similar work for other substation or terminal equipment. The frequency of such inspections should be more frequent than for small installations.

Voltage and ambient temperature should be checked periodically, as well as when the installation is made. The season of the year, addition of other equipment adjacent to the capacitor installations and similar factors may affect the ambient temperature. Checks should be made at times when the maximum ambient operating temperature is expected.

Similar checks should be made on indoor installations, particularly for contamination of the insulators and ambient temperature. Industrial plant atmospheres may carry contamination which will collect on bushings and cause bushing flashover. The ambient temperature in indoor installations may change due to the addition of other equipment, building alterations, and so forth.

The frequency of inspection for indoor installations should be on a schedule similar to the maintenance of other indoor electric equipment. Initially inspections at short intervals are desirable. These can be lengthened as experience indicates.

References

1. Shunt Capacitors, AIEE Committee Report. *Electrical Engineering* (*AIEE Transactions*) volume 62, May 1943, pages 222–30. (Since the present report is a continuation of the above item, the references listed at the end of that report are not repeated below.)

2. Capacitors. AIEE Standard Number 18, June 1934.

3. National Electrical Code, *C-1-1947*. American Standards Association (New York, N. Y.) 1947.

4. Shunt Capacitors. *Publication Number 45-100*, National Electrical Manufacturers Association (New York, N. Y.). August 1945.

5. Performance of Shunt Capacitors, AIEE Committee Report. *AIEE Transactions*, volume 68, part II, 1949, pages 1200–07.

6. Power Capacitors (book), R. E. Marbury. McGraw-Hill Book Company, Inc., New York, N. Y., 1949.

7. The Why of a 25 Kvar Capacitor, M. E. Scoville. *General Electric Review* (Schenectady, N. Y.) May 1949.

8. Switched Capacitor Control Voltage and Improved System Loading, N. N. Smelloff, O. Ramsaur. *Electrical World* (New York, N. Y.) December 4, 1948, page 85.

9. Practical Aspects of Applying Static Capacitors, V. R. Parrack. *Electric Light and Power* (Chicago, Ill.) December 1948, pages 70–73.

10. Capacitors Why and Where to Use, H. Brooks. *Factory Management and Maintenance* (New York, N. Y.) September 1948, pages 100–02; October 1948, pages 122–25.

11. Pole-Mounted Automatically Switched Capacitors Prove Economical, C. F. Myers. *Electric Light and Power* (Chicago, Ill.) September 1948, pages 64–66.

12. Why, When, and Where to Install Capacitors, J. W. Butler. *Industry and Power* (St. Joseph, Mich.) March 1948, pages 77–79.

13. Underexcited Operation of Turbogenerators, C. G. Adams, J. B. McClure. *AIEE Transactions*, volume 67, part I, 1948, pages 521–28.

14. New Kraft Capacitor Paper, H. F. Miller, R. J. Hopkins. *General Electric Review* (Schenectady, N. Y.) December 1947, pages 20–24.

15. Why not Bury the Capacitor Ghosts? J. W. Butler. *Electric Light and Power* (Chicago, Ill.) November 1946, pages 40–41; December 1946, pages 72–74; January 1947, pages 66–68.

16. Selection of Control Equipment for Automatic Switching of Capacitors, W. C. Bloomquist. *Electrical World* (New York, N. Y.) November 22, 1947, pages 88–91.

17. The Use of Capacitors in Industrial Plants, W. C. Bloomquist. *Electrical Construction and Maintenance*, (New York, N. Y.) part I, October 1947, pages 149–52; part II, November 1947, pages 129–32; part III, December 1947, pages 81–86.

18. The Probable Breakdown Voltage of Paper Dielectric Capacitors, H. Brooks. *AIEE Transactions*, volume 66, 1947, pages 1137–45.

19. Automatic Switching Schemes for Capacitors, W. H. Cuttino. *AIEE Transactions*, volume 66, 1947, pages 311–14.

20. Telephone Influence Factor and What to Do About It, G. L. Oscarson. *Petroleum Engineering* (Dallas, Texas), volume 18, August 1947, page 100.

21. Telephone Interference from Power Systems, P. B. Frost, E. H. Gould. *Electrical World* (New York, N. Y.) February 1, 1947, page 82.

22. Inductive Co-ordination of Rural Power and Telephone Systems. *Engineering Report Number 40*, Edison Electric Institute (New York, N. Y.) 1938.

23. Selective Devices for Reducing Harmonics in Power Systems—Part II, A-C Systems, *Engineering Report Number 34*, Edison Electric Institute (New York, N. Y.) 1937.

24. Banks of Capacitors Reinforce 132 kv Grid, L. F. Ferri, C. S. Dayton. *Electrical World* (New York, N. Y.) March 29, 1947, pages 66–69.

25. The Economies of Using Capacitors in Amounts to Require Automatic Switching, J. W. Butler. *AIEE Transactions*, volume 66, 1947, pages 322–28.

26. Capacitors Can Cause Motors to Reverse, H. Van Horn. *Electrical World* (New York, N. Y.) March 15, 1947, pages 127–30.

27. Safe Discharge of A-C Power Capacitors, R. L. Fancher. *Electrical World* (New York, N. Y.) April 13, 1946, pages 105–07.

28. Switched Capacitors Retrieve Line Loading Capacity, R. O. Loomis, E. J. Archbold. *Electrical World* (New York, N. Y.) April 27, 1946, pages 46–48.

29. Shunt Capacitors Reduce Costs and Increase Power Capacity, W. H. Cuttino. *Buildings and Building Management* (Cedar Rapids, Iowa) April 1946.

30. Selection and Application of Automatic Control for Capacitors, T. W. Schroeder, W. C. Bloomquist. *General Electric Review* (Schenectady, N. Y.) part I, October 1945, pages 7–11; part II, December 1945, pages 22–31; part III, March 1946, pages 37–41.

31. How Kilovars Are Related to System Design and Operation, J. W. Butler. *General Electric Review* (Schenectady, N. Y.) volume 48, September 1945, pages 51–57.

32. Application of Capacitors for Power-Factor Improvement of Induction Motors, W. C. Bloomquist, W. K. Boice. *Electrical Engineering* (*AIEE Transactions*) volume 64, May 1945, pages 274–78.

33. Use the Right Capacitor with Induction Motors, J. B. Owens. *Factory Management and Maintenance* (New York, N. Y.) May 1945, pages 144–46.

34. Power Supply for A-C Arc Welding, A. U. Welch, R. F. Wyer. *Electrical Engineering* (*AIEE Transactions*), volume 64, March 1945, pages 116–20.

35. Industrial Capacitor Application Calculation, J. S. Francis, S. Freeman, Jr. *Electrical World* (New York, N. Y.) February 3, 1945, pages 85–87.

36. Characteristics of Chlorinated Impregnants in D-C Paper Capacitors, L. J. Berberich, C. V. Fields, R. E. Marbury. *AIEE Transactions*, volume 63, 1944, pages 1173–79.

37. Capacitor Installation at Newport News, V. R. Parrack, E. L. Harder. *AIEE Transactions*, volume 63, 1944, pages 1165–72.

38. Automatic Control and Switching Equipment for Capacitor Banks and Its Application, T. W. Schroeder, W. C. Bloomquist. *Electrical Engineering* (*AIEE Transactions*) volume 63, September 1944, pages 649–54.

39. Extending the Use of Shunt Capacitors by Means of Automatic Switching, W. H. Cuttino. *Electrical Engineering* (*AIEE Transactions*), volume 63, September 1944, pages 674–78.

40. The Cause and Control of Some Types of Switching Surges, T. W. Schroeder. *Electrical Engineering* (*AIEE Transactions*) volume 62, November 1943, pages 696–700.

41. Safe Capacitor Selection for Power Factor Improvement, J. E. Barkle. *Power* (New York, N. Y.) April 1943.

42. Reactive Power Metered as In-Out Magnetizing, G. S. Lunge. *Electrical World*

(New York, N. Y.) April 18, 1942, pages 58–59.

43. Harmonics and Power Factor, G. W. Stubbings. *Electrical Review* (London, England) volume 130, June 5, 1942, pages 726–27.

44. Paper Dielectric Containing Chlorinated Impregnants, D. A. McLean, L. Egerton, G. T. Kohman, M. Brotherton. *Industrial and Engineering Chemistry* (Easton, Pa.) volume 34, 1942, pages 101–09. Also articles in January 1945, May and November 1946, and November 1947.

45. Power Factor Improvement, M. J. Alimansky, Insley. *Paper Trade Journal* (New York, N. Y.), December 1941, pages 37–38.

46. Important Properties of Electrical Insulating Papers, H. H. Race, R. J. Hemphill, H. S. Endicott. *General Electric Review* (Schenectady, N. Y.), volume 43, 1940, pages 491–99.

47. Impregnated Paper Insulation (book), J. B. Whitehead. John Wiley and Sons, Inc., New York, N. Y., 1935.

48. Dielectric Properties of Chlorinated Diphenyls, A. H. White, S. O. Morgan. *Journal*, Franklin Institute (Philadelphia, Pa.), volume 216, 1933, pages 633–34.

49. Lectures on Dielectric Theory and Insulation (book), J. B. Whitehead. McGraw-Hill Book Company, Inc., New York, N. Y., 1927.

No Discussion

The Calculated Risk of Operating Capacitors Without Individual Fuses

HAMILTON BROOKS
MEMBER AIEE

Synopsis: The development of power capacitors has proceeded along the line of relatively small individual units for sound engineering reasons, and the performance of capacitor units as individuals has been very satisfactory. When capacitors are grouped in banks, this individuality or independence may be retained by fusing each unit. When group protection only is supplied, as by the use of bank fuses or circuit breakers the installation is no longer a collection of individuals but a consolidated group whose performance may be decidedly different. It is shown in this paper how the performance characteristic of the individual unit changes with the size of the group. Since the selection of protective methods and equipment may be dictated largely by economical considerations, the risk of operating groups of capacitors without individual fuses is evaluated.

THE USE of shunt capacitors for power factor correction on distribution circuits has developed in a large way only during the past 15 years. Many papers have been written concerning the advantages, economics, and satisfactory performance of shunt capacitors[1] and they are accepted by the distribution engineer as an established item of distribution apparatus of proven worth. Capacitors differ from other distribution apparatus, however, in certain unique ways that are not always properly taken into account in the application. This sometimes influences the performance of the capacitor installation. One of the unique characteristics in the use of power capacitors is the installation of large blocks of corrective capacity which are composed of a large number of relatively small units. One does not, for example, make a 2,500-kva transformer installation by using 100 small transformers in parallel. The reason that capacitors are built in this manner is, of course, because it is the practical and economical way to do it.[2] It is perfectly possible to build a large tank-type capacitor, but when the thermal, mechanical, and insulation requirements are adequately handled, the resulting capacitor has no practical usefulness because of the cost.

It is fortunate that the unit construction is the economical way to build power capacitors. The advantages in standardization, diversity of application, and ease of handling and installation are important factors in the choice of capacitors to do a particular job as compared to other alternates. In addition, the reliability of operation that may be obtained by individualization cannot be achieved in a large consolidated unit, whether it be a capacitor or something else. It is a mathematical fact which is intuitively recognized that the life, strength, and reliability of a group of independent individuals tends toward the average, whereas that of a consolidated group is represented by the most unfortunate individual in the group.

The means of maintaining unit independence in the performance of groups of capacitor units is readily provided by special-purpose individual fuses that may be mounted on or near each unit. Sometimes it may appear that such protection is inconvenient or unjustifiable and uneconomical, and such may well be the case. Under many circumstances, however, omissions of individual fuses are indefensible. Capacitors have been particularly advantageous in that they may be economically installed in small groups distributed over a system, and a majority of capacitors have been applied in this way. Here the individuality of the capacitor unit does not play an important part in the performance. The application of larger and higher voltage banks is developing, however, and in these installations the influence of individuality on performance may not be neglected.

It seems appropriate, therefore, to discuss the manner in which life and reliability may be expected to be influenced by grouping and by the size of capacitor groups. It is proposed to show in a general way how fault conditions and protective measures govern the severity of contingent damage following the failure of a capacitor unit. Finally, it is the purpose of this paper to illustrate how a numerical value may be derived to represent the risk incurred in economizing on or in providing inadequate protective measures.

The Life Expectancy of Power Capacitors

The mortality curve of capacitor units is similar in many respects to the human mortality curve, the typical shape of which is shown by Figure 1. In this curve can be seen the hazards of infancy, the culling out of weak individuals, the continuous influence of accident and disease, and the inevitable aging or wearing out of the individuals. Such a curve, however, is only representative of a population as a whole. It does not reflect the benefits of infant care or the result of gross neglect. It does not distinguish between occupational hazards, nor does it show the effects of abuse as compared to that of careful living. Clearly only a part of the life expectancy thus represented is due to unavoidable factors in the individual make-up. A great deal depends on the individual's environment or lies within his control.

Accurate performance records of apparatus are difficult to obtain. No doubt all capacitor manufacturers maintain

---

Paper 49-253, recommended by the AIEE Transmission and Distribution Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 18, 1949; made available for printing September 9, 1949.

Hamilton Brooks is with the Westinghouse Electric Corporation, East Pittsburgh, Pa.

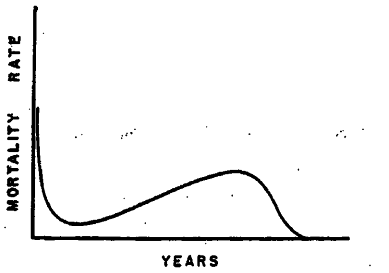


Figure 1. The shape of a human mortality curve showing the probability of death at a given age

careful service records, but these at best are incomplete and difficult of interpretation, and tend to greater inaccuracy with increasing age of the capacitor. The demise of a capacitor, unfortunately, usually takes the form of a short circuit of the insulation, regardless of whether it comes at the end of a long and useful life or at a premature age. The result of internal mechanical damage, or electrical and thermal abuse, is often the same, and the cause must be assigned from the circumstantial evidence. Unlike mechanical devices, there is no visual evidence of wearing out. Furthermore, insulation tests such as resistance and power factor measurements cannot be depended upon to indicate approaching failure, because capacitors as a class are very effectively sealed against entrance of moisture and other contaminants, and such chemical deterioration as may take place is highly localized and its effect is masked. Obviously then, the natural termination of the useful career of a capacitor unit is a short circuit unless it should be retired beforehand. Individual fuses are designed for this eventuality.

The AIEE-EEI Joint Subcommittee on Capacitors has recently made an extensive survey of the operation of shunt capacitors on electric power systems.[3] Valuable information is contained in this survey with respect to the mortality of capacitors in their early years of service. Such records, however, are representative in the same sense as the human mortality curve of Figure 1. The early years of a capacitor mortality curve are shown by Figure 2. This was derived from the AIEE-EEI data supplemented by other records. It is evident that aging or wearing out has not begun within the period covered by the curve and that the capacitors are still young. The difference in the initial shape of the curve reflects the effect of factory tests; the defective human individual is not killed at birth. As with the human mortality curve, however, the effects of care or abuse and hazards or protection are buried in the averaging. Using the terminology of the quality control engineer, one would expect rational subgroups from such a heterogeneous population as represented by all kinds of capacitors operated under all sorts of conditions to exhibit lack of control. More simply, the difference in performance between one set of capacitors and another is likely to be greater than can be accounted for by chance.

In economic studies, and in the analysis that follows, it is necessary to assign some value to the probability of failure within some given period. In any practical situation such a value must be largely arbitrary and dependent upon the amount of information and experience available. It is certainly desirable, however, to fit it as well as possible to the conditions at hand. Curve *A* of Figure 3 gives the cumulative mortality of capacitors from year to year corresponding to the yearly mortality of Figure 2. For the present purpose one may call this the probability of failure within the indicated period of usefulness. As pointed out, however, this is representative only in a broad way. Curves *B* and *C* illustrate how much a particular situation may depart from the average. There are distribution systems that use many small groups of capacitors dispersed over the system, and this application is of great benefit in relieving circuits of excess burden by applying the correction close to the load. Such capacitors are often exposed to lightning disturbance without protection and to mechanical damage in outlying districts and rural locations. Under these conditions and in view of the benefits, a performance as indicated by Curve *B* has been considered economical and satisfactory. With large installations continuity of service becomes important, and the investment warrants careful attention to operating conditions and protective devices. Equipment as received from the transportation agency is carefully inspected and tested for damage before installation. Records of these installations show a performance as typified by Curve *C*, if not better.

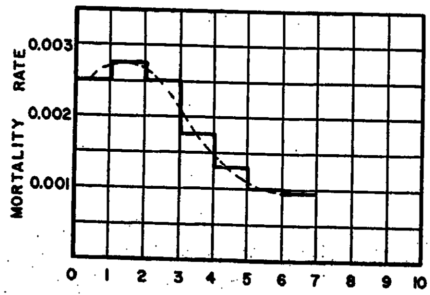


Figure 2. The shape of a capacitor mortality curve showing the probability of failure at a given age (derived from AIEE-EEI data)

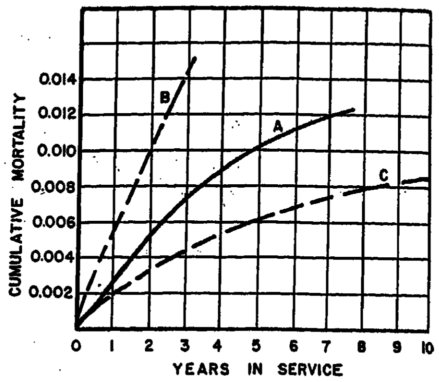


Figure 3. The cumulative mortality of capacitor units with years of service as shunt capacitors on distribution circuits

A—Consolidation for all conditions of service as derived from AIEE-EEI data

B—Severe and unprotected service. Applies to many small banks

C—Protected from severe and abnormal conditions. Applies to most large installations

The life expectancy of capacitors under practical conditions is therefore not so much a quality inherent in the capacitor unit as it is of the installation. It is accordingly determined to a considerable degree by circumstances within the control of the user. The capacitors may be deemed expendable as in the case for Curve *B*, or the importance of the installation may demand the best of care and protection. A value for the probability of failure, or the life expectancy, should then be chosen to fit the application. A reasonable way out of this difficulty is to select a convenient figure for the probability of failure and allow the age to vary to fit the circumstances. With reference to Figure 3, one may say that for the probability of unit failure within five years, $p=0.01$ is representative. For the hazardous conditions of curve *B* the loss of one out of a hundred units within two years may be expected. And finally, for the conditions applying to curve *C* it appears that 0.01 is a fair representation of the chance of an individual failure within a useful life span of 10 to 15 years. In what follows, a probability $p=0.01$, for the chance of a capacitor unit failure will be used in this sense, and the years may be selected to fit the application.

The Probability of Failure in a Group

The failure rate of capacitor units, from an economic standpoint, may be viewed as the rate of wearing out and incorporated in the cost of the installation. With industrial applications, where the monetary benefits are more sharply defined, capacitors almost always pay for themselves within $2^1/_2$ years or twice in five years. Thus, if the representative rate of one per cent failure in five years is used, the cost of wearing out over this period is only one-half of one per cent of the value received. As capacitors are grouped into banks, however, the interest shifts more or less from the probability of failure of a capacitor unit to the probability of failure in a group. The point of interest may be the cost of maintenance, the possibility of outage, or the matter of contingent damage resulting from the capacitor short circuit. It becomes of value, therefore, to investigate the probability of failure in groups.

Formally, the problem presents itself as follows: What is the probability of at least one failure in a group of n capacitors if the probability of failure of an individual is p? For small groups, say from one to ten units, the answer for all practical purposes is np, so for a group of ten units and a probability of unit failure $p=0.01$, the probability is that one group out of ten will experience a unit failure within the appropriate period. For larger groups it becomes necessary to calculate more precisely. This is conveniently done with the Poisson summation formula, and letting $P(A)$ stand for the probability of at least one failure, the answer is given by

$$P(A)=1-\epsilon^{-np}$$

extensive tables of which are available.[4] Figure 4 shows the probability of at least one failure in groups of size n when the probability of failure per unit is $p=0.01$. For a bank of 100 units, the probability turns out to be 0.63, meaning that a failure in two out of three such banks is to be expected within the arbitrary service period that is chosen. When $n=1{,}000$, a failure within the corresponding period is almost certain.

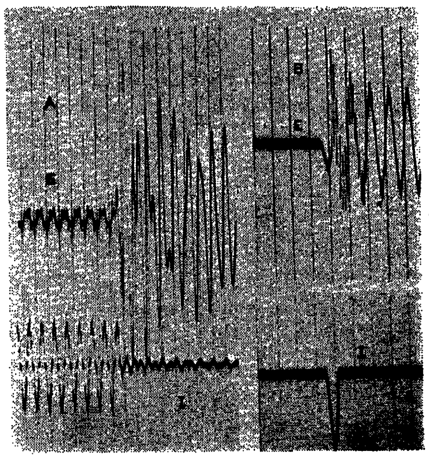


Figure 5. Capacitor unit fault currents

Oscillogram A shows a 250-ampere current that cleared after 26 cycles

Oscillogram B shows a 3,500-ampere current that produced violent rupture in 1/2-cycle

It is often desired to compare the degree of conformity of actual experience to statistical hypothesis, and a method of doing this may be mentioned. In any group of size n and probability of failure p, the expectation of failure is np, which should match with the average number obtained if an infinity of groups could be observed. Any experience with chance, however, is sufficient to demonstrate that a considerable departure from expectation is not unusual, even in matching coins. In the Poisson distribution, the standard deviation from expectation is the square root of the expectation, and it is an experimental fact that individual observations will very seldom depart more than three standard deviations from expectation through chance alone. To illustrate, if in a bank of 100 units one failure is expected, that is, $np=1$, the actual experience of no failure or four failures would fall within the bounds of chance, since $np\pm3\sqrt{np}=0$ or 4. A minus probability has no meaning. The occurrence of five failures would merit investigation or a re-evaluation of p, as would also be true if the average for a number of such installations were much different from the expected value. A considerable refinement in this process is theoretically possible but hardly justified by the necessary generality of assumptions. It should be noted that the expected number of failures, np, is not the same as the probability of at least one failure, $P(A)$.

One other observation may be made before passing on, although it should be somewhat obvious. It is not often that outage is a factor of prime importance in small- and medium-size capacitor banks but it becomes of increasing importance with larger installations. In such cases the individually-fused unit provides the means for obtaining great reliability. It is inconceivable that more than a very few units should fail within any short interval even in the event of extremely severe disturbances. It is truly surprising how much abuse is required to break a capacitor when one sets out to do it. If the loss of a small portion of the units would not adversely affect the operation of the circuit, and such would surely be the case, it requires no mathematical manipulation to show the improbability of losing the benefit of the capacitor. Therefore, individual fuses for each capacitor unit should always be used when continuity of service is important and bank outage is to be prevented.

The Consequences of a Short-Circuited Capacitor

A short-circuited capacitor unit may produce mechanical and electrical disturbances, the nature of which is somewhat peculiar to capacitors because of their characteristics. These disturbances are of concern mainly because of the possibility of damage to other units in the bank. If a faulted capacitor unit can be automatically disconnected without damage to the remaining units, then concern is limited to the loss of a unit or the outage of a bank, depending on whether individual fuses are used or bank fuses. If

Figure 4. The probability of at least one failure in a group of *n* capacitors if the probability of failure per unit is 0.01

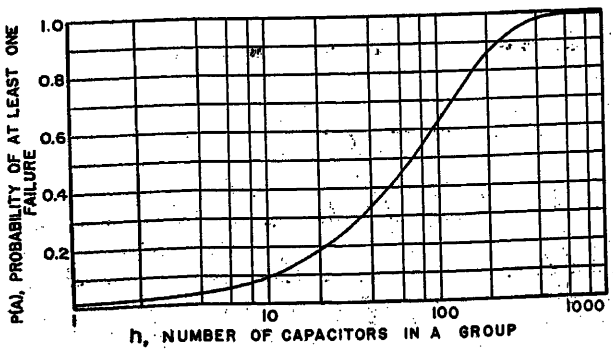
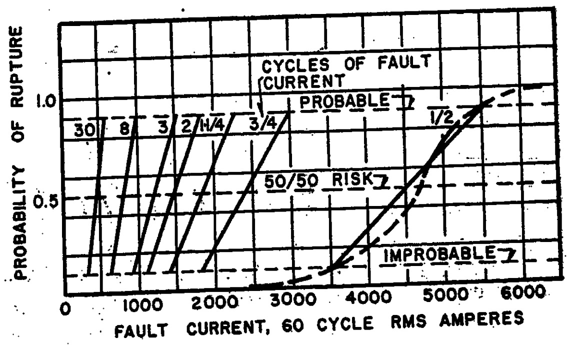


Figure 6 (right). The probability of capacitor case rupture with consequential damage for various durations of fault current

The dash curve represents a hypothetical probability distribution



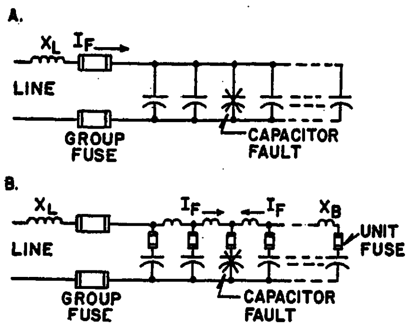


Figure 7. A simplified diagram showing the flow of fusing current to a capacitor fault

A—Capacitor group with main fuses only
B—Capacitor group with individual unit fuses

damage to other units in the group is likely to ensue, then this becomes the contingency of utmost importance.

Organic insulating materials in the presence of an arc decompose into large volumes of gases. In the case of modern capacitors, these gases are noninflammable and nonexplosive and no hazard occurs in this respect. The gases seek to escape the small confines of a compact capacitor unit, however, and if generated in sufficient quantity the pressure eventually forces a rupture of the capacitor case. In most apparatus employing fibrous and liquid dielectrics the proportion of metallic conductor is large compared to dielectric, at least in the neighborhood of the arc, and the conductors often fuse together, extinguishing the arc. The opposite is true in the capacitor, the conductor being light gauge foil finely laminated with the paper insulation in between. The arc, therefore, tends to eat away the conductor and grow larger. A greater production of gas at a faster rate is then to be expected in a capacitor fault. If the fault current is limited to a few hundred amperes, the pressure builds up slowly and many cycles of current flow may be endured before case rupture takes place. When the short circuit current exceeds 5,000 amperes, a violent rupture is generally produced within a half-cycle. Violent rupture results in mechanical damage to adjacent units and often in short-circuited bus connections.

The electrical disturbance is complicated and it is beyond the scope of this paper to treat it rigorously. From the line, the capacitor bank appears to be a lumped capacitance of large value which, with the line constants, produces a natural period of oscillation of a few hundred to a thousand cycles per second. From within, the group is a network of large capacitances and very small inductances with natural periods of many kilocycles. When the fault current is limited to small values, the arc in a short-circuited capacitor sputters along in a more or less continuous manner with small arc drop and no great electrical disturbance. With larger fault currents, which may flow from the line or come from the discharge of the other capacitors into the fault, higher arc voltages develop and the arc tends to be momentarily extinguished, resulting in a series of repetitive discharges. Oscillations at five to ten kilocycles, or even higher, then circulate currents of high magnitude between the fault and the other capacitor units, and this, if long continued, may fuse leads and spark internal connections or produce other incipient damage to the remaining capacitors which culminate in additional failures at some later time. When very large short-circuit currents are available from the line, the development of gas pressure takes place with great rapidity and in proportion to some power of the rise of current. Complete rupture of the capacitor case may then be expected at an instant of high current value, and the reaction of the sudden cessation of current flow may result in voltages severe enough to puncture insulation and flashover insulators and busses. Figure 5 serves to illustrate two of these conditions. Oscillogram *A* shows the moment of interruption of a 250-ampere current which has lasted for over 20 cycles and which ended in temporary clearing without the faulty unit being disconnected. Oscillogram *B* shows a 3,500-ampere current which caused violent rupture within the first half-cycle.

It can be seen from this discussion that for a certain range of fault currents and durations there is a probability or a risk of contingent damage which may be either mechanical or electrical. For conditions on either side of this region, there is either almost no risk in the case of small values or there is almost certain damage in the case of large values. It is of help in the selection of protective means to measure this region of probability in a numerical way insofar as practical. Such is the purpose of Figure 6, which shows the probability of case rupture against fault current for various durations of current flow. It is not presumed that this representation is exact, the difficulties of collecting sufficient data being too great, but it serves for the present analysis. The dash curve at the extreme right of Figure 6 represents a hypothetical probability distribution such as might be obtained by exhaustive testing. The other curves are approximations which seem justified by the available data.

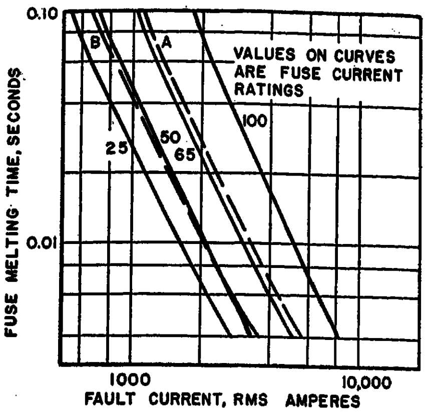


Figure 8. Average melting time—current characteristic curves for a representative type of fuse

Capacitor case rupture characteristics are superimposed, (A) showing probable case rupture and (B) improbable case rupture

The words probable and improbable are indefinite and unsatisfactory in an analysis unless some numerical value is attached. Accordingly, probable is defined as a probability of the order of 0.9 and improbable is a probability of the order of 0.1. These two boundaries are indicated by dash lines on the figure. Several things of importance may be perceived at once from this set of curves. For one, the inadequacy of circuit breaker protection to prevent case rupture is obvious from the low current values which fall within the limits of the 8-cycle distribution. More striking perhaps is the evident impossibility of preventing rupture at moderate short-circuit currents by means of conventional fuse operation. A dependence on 1/2-cycle fuse interruption would be most optimistic.

Figure 6 is deficient in that no graduation in the violence of case rupture is shown. A capacitor case is not a pressure vessel and a slowly accumulating gas pressure forces the seams to give way in a far less violent manner than when the pressure is built up quickly. Also, no measure of electrical damage is indicated by the curves. Actually, neither of these deficiencies imposes much of a handicap in using the curves for estimating the consequences of a short-circuited capacitor because one is generally interested either in the effects of high fault current or else the interrupting time of ordinary fuse protection is fast enough to clear the fault before damage. It is well to assume that arcing to the point of case rupture in the range above 1,500 amperes is productive of both mechanical and electrical damage to an undesirable degree.

The Effectiveness of Fuse Protection

Fuse protection is applied to capacitor installations for the purpose of clearing a fault. The possibility of some harmonic current and the low impedance of the capacitor to transient currents necessitate a liberal margin between the fuse rating and the normal capacitor current, so that close overload protection could not be achieved even though it were necessary. Faults between busses and over insulators are of much the same nature as in any other piece of equipment and may be cleared by circuit breaker or fuses in the conventional manner. The result of a short circuit within a capacitor unit, however, may be additionally guarded against by an individual fuse connected to the unit. The effectiveness of group fuses and of individual fuses for this contingency is now to be examined.

Figure 7 illustrates the flow of fault current to a short-circuited capacitor unit in an elementary manner. Circuit A is a capacitor group protected by group fuses. A fault developing in a capacitor unit is followed by the flow of an abnormal current from the line through the group fuse. With reference to the curves of Figure 6, a race takes place between the fuse and the capacitor unit to see if the one melts and interrupts before the other ruptures and causes damage. The odds on this race are portrayed by Figure 8, which shows a set of melting time-current curves for a representative type of power fuse. The capacitor rupture time-current characteristic is superimposed, the dash line A being the boundary of probable rupture, that is, the 0.9 probability level, and line B the improbable boundary or 0.1 level. The parallelism between fuse melting time and capacitor rupture time is remarkable and it is at once evident that no ordinary fuse of a higher current rating than 50 amperes may be depended upon to clear the fault before case rupture. Moreover, as higher fault currents are considered, a point is reached where the fuse must melt and interrupt within one-half cycle, a dubious situation, and then even the 50-ampere fuse ceases to protect. The conclusion is evident: with fuse ratings exceeding 50 amperes and for fault currents exceeding 3,500 amperes the successful interruption of the fault by group fuses is entirely a matter of chance, with the odds rapidly mounting against success.

Circuit B of Figure 7 illustrates the current flow through an individual unit fuse in the event of a unit short circuit. The line current is reinforced by the discharge of the other capacitor units into the fault. In fact, the line fault current is generally of little consequence and the individual fuse performs far beyond its apparent capability. Several things contribute to this effectiveness. For one, the current rating of the individual fuse is small, usually 15 to 25 amperes, the importance of which has been demonstrated by Figure 8. For another, the individual fuse is often blown entirely by the discharge of the other capacitors if the group is large and if the instant of short circuit occurs near the crest of the voltage wave when the capacitors are fully charged, and this is usually the case. With capacitors charged to 2,400 volts root mean square, the discharge current may be expected to attain a value of 10,000 amperes within 1/2,000th of a normal cycle. The greatest contribution to success, however, seems to be the effect of the natural period of oscillation within the group of capacitors which may range from five to 50-kc depending on the size of the group and the unit rating. This, along with the high discharge current, melts the fuse and forces a current zero sufficient to complete interruption before the line current has time to build up to a dangerous value. Thus, the individual fuse performs the apparent impossibility of clearing a fault in times far less than one-half cycle.

The Risk of Operating Capacitors Without Individual Fuses

Individual capacitor unit fuses are undesirable to the extent by which their use may increase installation cost, maintenance or space requirements, and there are many occasions where their need may be legitimately questioned. The curves of Figures 4 and 6 are useful in deciding this point. First, there are several situations that may be disposed of quickly. It should be clear that the magnitude of the available fault current has a large bearing on the probability of case rupture and contingent damage. The fault current may be satisfactorily limited by the circuit, as is sometimes the case with small capacitor installations. The fault current may be limited by the manner of connection, as with ungrounded star connection or where there is resistance in the neutral. Then the fault current through a short-circuited capacitor unit in one phase is limited to the current that may flow through the capacitor units in the other two phases. Large high-voltage installations are conveniently made by using a series of parallel groups of lower voltage rating units, and the fault current through a short-circuited capacitor unit is limited by the other series groups. Here much care must be used in proportioning the series and parallel groups to insure sufficient fault current to give reliable fuse operation.[5] In this case the choice of individual fuses is dictated by other considerations than the possibility of contingent damage.

Many capacitor installations are connected in delta or in star with grounded neutral at points where the fault current is large enough to cause damage. These installations may be large or small, and it is on this basis that a risk may be profitable or unprofitable. It will be presumed that individual unit fuses prac-

Table I. The Probability of Failure Accompanied by Contingent Damage and Group Outage for Capacitors Protected Only by Group Fuses

Based on 25-Kvar Units and a Unit Failure Probability $p=0.01$ Within an Arbitrary Period of Usefulness

Circuit Voltage	Number of Units in Group	3-Phase Kvar	Fuse Rating, Amperes	P(A)	Capacitor Fault Current, Rms Amperes <3500 P(B)	<3500 P_A(B)	≈4500 P(B)	≈4500 P_A(B)	>5500 P(B)	>5500 P_A(B)
4,160	3	75	30	0.03	0.1	0.003	0.5	0.015	0.9	0.027
	6	150	50	0.06	0.1	0.006	0.5	0.030	0.9	0.054
	9	225	65	0.09	0.9	0.08	0.9	0.08	0.9	0.08
	12	300	85	0.11	1.0	0.11	1.0	0.11	1.0	0.11
	18	450	125	0.16	1.0	0.16	1.0	0.16	1.0	0.16
	24	600	175	0.21	1.0	0.21	1.0	0.21	1.0	0.21
	48	1,200	300	0.38	1.0	0.38	1.0	0.38	1.0	0.38
13,800	3	75	10	0.03	0.1	0.003	0.5	0.015	0.9	0.027
	6	150	20	0.06	0.1	0.006	0.5	0.030	0.9	0.054
	9	225	25	0.09	0.1	0.009	0.5	0.045	0.9	0.08
	12	300	40	0.11	0.1	0.011	0.5	0.055	0.9	0.10
	18	450	50	0.16	0.1	0.016	0.5	0.08	0.9	0.14
	24	600	50	0.21	0.1	0.021	0.5	0.11	0.9	0.19
	48	1,200	100	0.38	1.0	0.38	1.0	0.38	1.0	0.38

$P(A)$ is the probability of one or more unit failures per group.
$P(B)$ is the probability of contingent damage *if* a unit fails.
$P_A(B)$ is the total probability of damage $= P(A)\cdot P(B)$.

tically always clear a short-circuited capacitor before other damage occurs and this is well justified by experience. Group fuses may or may not be successful and this constitutes part of the gamble. The other element of chance lies, of course, in the probability that a short circuit will take place. Let the event of at least one unit failure in a group of n capacitor units be denoted by A. Also, let the probability of an individual unit failure within X years of service be set at $p = 0.01$, the value of X being appropriate to the service conditions or experience as has been previously outlined. Then the probability $P(A)$ of at least one failure in a group of n units within the X year period is given by Figure 4. Now, if a short circuit does take place, material damage may occur and this is denoted by event B.

The probability $P(B)$, once a short circuit exists, may be estimated from Figure 6, taking into account the fuse characteristics. For practical purposes it seems well to say that damage is either probable, $P(B) = 0.9$, or improbable for which $P(B) = 0.1$. The total probability of damage is then conditional upon a short circuit happening in the first place, or

$$P_A(B) = P(A) \cdot P(B)$$

The total probability of damage has been calculated for some examples where the capacitors are protected only by group fuses. These examples are listed in Table I. With groups of capacitors, such as three or six units, the probability $P(A)$ of failure of at least one of the units in the group is small. If the available fault current is less than 3,500 amperes, the probability $P(B)$ of damage before fault interruption is estimated from Figures 6 and 8 to be 0.1, since the fuse rating can be 50 amperes or less. The total probability $P_A(B)$ being the product of $P(A)$ and $P(B)$, the risk of damage is very slight. If the fault current is approximately 4,500 amperes, Figure 6 shows the chance of damage to be about 50/50 if a short circuit occurs, because one must count on very quick interruption to avoid damage. Here again the total probability is small. For fault currents exceeding 5,500 amperes, the 50-ampere fuse may occasionally interrupt successfully if it melts fast enough, and a value of 0.9 is indicated for $P(B)$. Even in this case, however, the risk of damage may be reasonably incurred because the probability of a faulty capacitor is small and the corresponding product $P(A) \cdot P(B)$ does not have a very great value.

For groups of capacitors of intermediate size, such as for example 12 or 18 25-kilovar units, a fuse of the necessary current rating may not be capable of melting fast enough and so there is little chance of interruption before case rupture and damage occurs. Such is the case for the 4,160-volt groups and thus a probability of 1.0 is assigned to $P(B)$ even for fault currents less than 3,500 amperes. Now the total probability is identical with the probability of a short circuit in the group of units, and this may or may not be a reasonable risk depending on the economic factors affecting the particular instance. In the case of still larger installations, illustrated by the 48-unit group, $P(A)$ takes on such values as have been shown by Figure 4, and with $P(B)$ fixed at 1.0 because damage is almost certain if a short circuit occurs, the risk becomes a matter no longer to be decided by economic considerations. Large groups require individual fuse protection for satisfactory performance.

The absolute values of probabilities arrived at in this manner naturally should not be taken too seriously, although they can be as good as the data on which they are based. The real advantage is that the numerical values carry a significance that cannot be attached to a verbal description or arrived at by a mental weighing. In addition, the important factors, and the trend of their influence, can be clearly demonstrated.

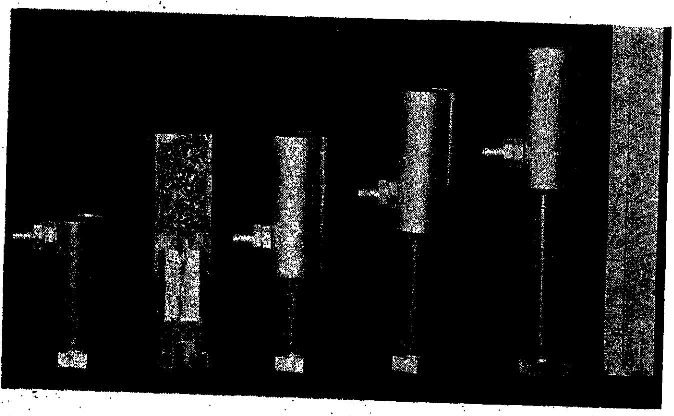


Figure 9. Some individual fuses for indoor use

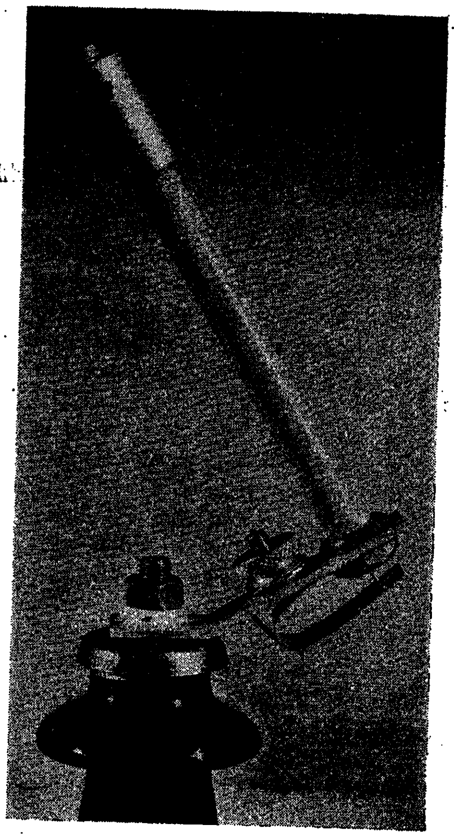


Figure 10. An expulsion-type outdoor individual fuse

Conclusions

The important place that power capacitors have attained in the economics of power distribution demands a better understanding of the problem of fault protection, and this is of increasing importance as the trend grows toward larger installations of capacitors. Capacitors are a reliable component and the fact that they are inexpensive enough to be expendable in many instances does not alter the inherent reliability that is possible when the individuality is properly maintained in large groups by the means of individual fuses.

The economic evaluation of different methods of protection requires a statistical treatment as well as a consideration of what is to be accomplished by the protection. The requirements of fuse protection have been outlined only briefly, but it should be evident that success hinges on the ability of the fuse to disconnect a faulty capacitor unit before rupture or electrical damage can take place. This is practically assured with individual fuses but is dictated by chance when group fuses alone are used if the fuse rating exceeds 50 amperes or if the fault current exceeds some 3,000 amperes. It has been shown that with small banks of 3 to 12 units this risk may be rofitable depending on particular con- iderations, but that with large groups of apacitors the omission of individual ises can be none other than costly if irge fault currents can flow into the apacitor unit. A method has been out- ned by which such risks may be evalu- ted.

The development of special purpose apacitor fuses has progressed along with apacitor unit development, and proven esigns are available which meet the re- uirements of inexpensiveness and convenience. Figure 9 shows one such fuse intended for indoor use, the fuse being screwed directly on a terminal stud of the capacitor unit. A fuse suitable for outdoor use is shown in Figure 10 and also this may be mounted directly on the capacitor terminal or on the bus. The means of successfully disconnecting a short-circuited capacitor unit is therefore available and, by methods demonstrated in this paper, the conditions demanding individual fuses can be determined in a logical manner.

References

1. SHUNT CAPACITORS, AIEE Committee Report. *Electrical Engineering (AIEE Transactions)*, volume 62, May 1943, pages 222–30.
2. THE WHY OF A 25-KVAR CAPACITOR, M. E. Scoville. *General Electric Review* (Schenectady, N. Y.) volume 52, May 1949, page 19.
3. REPORT OF A SURVEY ON THE PERFORMANCE OF SHUNT CAPACITORS, AIEE Committee Report. *AIEE Transactions*, volume 68, part II, 1949, pages 1200–07.
4. POISSON'S EXPONENTIAL BINOMIAL LIMIT (book), E. C. Molina. D. Van Nostrand Company, Inc., New York, N. Y., 1947.
5. POWER CAPACITORS (book), R. E. Marbury. McGraw-Hill Book Company, Inc., New York, N. Y., 1949, page 113.

Discussion

lenn W. Bills (Bonneville Power Adminis- ation, Portland, Oreg.): The author has early presented the protection problem cing power system engineers who are sponsible for the installation of capaci- rs. Since power-factor correction by use shunt capacitors is now commonplace be- use of overloaded system conditions, a lution such as outlined is particularly valuable at this time.

In the Bonneville Power Administration stem, over 300,000 reactive kilovolt nperes of shunt capacitors are installed, d many more are scheduled for installa-)n, including 115-kv groups as well as 230-kv series capacitors. Initial experience over a period of seven years indicated that fuse failures occurred, although very few capacitors failed and then mostly due to the failure of fuses to open. It was felt that the individual fuses were more of a hazard to normal system operation than were capacitor failures. However, our capacitors are installed in banks as large as 60,000 reactive kilovolt amperes, and the large short circuits which could occur required individual fuses for capacitors. Since newer, more appropriate fuses have been developed, our natural desire to avoid or to minimize their use is less pronounced.

Not only has the author given a lucid physical picture of the action that occurs when a capacitor unit fails, but he has provided a method of determining when to use individual capacitor fuses. It is pleasing to note that probability theory has been used to achieve numerical results rather than use of some "rule of thumb" method which may not produce the most economic solution. The majority of engineers shy from the use of probability mathematics, and papers such as this have the merit of bringing to their attention the ease with which fairly complex problems can be solved with the aid of simple probability methods.

Connection Arrangements and Protective Practices for Shunt Capacitor Banks

N. R. CLARK
ASSOCIATE AIEE

S. B. FARNHAM
MEMBER AIEE

THE FACT that capacitors are commonly built in units of 25 or 15 kilovars for power circuits makes possible a wide variety of groupings of these convenient "building blocks" to form large banks. The particular arrangement in which the individual capacitor units are connected in a 3-phase bank, and the type of protective arrangement used, may influence the cost of the installation and these factors certainly will have an important bearing on its operation and on the operation of its associated protective devices under fault conditions. Since a number of different connection and protective arrangements are possible, all resulting in banks having substantially the same kilovar and voltage ratings, it is in order to review briefly the commonly used arrangements and to outline their salient characteristics.

This paper is intended as an indorsement of, and a supplement to, certain sections of the AIEE Committee Report on the Operation and Maintenance of Shunt Capacitors,[1] herein referred to as the AIEE report.

Connection Arrangements

Consider first the simple delta and Y connections with all units in each phase connected in parallel. Here the voltage rating of each individual capacitor unit is equal to the system line-to-line voltage for the delta connection, or to the system line-to-neutral voltage for the Y connection. For systems in the 11—13.8-kv class the Y connection is generally used, since it takes advantage of the economical capacitor designs up to approximately 8 kv. For systems in the 2.4—7.2-kv group, however, either the delta or the Y connection may be employed at substantially equal cost. Here the gradual changeover of many 2.4-kv systems to 4.16-kv operation has resulted in the general acceptance of 2.4-kv capacitor units for both systems. Similarly, 7.2-kv capacitor units are often used on both the 7.2 and 12.5-kv systems. This practice offers obvious advantages with respect to standardization and stocking.

Capacitor banks for use on systems 23 kv and higher are almost invariably composed of two or more groups of parallel-connected capacitor units in series per phase.

The choice between delta and Y connection is generally determined by whichever arrangement will permit the fuller utilization of standard voltage rated capacitors. It frequently happens that the user has a preference for capacitor units of some particular voltage rating, especially if units of that same rating are already being used on his lower voltage distribution circuits. On the other hand, since the kilovars delivered by capacitors are proportional to the square of the applied voltage, there is a disproportionate reduction in the realizable kilovar capacity if capacitors are not operated at their full voltage rating. Hence, it may be advantageous in some cases to resort to special voltage rated capacitors, which will be more expensive per kilovar of rating, but which can be fully utilized. A practical expedient may be to use capacitor units of two different voltage ratings in different series groups of the same phase. Here the majority of the units can be of the desired standard rating, while the remainder may be either of a different standard rating or special. This is an entirely acceptable procedure so long as the ratios of the rated kilovars of the series groups are the same as the ratios of the rated voltages of the parallel-connected capacitor units in the series groups. For example, if a phase includes a series group of 2,400-volt units and a series group of 4,800-volt units, the 4,800-volt group should consist of twice the kilovars of the 2,400-volt group.

When the Y connection is selected, the question often arises as to whether the neutral should be grounded, or insulated and allowed to "float". Normally, only the floating neutral arrangement is employed on ungrounded neutral systems, or those protected by ground fault neutralizers. On grounded systems, either arrangement may be used. The floating neutral, however, does afford certain advantages to the capacitor equipment, including its protective devices, and generally is preferred as the standard arrangement. The advantages of the floating neutral are:

1. The capacitors are not subjected to the abnormal line-to-ground voltage stresses which may appear on the system as a result of lightning, switching transients, ground faults, or phase voltage unbalance.
2. For banks installed on ungrounded systems, the rack upon which the capacitors are mounted may conveniently and economically be insulated from ground, and allowed to float at a potential above ground. With such an arrangement, the terminal-to-case insulation provided in the individual units need correspond only to the voltage across the unit rather than the line-to-line voltage, and usually results in a saving in cost of the banks, particularly if the circuit voltage is 12 kv or higher.
3. When fuses are used for protection, the floating neutral arrangement limits the interrupting duty and makes it independent of the system short-circuit capacity, as will be explained later. This allows the use of economical fuse rating.
4. The floating neutral arrangement permits the use of "balanced" relay protection. This is covered more fully in a later section.

Paper 49-254, recommended by the AIEE Transmission and Distribution Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 21, 1949; made available for printing September 6, 1949.

N. R. CLARK is with the General Electric Company, Pittsfield, Mass., and S. B. FARNHAM is with the General Electric Company, Schenectady, N. Y.

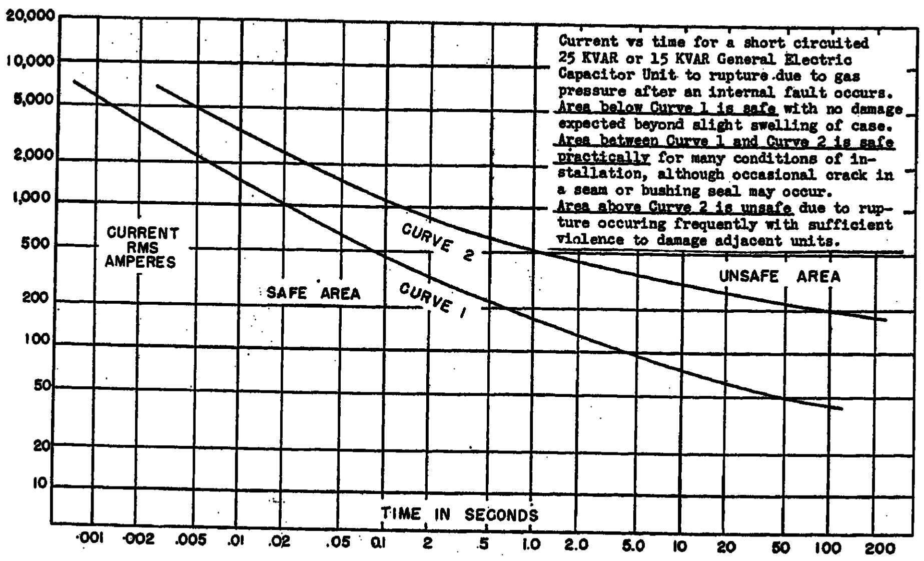


Figure 1. Current versus time for short-circuited capacitor units

Functions Desired of the Protective System

In order to secure maximum benefits, shunt capacitor banks used for kilovar supply are usually located on the distribution circuits, as near the load as practicable. As indicated previously, they may also be installed on subtransmission circuits above 15 kv if the particular case so dictates. Wherever located, they require adequate protection, in common with other system equipment, to provide the highest practicable degree of continuity of service to the users of electric power. Protective requirements for shunt capacitor banks differ from those for series-connected equipment such as transformers, in that for most installations it is possible for the power circuit to remain in operation with part or all of a capacitor bank out of service. Hence, in the event of capacitor failure, it is desirable to isolate the failure from the power system and minimize damage, without interruption in service.

Basically, protection is provided for the circuit, and not the capacitor. When an internal short circuit occurs in a capacitor unit, the arcing generates gases which can ultimately rupture the case, and danger to life and equipment is possible. A capacitor unit has a current-time characteristic similar to that of a fuse link; it should be disconnected from the circuit before sufficient energy has been delivered to the capacitor under short-circuit conditions to cause its case to rupture.

More than 100 tests have been made to establish the time-current characteristics of the modern capacitor manufactured by the company with which the authors are associated. Empirical curves plotted from the test data to show practical limits are shown in Figure 1. The test units were intentionally short circuited in all internal series sections by driving nails through the case and into the rolls, removing the nails, and soldering the holes in the case. The test capacitors were connected in series with suitable fuses, and short-circuit currents of various magnitudes were applied for various times under controlled conditions. Oscillograms were taken to record the voltage, current, and time. Figures 2 and 3 are a few of the capacitors used in these tests; they are typical of the test results from which the curve in Figure 1 was prepared.

For example, the first unit on the left, see Figure 2, received 400 amperes rms for 12 seconds, which is in the unsafe area above the upper curve, Figure 1.

The fifth unit from the left, Figure 2, received 3,700 amperes peak for 0.0083 second. This corresponds to approximately 2,600 amperes rms, and falls in the area between the two curves in Figure 1. The first unit on the left, Figure 3, received 600 amperes rms for 1/2 second, which is approximately on the upper curve, Figure 1. Figure 4 shows the damage which occurred when a capacitor unit failed in a 4,275-kilovar 2,400-volt 3-phase outdoor installation for which proper protection had not been provided. This represents a point in the high current area above the upper curve, Figure 1.

It is not intended to present the curves in Figure 1 as absolutely accurate for all types of installations for all fault conditions. They do, however, represent the pattern which resulted from the large number of tests made, and may be used as a guide in the selection of protective arrangements.

Listed here are a number of functions a capacitor protective arrangement may perform, all of which are generally desirable, and any of which may be considered mandatory by the particular system operator.

1. Protection against rupture of container.
2. Avoidance of system outage.
3. Indication of failure location.
4. Prevention of overvoltage.
5. Immunity to false operation.
6. Avoidance of overtemperature operation.
7. Limitation of kilovar loss to minimum.

How the Desired Functions Are Provided

The protective functions required for an individual capacitor bank are dependent upon operating conditions; and the selection of means to perform these functions is determined by economics and the policy of the operating company. No single protective device will perform all the functions listed, and ordinarily two or more devices are combined to attain the desired degree of protection. On the other hand, it generally is not necessary to provide an arrangement for any particular bank which will perform all the possible protective functions. The importance of a particular function will vary with the conditions of capacitor operation and the purpose the installation is intended to serve in the operation of the system.

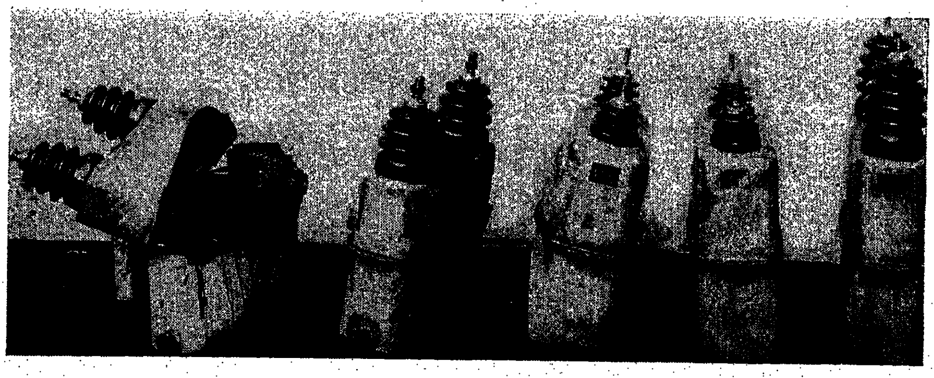


Figure 2. General Electric Pyranol capacitors shown after short-circuit tests

Fuses

In the early days of capacitor application as far back as 1910, capacitor fusing was recommended. Experience today with greatly improved fuses substantiates this early recommendation. Fusing provides the maximum number of desirable features and has been used most extensively for capacitor application. It is generally recommended as the best and most economical means of accomplishing the desired results for most installations.

INDIVIDUAL FUSES

For indoor installations and for housed outdoor installations, indoor-type current-limiting fuses of the indicating type are ordinarily used. Figure 5 shows a typical arrangement of this type. For outdoor installations without protection from the weather, expulsion-type outdoor fuses, as shown in Figure 6, are available.

The voltage rating of an individual fuse should be not less than the rated voltage of the capacitor unit with which it is used, and its maximum current-interrupting capacity should be not less than the current which will flow in the event the capacitor unit is short circuited.

Standard capacitors are designed with a zero to +15 per cent tolerance in microfarads, and must be capable of carrying 135 per cent of rated reactive kilovolt-amperes continuously. They are further required to withstand a maximum of 110 per cent of rated voltage, and 105 per cent of rated voltage average over a 24-hour period. Combining all these factors, it will be seen that a rms current of some value above 110 per cent of nominal can flow continuously in the capacitor. Therefore, as a general practice, fuses which have a 300-second melting time of the order of 5 to 10 times the rated capacitor current should be used. The purpose is to avoid unnecessary fuse blowing caused by the previously mentioned factors, by short time disturbances which do not adversely affect the operation of the capacitor bank, or by harmonic currents which may be safely permitted in accordance with industry design standards. The maximum fuse current rating is limited technically only by the bursting characteristics of the capacitor itself, although for economic reasons the upper limit is usually selected in the neighborhood of 250 per cent of nominal capacitor current.

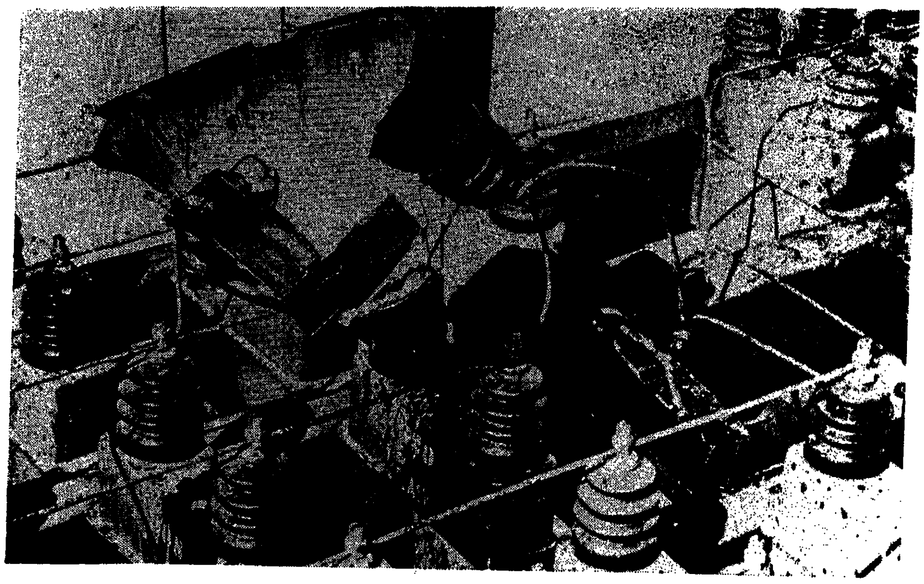


Figure 4. Damage resulting from improper protection

Since a dielectric failure in a capacitor unit does not always result in a low impedance fault, the current which will flow may vary from the full short-circuit current available down to a value which may approximate the 5-minute melting current of the fuse. Thus, individual fuses which will operate satisfactorily at the minimum as well as the maximum current should be selected.

Fuses of appropriate voltage ratings and time-current characteristics are available to meet the requirements of most circuits for delta-connected capacitor banks as shown in Figure 5 of the AIEE report, or similar banks Y-connected with grounded neutral.

Fuses so applied to delta-connected banks must be capable of interrupting system line-to-line fault currents; fuses for grounded Y-connected capacitors must be suitable for handling the system line-to-ground short-circuit currents. Where exceptionally high interrupting capacity requirements are encountered, or where line-to-line voltage of the circuits is above the most economical rating of individual capacitor units, it is usually desirable to use Y-connected banks with ungrounded neutral (Figure 6 of the AIEE report), thus limiting the fault current in case of a unit failure, and consequently duty on the fuse, by virtue of the impedance of the capacitors in the two unfaulted phases. It can be shown readily that the fault current is thus limited to a maximum of three times the continuous current which flowed in the faulted phase before the fault occurred. For line-to-line voltages above 13,800 volts, a delta-connected bank consisting of two or more series groups per phase (see Figure 7 of the AIEE report), or similar Y-connected bank, is appropriate. In either case, the fault current is limited by the impedance of the capacitors in series, which will withstand the momentary overvoltage resulting from a unit failure until the fault is cleared.

GROUP FUSES

Group fusing of capacitor banks may be considered as a special case of individual fusing, and in general the same advantages and limitations apply. Connections for group fusing of a complete bank are shown in Figures 9 and 10 of the AIEE report. These are the arrangements used extensively in small banks on distribution feeders, where fuse cutouts may be used both as a means of protection and for switching. For larger banks, groups of two or more capacitor units with a single fuse may be connected in parallel or in series-parallel combinations.

Fusing of groups of units involves the use of fuses with relatively high continuous current ratings, which necessarily allow more energy to flow into a fault before interruption occurs than would be the case with individual fuses. Thus, in order to prevent rupture of the container of a

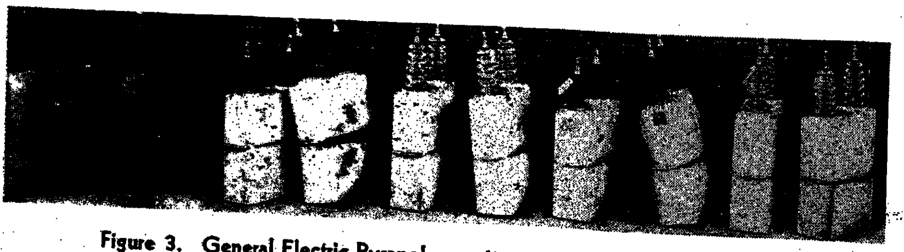


Figure 3. General Electric Pyranol capacitor units after short-circuit tests

Table I

Number of Parallel Groups in Series	Minimum Number of Units Per Group for Capacitor Bank-connected: Ungrounded Y	Delta or Grounded Y
1	4	1
2	8	6
3	9	8
4	9	9
5, 6	10	9
7, 8	10	10
9 through 16	11	11

failed capacitor, group fuses should have a characteristic which falls below the time-current characteristics indicated in Figure 1.

LIMITATIONS

Although individual or group fuses perform the majority of the functions desired of a protective system in the usual case, they have limitations of which the operator should be aware, and which may be important in specific installations.

Individual unit fuses will not, of course, give protection in case of a line-to-ground or a line-to-line fault in the bus or connections external to the capacitor units, and current transformers arranged to trip a circuit breaker and disconnect the bank are desirable to provide protection for this condition. Group fuses provide protection against such faults only when one fuse is used in each line for the entire bank.

For the Y-ungrounded-neutral connection, and for either delta or Y connection when two or more series groups are used in each phase, the following considerations apply:

1. The number of capacitor units, or groups of units connected to a single fuse, in parallel contribute to the fault current which will flow through a fuse connected to a short-circuited unit, and this number should be sufficient to provide enough fault current to cause the fuse to clear in time to prevent rupture of the capacitor container.
2. The number of capacitor units, or groups of units connected to a single fuse, in parallel affects the distribution of voltage among the remaining units after one or more fuses have blown. The minimum number of units or groups which should be connected in parallel is determined by the permissible overvoltage operating limits of capacitors.

Table I indicates the minimum number of units which should be connected in parallel to insure sufficient current for proper fuse blowing in the event of a fault, and to maintain overvoltage on the remaining units within 110 per cent of the voltage prior to the fault. Where group fusing is used, a group with a single fuse should be considered as a single unit for purposes of applying the table.

Although capacitors are guaranteed for a maximum average of 105 per cent of rated voltage over a 24-hour period, the short time maximum allowable voltage of 110 per cent of rating was selected in making up the table, since it was assumed that the blown fuse would be located, its cause corrected, and the fuse replaced promptly.

Relays

Overcurrent or overvoltage relays used in conjunction with suitable instrument transformers to detect a fault and trip a circuit breaker may be used either alone or as a supplement to fuses. This means of protection cannot, as a general rule, be arranged to indicate the defective unit when failure occurs, and therefore usually is employed as a supplementary measure.

Overcurrent relays connected in the secondary circuit of current transformers in the capacitor line connections may be used to provide minimum protection, but are generally used to supplement fuses. Since the minimum time required for disconnecting a capacitor bank from the line with a circuit breaker and relays is of the order of 1/10 second or more, considerable damage may occur if fault currents exceed a few hundred amperes.

Except for limited bank sizes connected Y-ungrounded-neutral, or banks connected either Y or delta with two or more series groups per phase, wherein the current is limited, this method does not prevent rupture of a container, because of the inherent time required for circuit breaker operation.

Because of the considerable magnitude of the transient charging current which flows when a capacitor bank is energized, the use of instantaneous relays connected to line current transformers may under certain conditions result in immediate tripping of the circuit breaker when it is closed to energize the bank. The magnitude and frequency of the transient charging current is determined by the circuit voltage, the size of the bank, and the inductance of the circuit between the capacitor bank and the source. When a bank is energized adjacent to a second bank which is already energized, the second bank will discharge into the first, increasing the possibility of operating instantaneous relays. Time delay current relays which will "ride-out" the transient inrush, used in conjunction with fuses, provide a satisfactory arrangement for such a situation.

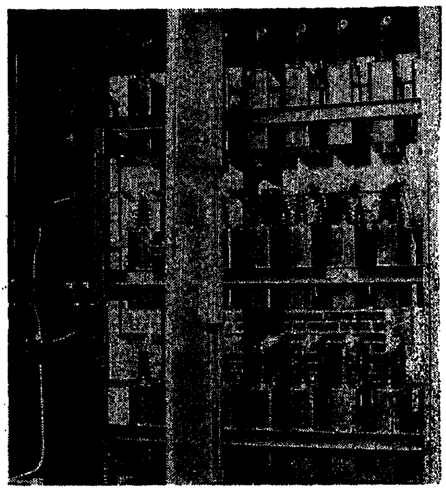


Figure 5. Housed outdoor capacitor bank with individual current-limiting fuses

A relay connected in the secondary of a transformer between capacitor bank neutral and ground is sometimes used as shown in Figure 13 of the AIEE report. Failure of a capacitor unit destroys the symmetry of the arrangement, and results in a neutral-to-ground voltage if a potential transformer is used, or the flow of current from neutral to ground if a current transformer is used. The relay is of the voltage type or current type, as appropriate to the transformer employed. Ordinarily the relay is of the time delay type, since an instantaneous relay may be susceptible to false operation on system line-to-ground faults external to the capacitor bank. Because of its relatively slow speed of operation, due to the necessity of using time delay relays co-ordinated with other system relays, and to the inherent time required for circuit breaker operation, this arrangement does not usually afford protection against capacitor case rupture. It is ordinarily used only as a supplement to fuses, and is normally employed only on grounded systems.

It is sometimes desirable to connect a capacitor bank in double-Y with neutrals of the two parts of the bank ungrounded but connected together through a transformer, with a relay in the transformer secondary to trip a circuit breaker and de-energize the bank in the event of failure of a capacitor unit (Figure 12 of the AIEE report). This arrangement operates on the unbalance between the neutral points of the two Y sections which results in a flow of current if a current transformer is used, and a voltage differential if a potential transformer is used. For large banks which have all capacitor units in each leg connected in parallel, it is not possible to obtain breaker operation in time to prevent case rupture even with an instantaneous re-

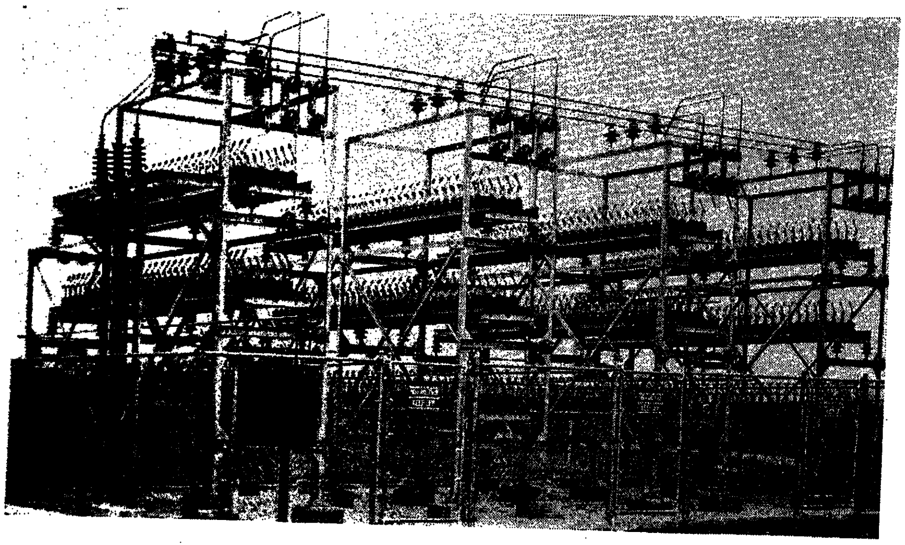


Figure 6. Outdoor open capacitor bank with individual expulsion-type fuses

lay, and in such cases this type of protection is sometimes used as a supplement to fuses. Since this arrangement does not protect against a line-to-line or a line-to-ground fault, line current transformers are also ordinarily used.

A differential current protective arrangement which may be used is that shown in Figure 14 of the AIEE report. Each phase is divided into two sections, and current transformers are differentially connected in each section so that under balanced conditions no current flows in the relay coil. When a unit failure occurs, the resulting current unbalance flows through the relay coil, picking up the relay and tripping the circuit breaker. This arrangement offers no appreciable advantage over that described in the previous paragraph, and since it is usually more expensive it is little used.

Still another transformer and relay protective arrangement uses voltage unbalance between phases as the operating signal; a sketch is included as Figure 11 of the AIEE report. The primary winding of a potential transformer is connected in parallel with the capacitors in each phase of the bank, and the secondary windings are connected in broken delta, with a voltage relay connected across the break in the delta. Failure of a capacitor unit results in a voltage unbalance across the relay coil, causing the relay to operate and trip the circuit breaker. In common with other relay-transformer arrangements, this means of protection requires de-energizing the entire bank when a single unit fails. Often, tripping of the circuit breaker cannot be attained prior to container rupture, and fuses are required if such protection is desired. However, this arrangement will trip the circuit breaker if a line-to-line fault occurs in the capacitor bank, and also in the event of a line-to-ground fault if the system neutral is grounded.

Thermal relays are occasionally used, as a supplement to other types of protective arrangements, to detect abnormal operating temperatures and initiate corrective action before damage is done. Thermal relays are used extensively to control operation of forced cooling equipment, such as fans, permitting intermittent operation only as required to meet operating conditions.

Lightning Protection

In common with other apparatus connected to a power system, capacitors should be protected by lightning arresters. Here the primary function is protection of the capacitor bank, and not the system itself. The choice between lightning arresters rated for grounded neutral service or ungrounded neutral service should be in accordance with established industry practices. The rating of the lightning arrester (line-to-ground) is determined by the system grounding, and is not related to the grounding, or lack thereof, of the capacitors themselves.

Occasionally the suggestion is made that lightning arresters are unnecessary for capacitor banks which are Y-connected with grounded neutral. It is true that capacitors so connected do have some ability both to slope off the steep front of an incoming wave and to reduce its crest value. However, this ability is limited by the size of the capacitor bank, while the amount of energy to be expected in a given surge or lightning stroke is indeterminate. Hence, granting that the grounded capacitor tends to help protect itself, this ordinarily should only be regarded as a fortuitous circumstance, and not as a justification for omitting the lightning arresters. Also, there are generally switches or circuit breakers associated with the capacitor installation which require lightning arresters. Their need for protection is greatest when in the open position, with the capacitor bank de-energized. Hence, the lightning arresters should be connected on the line side of any circuit-interrupting or isolating devices at the capacitor installation.

A related voltage-stress problem is whether or not the capacitor cases should be grounded. This decision is usually based on the operator's safety practices. Some operating groups require that the frameworks, tanks, and cases of all apparatus be grounded for safety, while others require that they be ungrounded for the same reason. Let it suffice, therefore, to point out that if the capacitor case is grounded, the full voltage-to-ground appears across one bushing in parallel with the terminal-to-case insulation inside the capacitor. If the case is not grounded, the voltage-to-ground will divide across the bushing, the internal insulation, and the insulation of the supports. Obviously this division in any given bank will depend on the size and connection arrangement of the bank and on the details of its supporting structure.

Conclusions

1. Capacitor units in standard voltage and kilovar ratings may be used as convenient "building blocks" to make up practically any size bank desired, for practically any circuit voltage.

2. Like other system equipment, capacitor banks require adequate protection in order to maintain a high degree of continuity of service to power purchasers.

3. There are several protective functions to be performed, and a variety of protective arrangements to perform the desired functions.

4. Engineering data obtained from tests and presented in this paper permit correlation of the time-current characteristics of the protective arrangement with the time-current characteristics of the individual capacitor unit under fault conditions.

5. For specific cases, it may be desirable to supplement fuses, or to use an alternative arrangement, at the discretion of the operator or the manufacturer; however, fuses are the most common, and the generally recommended means for obtaining system protection against capacitor unit failure.

Reference

1. REPORT ON THE OPERATION AND MAINTENANCE OF SHUNT CAPACITORS, AIEE Committee Report. *AIEE Transactions*, volume 68, part II, 1949, pages 1208-19.

Discussion

A. E. Peltosalo (General Electric Company, Pittsfield, Mass.): The authors have presented an excellent paper outlining the parameters involved in protective practices and connection arrangements for shunt capacitor banks. The time-current characteristic curves contained in Figure 1 of the paper are worthy of close scrutiny.

The safe limits of capacitor behavior under fault conditions are subject to many different interpretations. A capacitor failure which results in even a small crack in the container, but allows leakage of the dielectric liquid may be intolerable, as in locations where the general public has access to the area below the capacitor installation. However, in many locations, such as a substation, temporary dripping of liquid may not be regarded as very serious. Similar considerations must be given to the degrees of damage which may result from rupture of the capacitor container. The authors' paper has shown that, if sufficient energy is put into the unit, the container may rupture with sufficient violence to damage adjacent units and to endanger any personnel in the immediate vicinity. There are, however, many locations where the rupture of a container might not be objectionable, for example, a capacitor bank completely enclosed by a wire fence in an isolated location.

To assist system operators in the fusing of capacitor units, the curves of Figure 1 include both a safe and unsafe area with a band between. Experience has shown that this intermediate band is usually safe for most conditions of installation, although an occasional crack in a seam or bushing may occur. These curves might well be called damage curves, because by means of these curves the operator is able to predict the damage resulting from particular values of fault current and time.

It is the usual practice to group-fuse pole-type unit installations (approximately 60 per cent of all power factor capacitors are installed in this manner). If, for instance, it is acceptable to fuse up to curve 2, rather than curve 1, the cost per kilovar of the larger capacitor bank (made necessary by this arrangement) may be smaller. This is due, of course, to the allocation of the installation cost (line crew's time, lightning arresters, hangers, and cutouts) to the larger bank. For these installations it now is common practice to use fuse links which have a total clearing time-current characteristic below curve 2 (it may or may not be below curve 1). For example, a plot of the time-current characteristics of General Electric universal fast-blowing cable-type fuse links will show it possible to use fuse ratings up to and including a 75-ampere size and still remain below curve 2. These time-current characteristics will fall below curve 2 and provide satisfactory protection in most cases.

A similar comparison can be shown for the fuse links of any manufacturer, as long as the total clearing time-versus-current characteristic is known. By means of this type of comparison the operator is able to predict the possible damage resulting from a capacitor failure and can therefore set the limits of fuse sizes for any particular application. Occasionally operators have used fuses with characteristics above curve 2. Experience has shown that some damage to adjacent units may result from this manner of fusing. Some operators have even made a practice of omitting fuses entirely, since the chance of capacitor failure is quite small, and since the damage resulting from failure would not be seriously objectionable. Thus, there are wide variations in fusing practice, reflecting different evaluations of the possible degree of damage by different operators.

To obtain the minimum cost of installation consistent with safety it is well for eacy capacitor user to verify the amount of damage tolerable in case of a capacitor failure in any particular location. To this end the curves of Figure 1 should prove very useful.

Harry P. St. Clair (American Gas and Electric Service Corporation, New York, N. Y.): In connection with this paper on connection arrangements for shunt capacitors, it may be of interest to describe briefly the recent program carried out on the American Gas and Electric Company system in the application of high-voltage capacitors. Following an investigation which disclosed the favorable economics of high-voltage shunt capacitor banks in fairly large sizes, several installations were undertaken at 44 kv, followed by several more at 33 kv and one at 22 kv. A partial list of these installations already in service or authorized is shown on Table I of the discussion.

Table I

Location	Capacity, Kva	Voltage, Kv	Control
Logan	10,000	44	Manual
Sprigg	10,000	44	Time Clock
Allen	6,600	44	
Mike Grady	6,600	44	
Scarbro	13,200	44	
Bellefonte	12,000	33	
Fremont	6,000	33	
Fieldale	6,000	33	
Kenton	3,300	22	

In all of these installations the entire capacitor block is controlled by a single switch, and the voltage boost produced by the capacitor varies from three or four volts to six or seven volts. With a voltage change of this magnitude automatic control by voltage relay is not considered feasible and time clock control, arranged to switch the capacitor on over the normal load period of the day, is used instead. It is considered that a large voltage change occurring only twice a day, once in the morning and once in the evening, will not be as objectionable as more frequent voltage changes, even of smaller magnitudes. For the most part, the 44-kv capacitor installations are in coal mining territory where close voltage regulation is not as critical as in urban areas.

In several instances, these large high-voltage capacitor installations have been applied at locations where a synchronous condenser or even generators are available for voltage regulation so that the capacitors provide a supplemental boost, leaving the job of fine regulation to the condenser. This has worked out very well, and it is expected that the practice will be followed even more extensively in the future.

The problem of switching at first appeared to limit the size of a single 44-kv step to about 10,000 kva, but further tests carried out by the switch manufacturers showed that a much larger block, up to 30,000 kva, could be handled by one switch. It also now appears that large capacitor blocks can be installed and operated at 69 kv at about the same unit cost as those already installed at 44 kv. The switching problem up to blocks of 30,000 kva appears to offer no difficulty and it is expected that fairly early use of this technique will be made.

In one case, which may be of interest, a large saving was realized by installing a 20,000-kva 11-kv capacitor bank on the tertiary winding of a 37,500-kva transformer bank, originally designed to accommodate a 20,000-kva synchronous condenser. With a 15,000-kva condenser already in operation at this station, the control problem offered no difficulty and the capacitors provided the needed additional correction.

N. R. Clark and **S. B. Farnham**: Mr. Peltosalo's discussion is a welcome amplification of the considerations which apply in using the time-current characteristic curves included in Figure 1 of the paper.

The use of capacitors in comparatively large banks on circuits above 15 kv, as mentioned by Mr. St. Clair in his discussion, is an application which is gaining increasing acceptance. While one of the basic principles of capacitor application is that they be located as near the load as possible, it is sometimes desirable to locate banks to supply bulk kilovars on the subtransmission system. This has been done in many cases by connecting the capacitor bank to the low-voltage tertiary winding of an existing transformer. When such a connection is not possible, it is quite feasible in many cases to take advantage of the flexibility of capacitors and utilize a high-voltage bank connected, with a suitable switching means, directly to the power line.

Design of Mobile Radio Communication Equipment for Land-Mobile Services Operating on Frequencies Between 152–174 Megacycles

R. A. BEERS
NONMEMBER AIEE

W. A. HARRIS
NONMEMBER AIEE

A. D. ZAPPACOSTA
NONMEMBER AIEE

Synopsis: The rapid growth of the relatively new 152–174 megacycle mobile communications band and the resultant problems are discussed briefly. Equipment has been designed to double the number of usable channels in the same locality for most applications. A description of the equipment and its performance is presented.

SINCE the opening of the 152–162 megacycle band on an experimental basis to the land-mobile services in 1945, its utilization has been so rapid that available channels for assignment to certain types of services have about reached the exhaustion point in many localities. This band was set up on the basis of 60-kc channels and a block of continuous channels was designated for each type of service. Actual assignments have been on an alternate channel basis in the same locality.

With alternate channel operation in the same locality, it was not long until considerable interference between stations was encountered. Some of this interference was caused by insufficient alternate channel selectivity in the intermediate frequency portion of the receiver. In other instances where the intermediate frequency selectivity was adequate, the receiver front end selectivity was insufficient and the receiver became desensitized by a strong alternate channel signal and hence could not receive the desired signal.

In areas where three or more alternate channel services were simultaneously in operation, a type of interference was encountered which was not anticipated. The alternate and next alternate channel signals mixed in the receiver front end, and produced a third signal whose frequency was exactly the same as that to which the receiver was tuned.

Example:

Desired signal frequency = 152.00 megacycles
First alternate signal frequency = 152.120 megacycles
Second alternate signal frequency = 152.240 megacycles

$$\begin{array}{r} 2\times 152.120 = 304.240 \\ 152.240 \\ \hline 152.000 \text{ megacycles the desired frequency} \end{array}$$

The same condition also exists if the two unwanted signals are 120 and 240 kc respectively, below the desired signal frequency.

Mathematical exploration showed that this condition, which now is generally called intermodulation interference, existed because of third-order plate characteristic curvature of the receiver radio-frequency amplifier tubes and fourth-order plate characteristic curvature of the mixer tube, and laboratory work pointed to the fact that the mixer tube was the worst offender. It was found that two transmitters located closely together and separated by 120 kc in frequency also would produce and reradiate off-frequency signals, both above and below their actual frequency, whose frequency would be their carrier frequency plus or minus the frequency difference between the two transmitters. Transmitter intermodulation usually is much less serious than receiver intermodulation, unless the two transmitter antennae are located very closely together.

As the use of the service grew beyond expectations, it was soon apparent that more channels would become a necessity, particularly in the metropolitan areas. Adjacent channel operation in the same locality would double the number of channels, but was considered impractical by many for one or more of the following reasons:

1. Difficulty of obtaining the required selectivity and over-all stability.
2. Transmitter modulation products and spurious radiations must be materially reduced.
3. Receiver intermodulation.
4. Size of mobile equipment to give desired performance.
5. Equipment cost.

Before adjacent channel operation was

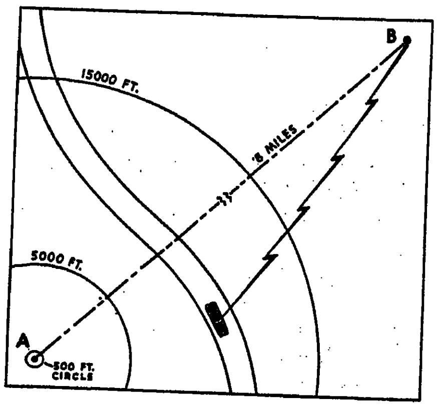


Figure 1. 2-signal adjacent channel test

Paper 49-256, recommended by the AIEE Communications Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 19, 1949; made available for printing September 13, 1949.

R. A. BEERS, W. A. HARRIS, and A. D. ZAPPACOSTA are all with the Radio Corporation of America, Camden, N. J.

The authors wish to acknowledge the assistance of E. F. Zimmerman and W. W. Coxe.

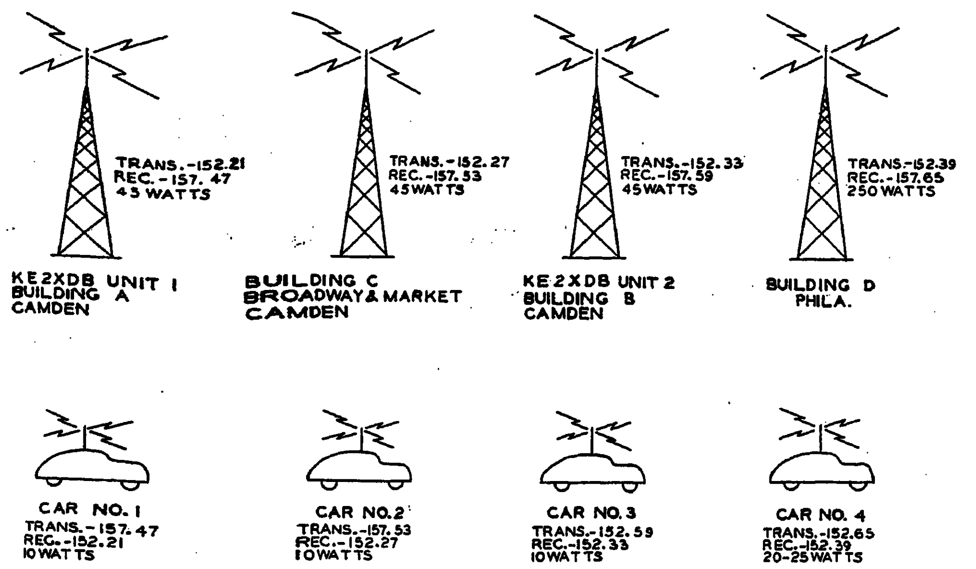


Figure 2. 4-signal adjacent channel test

discarded as impractical, a thorough investigation of the matter was undertaken to determine if the problems involved could be adequately solved.

Field Tests

Preliminary Operational Tests

Extensive field tests were carried out with transmitters of various power rating from 2 to 30 watts under conditions of favorable terrain and in various industrial sections such as Philadelphia, Pa., and Camden, N. J., to determine what power was needed in the transmitter and what sensitivity was required of the receiver. Data collected on the Radio Manufacturers Association's recommendation was gathered on the maximum temperature and humidity conditions to be expected anywhere in North America. Calculations and some field tests were made to determine the maximum adjacent channel signal strength to be expected from a land station of 250-watt power in a mobile receiver to determine the required receiver adjacent channel selectivity. This was necessary so that the mobile receiver would be able to receive a weak desired signal when located very close to a powerful land transmitter. Adjacent channel field tests were run using a very selective receiver to determine the degree of modulation control required to allow practical adjacent channel systems. Tests were run with a number of different persons, both male and female voices, under widely varying conditions of ambient noise with abrupt changes in sound level to evaluate the performance of various modulation limiters for the transmitter.

Two Adjacent Channel Tests

Two 45-watt land station transmitters, designated as A and B, with a carrier separation of 60 kc, were operated simultaneously, see Figure 1. The 1/4 wavelength ground-plane antennae for both stations were approximately 100-feet high.

Using mobile equipment available in late 1948, the mobile receiver was interfered with to such an extent that the desired signal was "captured" by the nondesired signal whenever the mobile car was within one to three miles of the adjacent channel 45-watt land station transmitters.

The new mobile receiver was able to listen to the desired frequency without interference until it was within 250 feet or less of the adjacent channel 45-watt transmitter.

Four Adjacent Channel Tests

In order to best simulate normal operating conditions, the tests were run in conjunction with two commercial services that were already in operation. These services were the Yellow Cab Company of Camden, N. J., and the Bell Telephone Company of Pennsylvania.

The equipment in building C was operating on 152.27 megacycles and 157.53 megacycles, while building D was on 152.39 and 157.65 megacycles. Experimental transmitters were placed in operation on 152.21, 152.33, 157.47, and 157.59 megacycles. This resulted in four adjacent channel systems, each having two frequencies. The arrangement is shown in Figure 2.

In the first test that was run, the four mobile units were parked in front of building A which housed one of the transmitters. The four land station transmitters were separated from the mobile receivers by 100 feet, $1^1/_2$ blocks, 7 blocks, and $2^1/_2$ miles respectively. All four of the mobile receivers responded with clear and interference-free reception. A sketch of this test is shown in Figure 3.

In the second test the procedure was reversed and the mobile transmitters were put on the air. The equipment in building B was remote controlled so that the signals could be heard in building A, and station receivers were installed on the two commercial channels with antennae

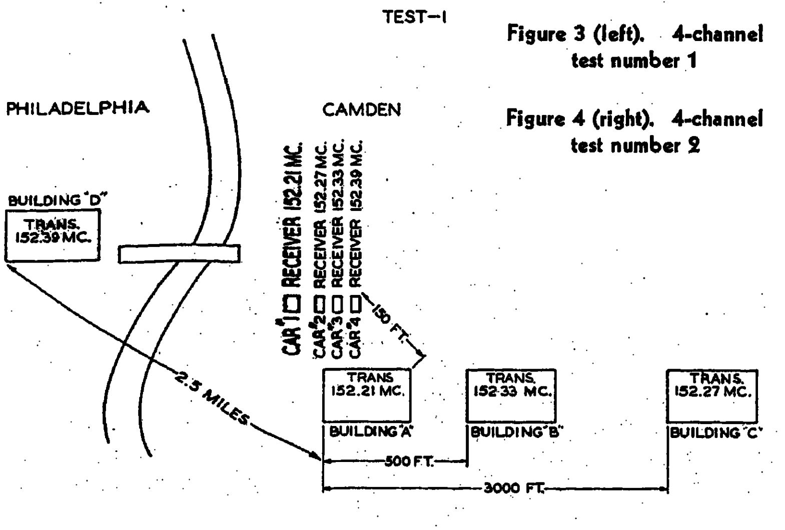
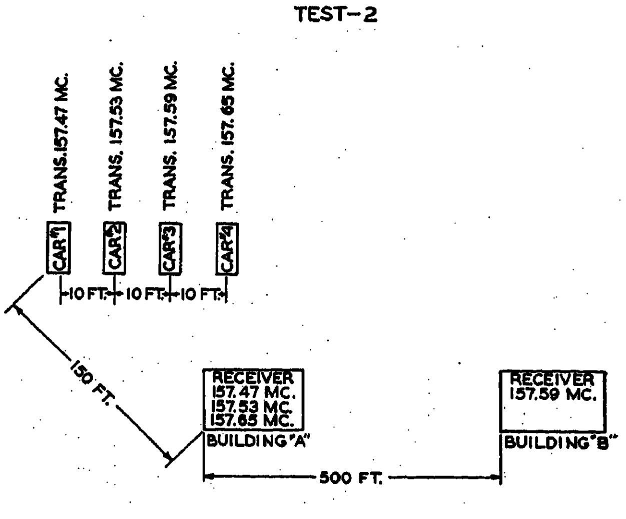


Figure 3 (left). 4-channel test number 1

Figure 4 (right). 4-channel test number 2



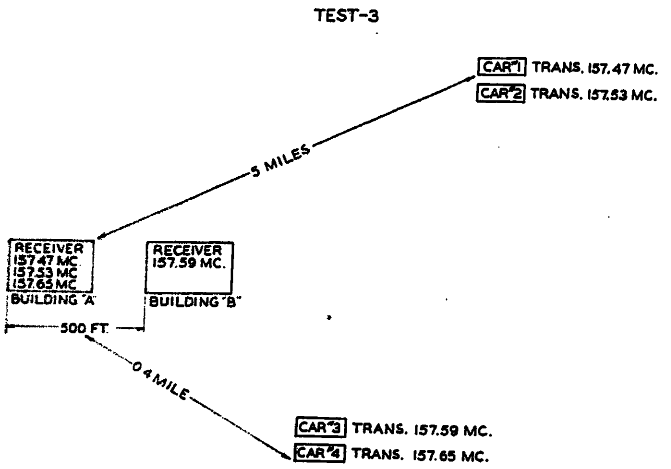


Figure 5. 4-channel test number 3

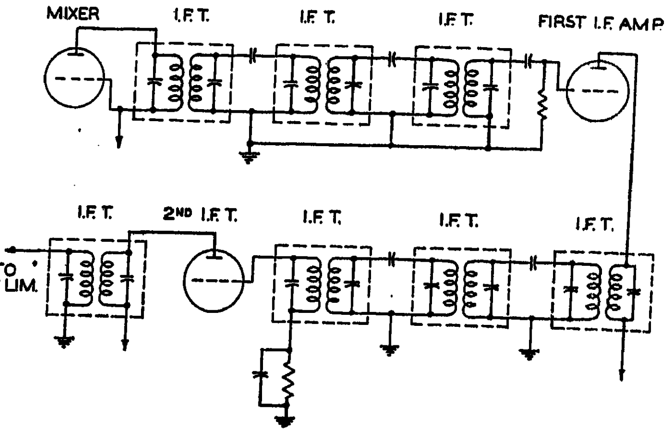


Figure 6. A portion of receiver intermediate-frequency amplifier

on building *A*. In this way all of the mobile transmitters could be heard from one building. The second test was as successful as the first in that signals heard in each receiver were free from interference. The sketch of this test is shown in Figure 4.

The outstanding improvements in intermodulation response of these receivers were observed in the third test of the series. Mobile units numbers 2 and 3 were placed 0.4-mile from building *A* and mobile units numbers 1 and 4 were placed 5 miles away.

The two nearby mobile transmitters produced intermodulation products in the receivers tuned to the distant transmitters, as shown in the preceding section. However, the distant mobile transmitters, were able to call in from a distance of 5 miles with no interference, as shown in Figure 5.

If the mobile units that were producing the interference were brought in nearer, some intermodulation response would have been noted, but they could come very close before they would block out the transmitters which were 5 miles away. This represents a remarkable improvement over the older designs which have an interference area of about a mile and a half as compared to approximately 0.4-mile with the new Radio Corporation of America equipment.

When a 4-channel system is put into operation using this equipment, interference-free reception could be expected in all cases except where two interfering channels are within 0.4-mile of the receiving antenna. For practical operating conditions, the number of times that two interfering transmitters will be operating simultaneously within 0.4-mile from the receiver will be small enough to provide very satisfactory performance.

The object of these field tests was to determine the parameters necessary to produce a new product economically while assuring adequate performance. The following decisions were made in order to provide adjacent channel operation in a low cost mobile system:

Mobile Receiver

1. Sensitivity—0.5 microvolt for 20-decibel quieting.
2. Adjacent channel selectivity—100-decibel attenuation at ±60 kc.
3. All spurious responses to the attenuated 85 decibels.
4. Frequency stability—0.003 per cent from −30 to +60 degrees centigrade, ambient.
5. Frequency range—152 to 174 megacycles.

Mobile Transmitter

1. Power output of 12 to 14 watts.
2. Modulation limiter which produces a high average modulation on sudden increase of speech energy. The limiter should prevent overmodulation and introduce no distortion or microphonics.
3. All spurious emissions to be attenuated at least 60 decibels.
4. Frequency stability—0.003 per cent from −30 to +60 degrees centigrade, ambient.
5. Frequency range—152 to 174 megacycles.

The land station equipment must necessarily equal or better the performance of the mobile receiver and transmitter as outlined before.

Receiver Design

General Requirements

It was determined that sufficient intermediate frequency selectivity and over-all system stability could be achieved without too much difficulty for adjacent channel operation in the same locality if a peak transmitter frequency deviation of ±15 kc and a maximum audio-modulating frequency of 3,500 cycles per second was maintained.

Measurements and tests indicated that the receiver should have a sensitivity in the order of 0.5 microvolt input for 20 decibels of noise quieting, because the ambient noise level was found to be low enough in a number of areas where the equipment may be used to provide reliable communication on a 0.5-microvolt signal if the receiver had the required sensitivity.

As far as the intermodulation problem is concerned, it was reasoned that intermodulation should be very little more severe with adjacent channel operation than with alternate channel operation. Little radio-frequency selectivity can be achieved with components that will fit into a mobile receiver at either 60 or 120 kc off resonance. Anything that could be done to improve the receiver's susceptibility to intermodulation interference and desensitization by improvements in circuit design, aside from increased radio-frequency selectivity, should therefore be equally effective for adjacent or alternate channel operation. From results of these early calculations and tests, a receiver was designed incorporating the features believed necessary to make adjacent channel operation in the same locality possible.

Circuits Required

One hundred decibels of selectivity was provided at 45-55 kc off resonance. This selectivity was obtained with more or less conventional intermediate frequency transformers, except that three transformers were coupled together between the mixer and first intermediate frequency amplifier stages, and three more transformers were coupled together between the first and second intermediate frequency amplifier stages, see Figure 6. The over-all receiver is designed so that

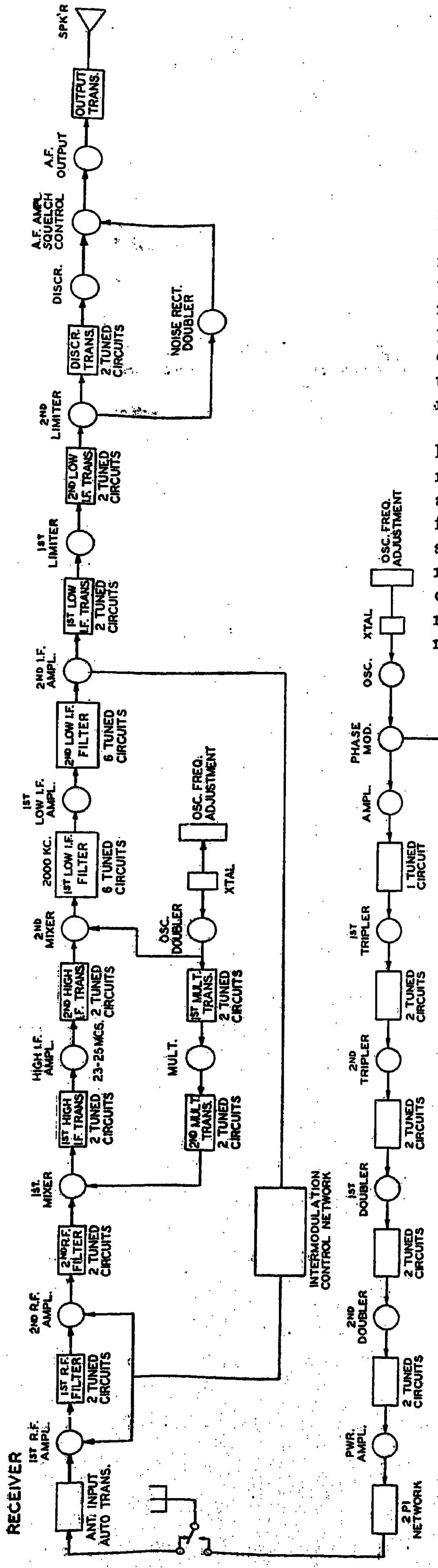


Figure 7. Block diagram of equipment

most of its gain occurs after the major portion of its selectivity, thereby permitting the receiver to receive a very weak desired signal with a strong undesired signal present.

The sensitivity requirement was met by using a type *6BH6* pentode tube, triode connected in a grounded-grid radio-frequency amplifier circuit, see Figure 8. This circuit provided approximately the same amount of sensitivity that could be obtained with a type *6AK5* in a conventional radio-frequency amplifier circuit at a greatly reduced tube cost, and with the same tube type that is used extensively throughout the remainder of the equipment.

Automatic volume control bias from a limiter stage is applied to the grids of the radio-frequency amplifier tubes. This, along with some refinements in the radio-frequency amplifier and first mixer design, considerably improve the intermodulation rejection performance, because the desired signal will bias off the radio-frequency amplifier stages and reduce the amplitude of the undesired signals at the first mixer grid. Since the selectivity to the automatic volume control takeoff point is in the order of 100 decibels, the undesired signal can never produce a control voltage.

PERFORMANCE OF THE RECEIVER

The series of curves illustrated in Figures 9-14 show the performance of the new receiver. Figure 9 shows the noise quieting selectivity of the receiver. The information for this curve was obtained by inserting sufficient radio-frequency signal input to the antenna at the receiver's resonant frequency to produce 20 decibels of noise quieting. The signal generator was then detuned above and below resonance to 60 kc, the adjacent channel in steps of 10 kc, and the signal generator output increased until 20 decibels of noise quieting was obtained.

Figure 10 shows the receiver sensitivity in terms of signal-to-noise plus distortion ratio versus input microvolts in series with a 50-ohm dummy antenna. The input signal was modulated ±10 kc at 1,000 cycles per second.

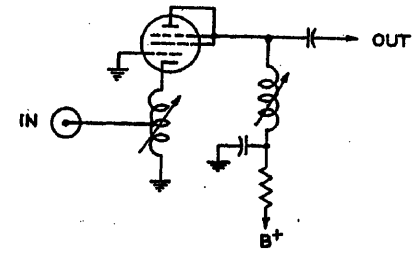


Figure 8. Receiver: first radio-frequency amplifier

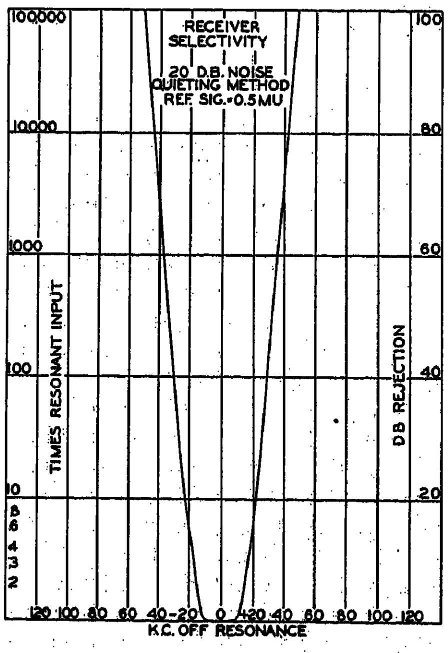


Figure 9. Receiver selectivity

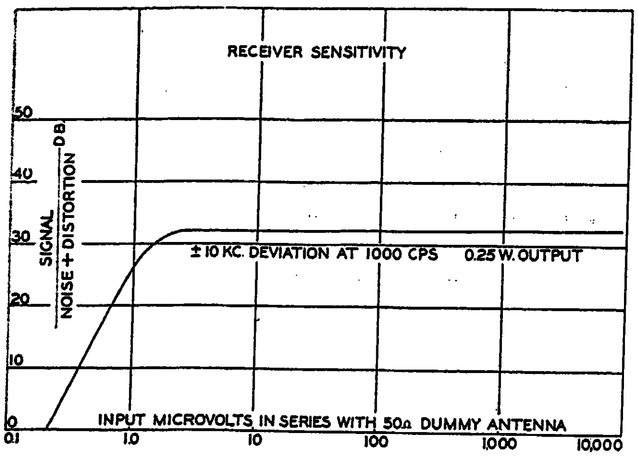


Figure 10. Receiver sensitivity

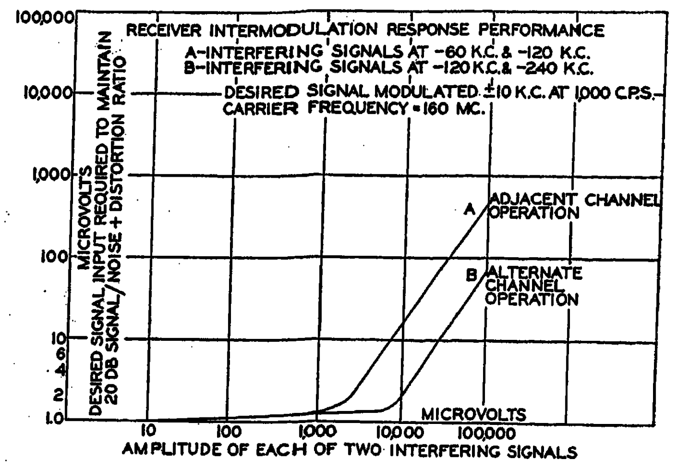
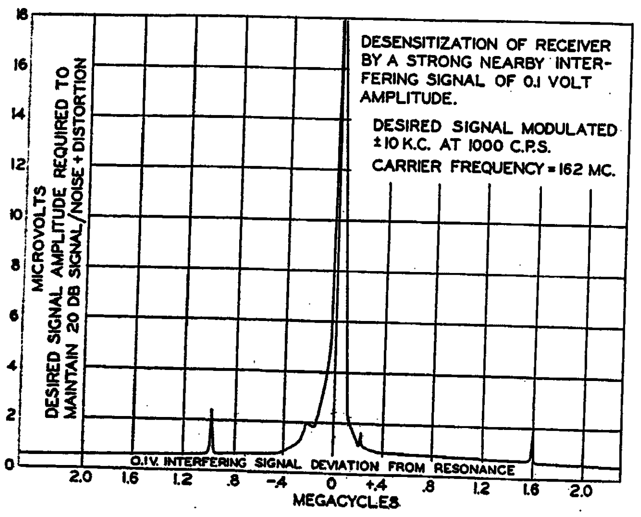


Figure 12. Receiver intermodulation rejection



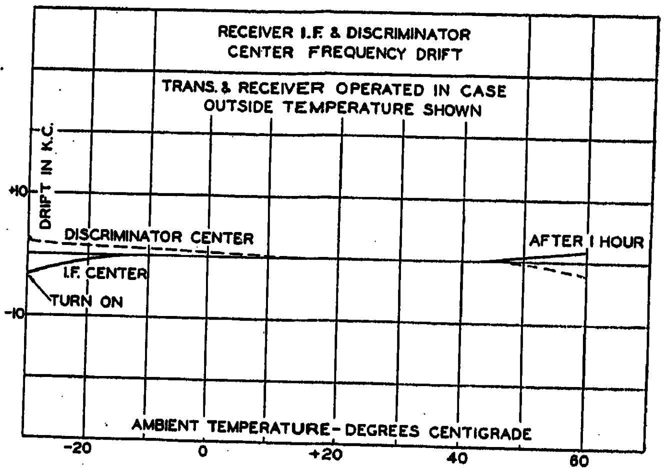


Figure 13. Receiver intermediate-frequency drift

Figure 11. Receiver spurious response rejection

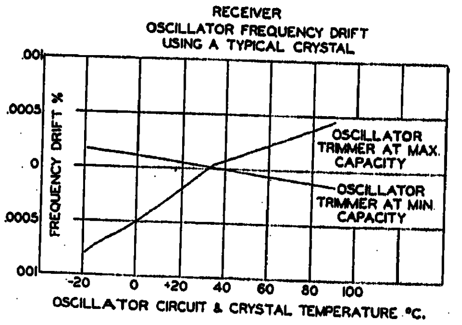


Figure 14. Receiver oscillator stability

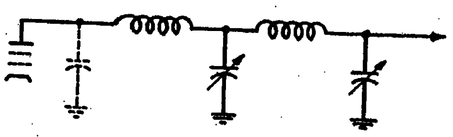


Figure 15. Transmitter output circuit

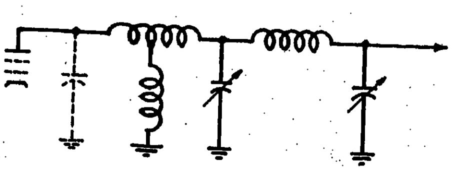


Figure 16. Transmitter output filter circuit

Figure 11 shows how much the receiver is desensitized by a strong signal close to the receiver resonant frequency, and also the spurious responses of the receiver. To obtain this information two signal generators were coupled into the receiver antenna connector. The interfering signal amplitude was kept constant at 0.1-volt and its frequency was varied ±10 megacycles from receiver resonance. The desired signal was modulated ±10 kc at 1,000 cycles per second and its amplitude was adjusted to maintain a 20-decibel signal-to-noise plus distortion ratio.

Figure 12 shows the performance of the receiver under conditions causing intermodulation. Curve *A* is for two signals, −60 kc and −120 kc off resonance, and curve *B* is for two signals, −120 kc and −250 kc off resonance. The amplitude of each of the two interfering signals was equal for each measurement, and the amplitude of the desired signal was adjusted for each measurement to produce a 20-decibel signal-to-noise plus distortion ratio. It will be noted that the receiver's susceptibility to intermodulation response averages only about 9 decibels more for adjacent channel systems than for alternate channel systems. This variation is largely due to the difference in the receiver's adjacent and alternate channel rejection plus the difference in receiver desensitization for the two cases.

Figure 13 shows the intermediate frequency amplifier and discriminator center frequency drift over an ambient temperature range of −30 to +60 degrees centigrade. In making these tests the receiver was operated in its case along with the transmitter, and the transmitter was operated to produce the normal temperature rise inside the equipment case.

Figure 14 shows the complete oscillator circuit frequency drift with a typical crystal and with the oscillator frequency trimmer capacitor set at both maximum and minimum capacity.

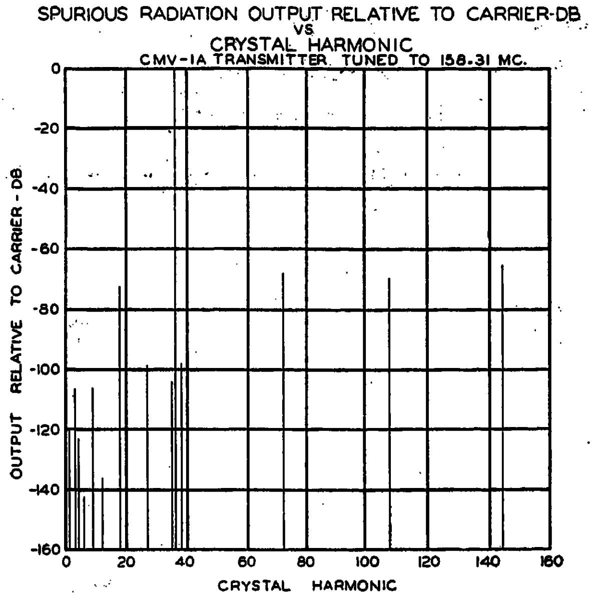


Figure 17 (left). Transmitter spurious emission

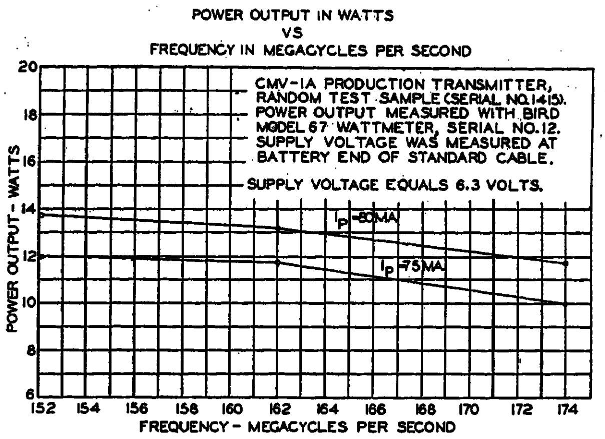


Figure 18 (right). Transmitter power output

Transmitter Design

Spurious Emissions

The problem of reducing spurious emissions from the transmitter was attacked by using a low crystal multiplication, by using double-tuned circuits in all multipliers and using a maximum per stage multiplication of three, by filtering all $B+$ leads and by using a 2-section band-pass filter for matching the power amplifier to the antenna.

A transmitter using phase modulation normally employs considerable frequency multiplication in order to achieve ±15-kc carrier frequency deviation at low-audio frequencies. The phase modulator used in this apparatus is capable of approximately ±90 degrees shift so that a multiplication of only 36 is required to allow full deviation for a 300-cycle modulating frequency. This multiplication is arrived at by use of two triplers and two doublers.

All multiplier stages were double tuned in order to obtain the selectivity required. Double tuning also is quite helpful in reduction of difficulty caused by ground currents, since magnetic coupling allows circuit isolation except for stray capacity coupling.

All $B+$ leads were filtered by resistance-capacitance combinations in order to reduce coupling from this source. Resistors are actually better than chokes for this service since they do not have resonant transmission points and therefore filter over an extremely wide band. An additional advantage of this method of isolation is that trouble shooting is considerably simpler in the case of $B+$ short circuits, since the resistors will either burn up and act as a visible blown fuse or will provide isolation so that an ohmmeter can be used to detect the bad circuit.

In the initial design work of the power amplifier output circuits, we had planned to use a simple 2-section network of the low-pass type as shown in Figures 7 and 15. While this operated as predicted to reduce carrier harmonics, it allowed transmission of a one-half carrier frequency component which would normally have been eliminated by a parallel resonant circuit in the plate of the power amplifier; a parallel resonant circuit does not, of course, eliminate carrier harmonics to anything like the extent of the low pass *pi* circuit under consideration. This one-half carrier frequency component was getting through the doubler plate and power amplifier grid circuits with sufficient amplitude to be amplified by the power amplifier and cause trouble. Accordingly the first *pi* circuit was changed to a band-pass network by the addition of a shunt coil tapped on the first series inductance, as shown in Figure 16.

Care was taken in circuit layout, shielding, and grounding in order to prevent coupling around the circuits provided. Particular attention was paid to the antenna changeover relay, which is of a type proved by many years of successful aircraft service to give less trouble than has been encountered with available coaxial types. The relay is carefully shielded, yet the contacts are readily accessible for service.

The engineering model showed no spurious emission in excess of 70 decibels below carrier frequency, as shown in Figure 17. Measurements were carried from 4.2 megacycles (the crystal frequency) to about 800 megacycles. Production models which have been tested have shown variations of about ±6 decibels from the values given in Figure 18.

Power Output

The total power amplifier plate network efficiency is slightly better than that which is obtained by use of a more conventional parallel tuned circuit, thus allowing a power amplifier efficiency of approximately 50 per cent. This efficiency is quite good for a *2E26* tube with only a 310-volt plate supply at 152-174 megacycles.

Power output versus frequency is shown in Figure 18.

Modulation Control

The phase modulator design and multiplication employed allowed us to achieve over-all transmitter distortions as low as 2 to 5 per cent.

To control deviation without adding distortion or microphonics, a transducer modulation control (TMC) system was devised. The TMC system takes advantage of the fact that output from a carbon microphone is approximately proportional to microphone button current below a saturation value. The circuit is extremely simple, as shown in Figure 19. The audio output from the microphone transformer is differentiated by *C-1*, *R-1* to give a pre-emphasis characteristic and applied to the amplifier rectifier tube *V-1*. Thus from this rectifier a d-c voltage is derived which is proportional to the fre-

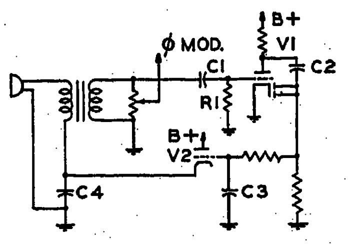


Figure 19. Transducer modulation control circuit

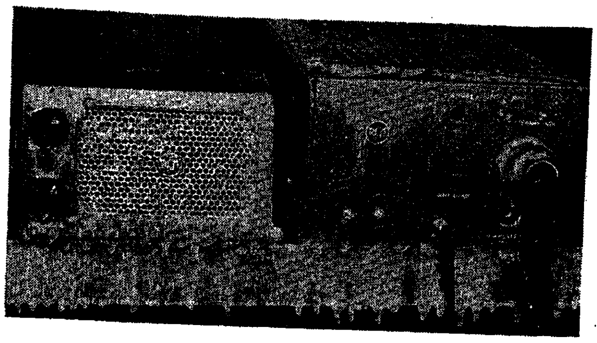


Figure 20 (top left). Equipment temperature tests

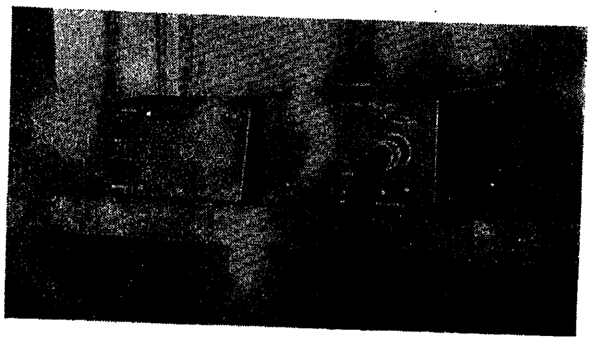


Figure 21 (bottom left). Equipment humidity tests

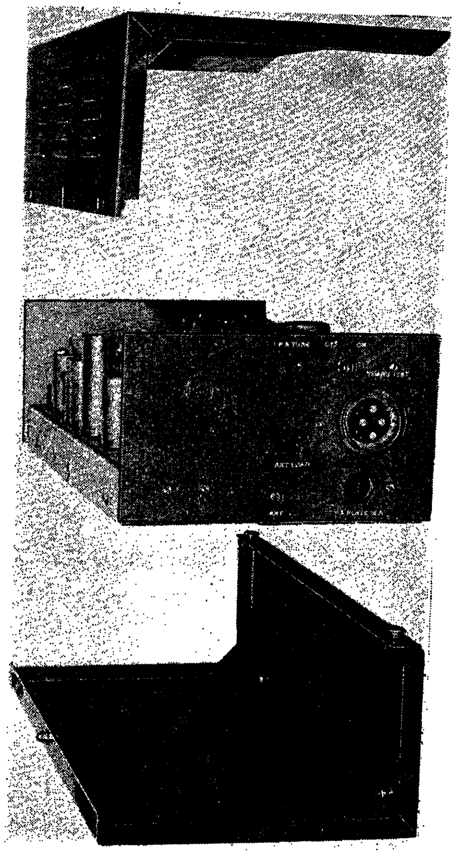


Figure 23 (right). Equipment mounting versatility

quency deviation given by the phase modulator. This control voltage is fed to *V-2* with proper polarity so as to reduce the microphone current should the deviation be increasing above a certain threshold; this reduction in current then reduces the audio input to the phase modulator. The time constants of the resistance-capacitance networks in the d-c line are arranged so that the microphone current is reduced quickly to control deviation and released slowly to prevent appearance of excessive noise between words.

The control range is 20 to 30 decibels; such a wide control range is especially desirable when a carbon microphone is employed, since output from the carbon microphone is quite variable depending on position.

It can be seen from Figure 19 that the audio signal circuit does not go through the limiter tubes at all, thus no distortion or microphonic products are added. Any noise produced by *V-2* is by-passed by capacitor *C-4* and does not affect the signal circuit. Also, it will be observed that the modulation control circuit in no way affects the phase-modulated waves emitted by the transmitter. The increase in frequency deviation with modulation frequency is compensated for by a de-emphasis network in the receiver.

Power Supply

Two power supplies are used for the transmitter. A small power supply is used for either the receiver or for the transmitter low level and audio circuits. A larger supply is used for the doubler-driver and power amplifier stages only.

The small power supply utilizes a self-rectifying vibrator and delivers 50 milliamperes at 200 volts. This supply runs continuously when the equipment is turned on and is switched from receiver to transmitter by contacts on the antenna relay.

The larger power supply delivers 135 milliamperes at 325 volts, using an interrupter-type vibrator and a separate full-wave rectifier. The vibrator has two sets of interrupter contacts which are wired separately to individual primary

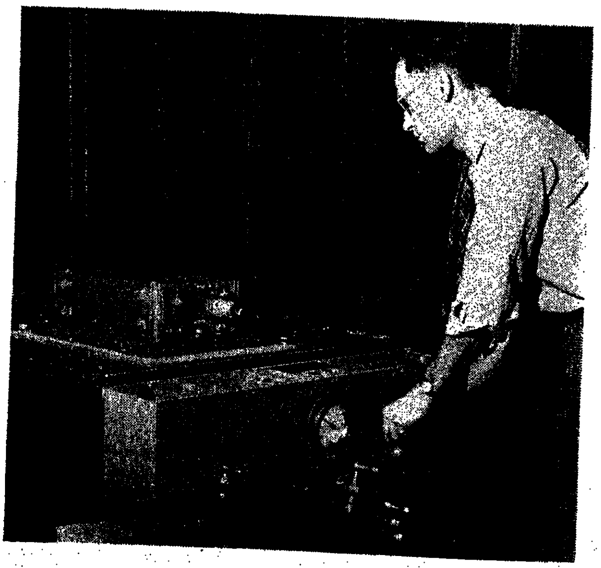


Figure 22 (left). Equipment vibration tests

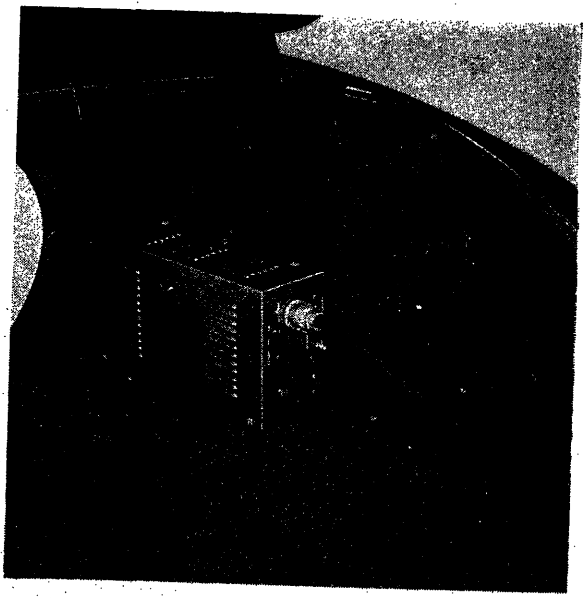


Figure 24 (right). Equipment mounted in car windings on the power transformer to obtain good contact life. The rectifier is either a single *OZ4-A* or two *6X5GT* tubes connected in parallel. The two *6X5GT* tubes may be used in applications where maximum reliability is required on much-used systems. The *OZ4-A* is recommended for normal use where battery drain is important and usage is not too frequent. The *OZ4-A* is a cold cathode-gas rectifier and its life is limited to approximately 15,000 to 45,000 starts.

TRANSMITTER TESTS

Spurious emissions were tested on the antenna line by a substitution method. Each crystal harmonic was measured on a receiver. A calibrated signal generator was then adjusted until the receiver signal strength readings were the same. The ratio of the generator output to the line voltage existing at the carrier frequency was taken as the ratio of spurious signal to desired carrier. It was necessary to trap out a portion of the fundamental carrier frequency to prevent its reaching the receiver; it was found that the receivers used would produce harmonics of their own if fed a very strong signal.

Actual field intensity measurements also were made with equipment installed in an automobile. In this case, field intensities were obtained for the carrier and the various crystal harmonics, and were compared directly. The automobile itself was rotated for maximum pickup and both the horizontal and vertical components of radiation were obtained.

Limiter action was checked by use of an oscillograph connected across the discriminator of a suitable receiver and calibrated for deviation. Normal voice inputs were used with widely varying inflections and levels. Both male and female voices were tried. It was found possible to overmodulate the equipment for the first portion of certain syllables if spoken very loudly after a prolonged quiet period; however, as expected, quality was very good throughout the range of inputs used. To check the seriousness of the occasional short burst which could get through, systems were operated on adjacent channels (60 kc separation) and it was found that while an occasional burst could be heard when the interfering signal was very loud and close to the receiver, it was of such short duration that no intelligence was lost.

System Design Considerations

EXACT FREQUENCY ADJUSTMENT

Simplicity in adjusting the mobile receiver to the land station transmitter was accomplished by providing a vernier capacity in the crystal oscillator circuit to give a frequency adjusting range of ±0.01 per cent. This vernier capacity is adjusted to give zero discriminator voltage when receiving the desired signal. It is not necessary to change the adjustment of the discriminator or intermediate frequency amplifier circuit. The mobile transmitter crystal-controlled oscillator circuit also is provided with a vernier capacitor which is adjusted to give zero discriminator voltage at the land station receiver.

TEMPERATURE, HUMIDITY, AND VIBRATION

To insure optimum performance under extreme temperature variations from -30 to $+60$ degrees centigrade ambient temperature, the completed equipment was carefully tested and measured as shown by Figures 13, 14, and 20. The equipment was tested for several days at 90 per cent humidity and 60 degrees centigrade, as shown by Figure 21. The mobile equipment was designed to operate at optimum performance while subjected to several hours of vibration in three planes with an amplitude of $\pm 1/32$ inch and a frequency variation from 5 to 55 cycles per second, see Figure 22.

MOUNTING AND SERVICING

The mounting base of the mobile equipment becomes one-half of the case. The cover becomes the top of the case, see Figure 23. The chassis is reversible 180 degrees without changing the position of the base plate or of the cover. Also, the equipment can be mounted in two planes. The chassis is removed from the case without requiring any headroom as in a drawer-type case. One main battery cable connects the equipment to the battery and control unit.

No Discussion

A New Loss-of-Excitation Relay for Synchronous Generators

C. R. MASON
MEMBER AIEE

INTEREST in loss-of-excitation protection for synchronous generators has been increasing, judging by the number of AIEE and other papers that have been presented and by the number of inquiries that have been received. Perhaps the principal reason for this increasing interest is that the time-honored use of field-undercurrent and undervoltage relays for loss-of-excitation protection is receiving critical examination, particularly because of the increasing practice of operating generators under-excited during light-load periods.[1,2] It has been reported from several sources that such relaying cannot be adjusted to differentiate between purposely reduced excitation during light-load periods and accidental loss of excitation.[3] It also may be that the increasing size of generators and reductions in reserve capacity are causing growing concern of the possible harm to a machine that may follow loss of excitation.

The consequences that may attend loss of excitation are severe system-voltage reductions, and overheating of the generator rotor. Not only is voltage reduction undesirable in itself, but it also reduces the pull-out limits of other machines.[1] The likelihood of undesirable reduction in system voltage can be determined analytically; systems where automatic generator-voltage regulation is not used are most apt to experience such a difficulty. Concern with rotor overheating is chiefly limited to round-rotor generators without amortisseur windings, but the likely degree of damage is most difficult to determine. A need for further investigation in this direction seems to be indicated. The experience of some electrical utilities would seem to indicate that an alert and skillful operator can recognize a loss-of-excitation condition and take the necessary steps to correct the situation in time to avoid obvious damage to a machine. A relay that could indicate which of several machines was in trouble would be very useful in such cases. Others prefer to rely on automatic protective relaying.[3] It is generally agreed, however, that if excitation is lost and cannot be restored quickly, the generator should be disconnected promptly from the system.

Perhaps the first occasion where the difficulty (of selecting between purposely reduced excitation and accidental loss of excitation) received serious consideration was that which produced the reactive-current relay.[3] This relay uses the a-c quantities at the terminals of the generator, and is capable of greater selectivity than field-undercurrent and undervoltage relays.

Further study of the characteristics of loss of excitation as compared with power swings and loss of synchronism, coupled with new relay developments,[4] has led to simpler relaying equipment having still greater selectivity. A single-phase distance-type relay, commonly called a mho relay, is arranged to respond only to certain relations between the alternating current and voltage at the terminals of a generator. How these relations vary after excitation is lost, and how the relay can distinguish between loss of excitation and other normal or abnormal operating conditions, is described in this paper.

The Loss-of-Excitation Relay

The loss-of-excitation relay, Figure 1, is a single-phase single-element high-speed distance-type relay of the so-called mho family.[4] It is arranged to operate from the voltage between two phases, and the difference between the currents of the two phases, at the terminals of the generator to be protected. The operating element is fundamentally the same as that employed in distance relays, but its operating characteristic is shifted from the position used for distance relaying.

Figure 2 shows the operating characteristic plotted on an R-X diagram, where the co-ordinates are positive-phase-sequence per-unit ohms based on the full load rating of the generator. The center of the circle is on the $-X$ axis. For reasons to be given later, the offset OC is shown to be approximately equal to half of the direct-axis transient reactance of the generator, and OD is approximately its direct-axis synchronous reactance. However, both the offset and the diameter are adjustable. The relay will operate to close its contacts for any impedance vector, such as Z, which terminates within the circular operating characteristic.

In terms of phase-to-neutral secondary ohms under balanced 3-phase conditions (positive-phase-sequence ohms), the offset is adjustable to be 0, 1, 2, 3, or 4 ohms. The diameter of the circle is adjustable between 3 and 30 ohms in 1 per cent steps. Assuming that generator current transformers, having standard 5-ampere secondaries, are chosen so that rated full-load current of the generator is 75 per cent of the current-transformer rating, and that the potential source at rated voltage provides 120 secondary volts phase-to-phase, the offset steps in per-unit ohms become approximately 0, 0.054, 0.11, 0.16, and 0.22. Similarly, the range of diameter adjustment is approximately 0.16 to 1.6 per unit.

Paper 49-260, recommended by the AIEE Relays Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 19, 1949; made available for printing September 6, 1949.

C. R. MASON is with the General Electric Company, Schenectady, N. Y.

The author wishes to take this opportunity to acknowledge the work of Mr. M. Temoshok, who supervised the differential analyzer studies and coordinated the results, and also to thank other of his associates who helped in the planning of the material for this paper.

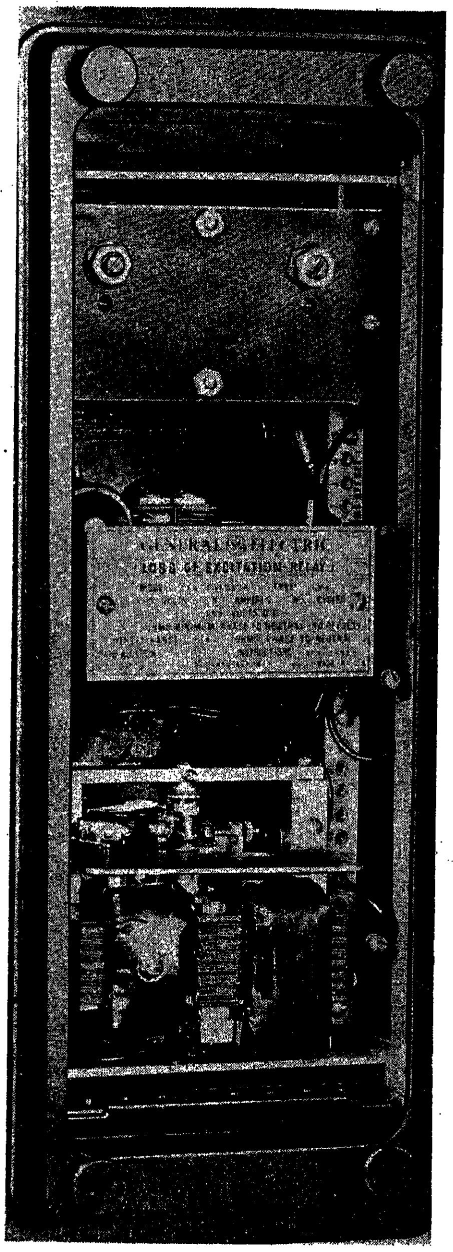


Figure 1. Loss-of-excitation relay

The conventions employed in the use of the *R-X* diagram are as follows. In terms of per-unit ohms at the terminals of the generator under balanced 3-phase conditions, the diagram shows the resistance and reactance components of an impedance which is the ratio of a phase-to-neutral voltage to the corresponding phase current. Positive *R* means that the generator is supplying power to the system, and negative *R* means that the generator is receiving power from the system. Likewise, positive *X* means that the generator is supplying reactive power to the system, as when it operates with high excitation; and negative *X* means that the generator is receiving reactive power from the system as when it operates with low excitation.

In other words, by the use of the *R-X* diagram, there can be shown all possible relations between a given combination of generator current and voltage, as well as a particular area in which these relations will operate a relay which is energized by the same current-and-voltage combination. It remains, therefore, to show the loss-of-excitation characteristic on the *R-X* diagram in order to show how the relay will respond. Then, other normal or abnormal system-operating characteristics will be shown for which the relay should not operate.

The Loss-of-Excitation Characteristic

Differential-analyzer studies have been made of the loss-of-excitation characteristic under various conditions, taking into account the important variables. The technique of these studies was the same as that described in reference 5, and will not be described here; instead, only the pertinent results will be given.

Figure 3 shows typical loss-of-excitation characteristics for the various conditions studied, plotted on the *R-X* diagram together with the suggested relay characteristic of Figure 2. The values of *R* and *X* corresponding to the initial generator loading at the instant before the excitation was affected (or in other words, at zero time) determine one end (labelled zero) of each characteristic. The time in seconds after the excitation was affected is shown at various points along each curve. Each characteristic is terminated at the instant just before the generator first reaches 180 degrees out of phase with the equivalent generator back of the system.

The characteristics do not actually end at those points, but from then on the changes in impedance become so erratic that it is not feasible to try to plot them. Nor do they have any further significance in so far as relay operation is concerned because the relay has already had ample opportunity to operate. If the slip is relatively high, as for heavy initial generator loading, the impedance varies within a restricted area centered approximately on the last plotted point of the characteristic. If the slip is low, as for light initial generator loading, the impedance varies for a while about the last point, then swings entirely outside of the relay characteristic, and then retraces the same loss-of-excitation characteristic until the next pole is slipped, and so forth.

The reason for the erratic changes in impedance is the effect on the generator impedance of erratic changes in rate and direction of slip as poles are approached and passed, as shown in Figure 4. The curves of Figure 3 end at the equivalent impedance of the generator, but as each pole is slipped, there is a change in generator impedance from direct-axis to quadrature-axis values, and these values themselves vary with rate of slip. At very slow slip, as for operation below the knee of the generator slip-torque curve, the variations of impedance are large in comparison with the average and are obtainable only from a differential-analyzer study. But if the machine is operating beyond the knee of the slip-torque curve, the variations in slip are small in comparison with the average value, and the direct- and quadrature-axis impedances can be calculated with fairly good accuracy using the average value of slip. There will still be variation from direct- to quadrature-axis impedance as each pole is slipped, but the region within which the end of the loss-of-excitation characteristic will lie can be fairly well located.

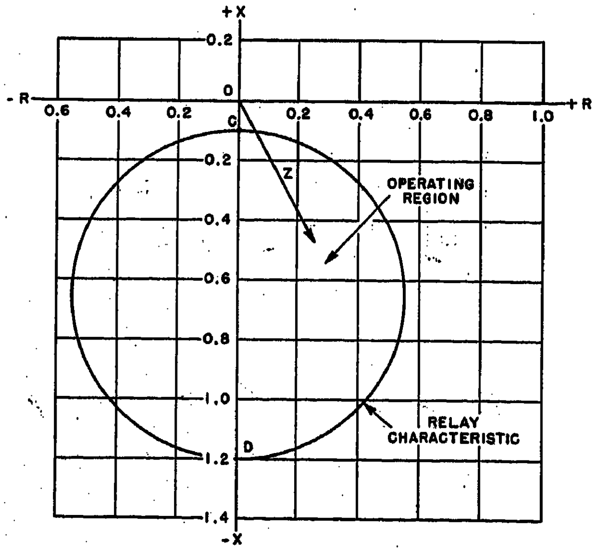


Figure 2. Operating characteristic of loss-of-excitation relay

Figure 5 shows a curve of the average of the calculated direct- and quadrature-axis impedances about which the end of the loss-of-excitation characteristic varies for all possible values of slip of a generator having certain constants. The average impedance is low at high slip, approaching the average of the direct-axis and quadrature-axis subtransient impedances of the generator. At very low slip, the average impedance approaches the average of the direct-axis and quadrature-axis synchronous impedances. Neither extremes are ever actually reached, however.

The foregoing considerations will serve to show that the suggested operating characteristic of the loss-of-excitation relay encompasses an area into which all loss-of-excitation characteristics will enter prior to the generator slipping its first pole. The size of machine, initial loading, governor and voltage-regulator action, and so forth, all affect the final rate of slip which, together with the machine constants, determines where the characteristic ends. In comparison with the relatively slow change in impedance of the loss-of-excitation characteristic, the relay may be assumed to operate immediately its operating region is entered.

Relation Between Loss-of-Excitation and Loss-of-Synchronism Characteristics

A differential-analyzer study is necessary to take into account certain variables and to show how certain quantities vary with time; however, much valuable analytical work has been done in connection with loss of synchronism and its effect on relays, in which certain simplifying assumptions have been made, and the finding of such studies can be applied to this problem. Reference 6 gives loss-of-synchronism characteristics for all possible values of excitation. Since loss of excitation involves both changes in excitation and loss of synchronism, an examination of that paper will provide an excellent insight into the mechanism of loss of excitation.

The essential information to be gained from reference 6 is shown in Figure 6. The line *OA* is the equivalent impedance of the generator, assumed to be the direct-axis transient impedance. The line *OB* is the equivalent impedance of the rest of the system to which the generator is connected, including the direct-axis transient impedance of an equivalent generator back of the system impedance. Three loss-of-synchronism characteristics are shown for three different conditions of the voltage *E* back of transient reactance of the generator in question (which we shall call *A*) relative to the generator back of the system (which we shall call *B*). These three curves are actually arcs of circles centered on extensions of the straight line *AB*. The curve for $E_A/E_B=1$ is a straight line (a circle of infinite radius) and is the perpendicular bisector of the straight line *AB*. There is an infinite number of

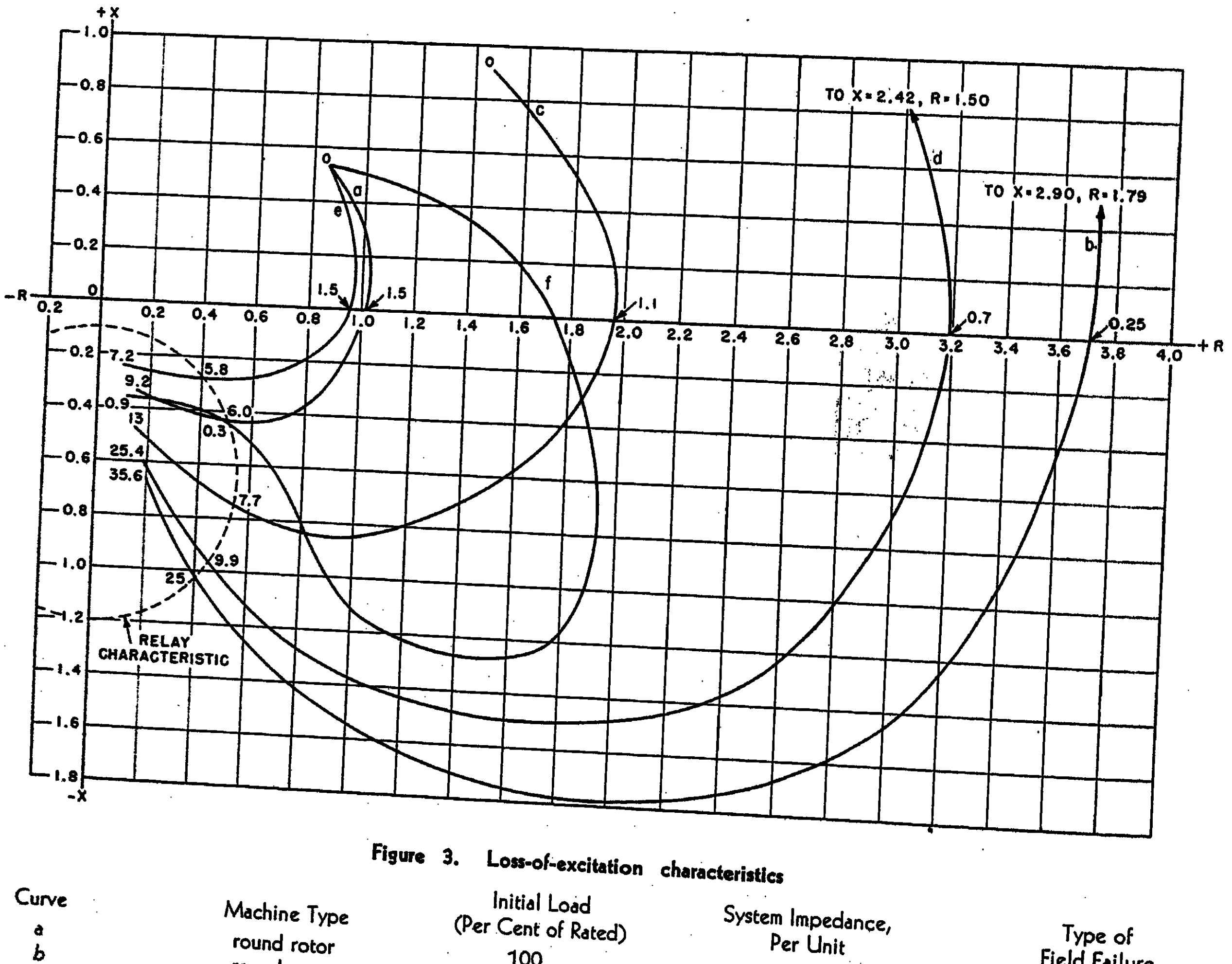


Figure 3. Loss-of-excitation characteristics

Curve	Machine Type	Initial Load (Per Cent of Rated)	System Impedance, Per Unit	Type of Field Failure
a	round rotor	100	0.015+j0.1	short circuit
b	round rotor	29.4	0.015+j0.1	short circuit
c	salient pole	58.8	0.015+j0.1	short circuit
d	salient pole	35.3	0.015+j0.1	short circuit
e	round rotor	100	0.03 +j0.2	short circuit
f	round rotor	100	0.015+j0.1	open circuit

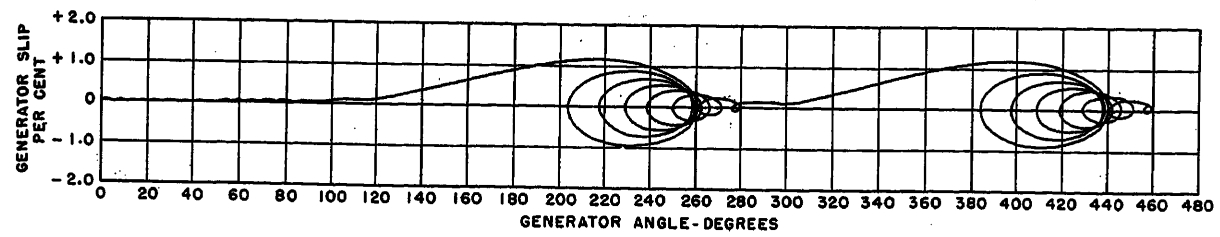


Figure 4. Generator slip versus phase angle by which the generator leads the equivalent system generator

such curves for all possible values of E_A/E_B. For values of E_A/E_B progressively greater than unity, the circles become smaller, their centers move in toward *B*, and finally the circles shrink to the point *B*. Similarly, for values of E_A/E_B progressively smaller than unity, the circles shrink to point *A*.

The arc *PA* of Figure 6 is part of a circle, the rest of which is shown dotted, which passes through *A*, *B*, and *P*, and whose center is on the $E_A/E_B=1$ line. This is the circle for a certain phase angle between the generator at *A* and the equivalent generator at *B*, with generator *A* leading generator *B*. There is an infinite number of such circles for all possible angular relations between the generators, and all of these circles pass through *A* and *B*. As the phase angle increases, the diameter of the circle decreases until its center is at *M* and its diameter is *AB*. At this point, generator *A* is 90 degrees ahead of generator *B*. Further advance in phase angle is represented by circles of increasing diameter with their centers farther and farther to the left of *M*.

As indicated on Figure 6, starting from an initial point *P*, the impedance vector will trace curve *PX* for an increase in phase angle with no change in excitation, and curve *PA* for a decrease in excitation with no change in phase angle. Since loss of excitation involves both types of change simultaneously, it follows that the actual curve lies between the boundary arcs *PX* and *PA*. For rapid decrease in excitation with relatively little advance in angle, as for the case of an open-circuited generator field under light-load conditions, the characteristic lies close to the arc *PA*. But for a short-circuited field having a long time constant, the characteristic lies farther away from *PA*.

The foregoing considerations lead to the question of what happens to the left-hand end of the characteristic. With the simplifying assumptions of this approach to the problem, the characteristic must ultimately terminate at *A*. Whether it would follow the arc *PA* directly to *A*, or would cross the line *AB* between *A* and *X*, and then spiral in a counterclockwise direction around *A*, finally ending at *A*, would depend on the relative rate of change of phase angle and of excitation.

We have obviously reached the point where further speculation is useless. Actually, owing to induction, the effective excitation is never completely lost; the nearest approach is for an open circuit in the field of a generator having no amortisseur. Also, the point *A* is fictitious because the generator impedance is not a constant value equal to the direct-axis transient impedance, as has been previously pointed out. However, this method of analysis has served to provide a better concept of the loss-of-excitation characteristic than could be obtained from the results of a differential-analyzer study alone.

It also has provided a good introduction to the next subject to be considered, which is the effect of power swings or loss of synchronism on the loss-of-excitation relay.

Effect of Power Swings or Loss of Synchronism on the Loss-of-Excitation Relay

The loss-of-synchronism characteristic has been shown already in Figure 6 and explained in the foregoing text. It remains to examine this characteristic for the purpose of determining whether or not the loss-of-excitation relay might operate undesirably during such conditions.

It is probably evident that since these loss-of-synchronism characteristics completely blanket the *R-X* diagram, if the excitation of the generator is sufficiently reduced there is no area in which a loss-of-excitation relay could operate without operating also for power swings or loss of synchronism. Prolonged out-of-step operation of a generator may reduce the machine excitation, even though the source of normal excitation voltage is unaffected, to a point where relay operation would occur. This, however, generally would be advantageous. In fact, Figure 6 does not show the condition that most seriously restricts the location of the loss-of-excitation characteristic, which is when the generator in question is small in comparison with the rest of the system. This condition is shown in an exaggerated way in Figure 7, where the system is assumed

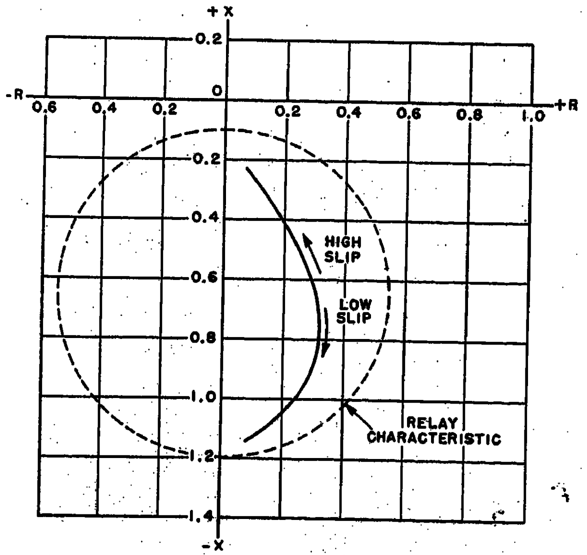
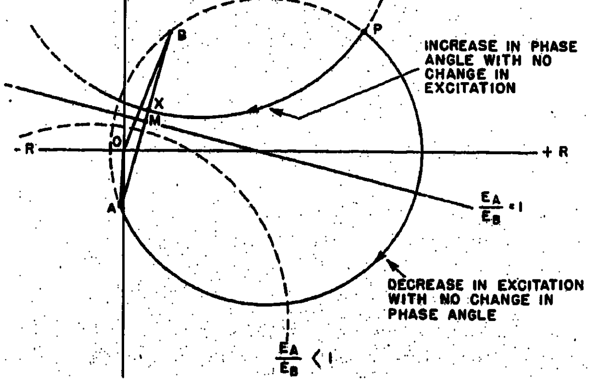


Figure 5 (left). Average of direct- and quadrature-axis impedances for all values of slip

Figure 6 (right). Loss - of - synchronism characteristics


+X
-X
-R
+R
$\frac{E_A}{E_B}>1$
$\frac{E_A}{E_B}=1$
$\frac{E_A}{E_B}<1$
B
P
X
M
O
A
INCREASE IN PHASE ANGLE WITH NO CHANGE IN EXCITATION
DECREASE IN EXCITATION WITH NO CHANGE IN PHASE ANGLE

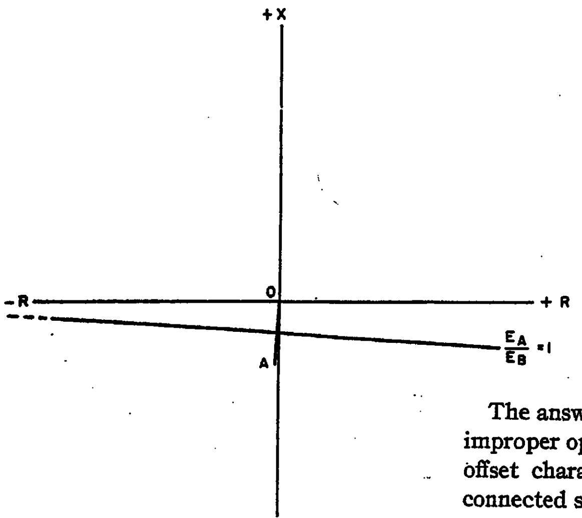


Figure 7. (left) Loss-of-synchronism characteristic for case of an infinite system

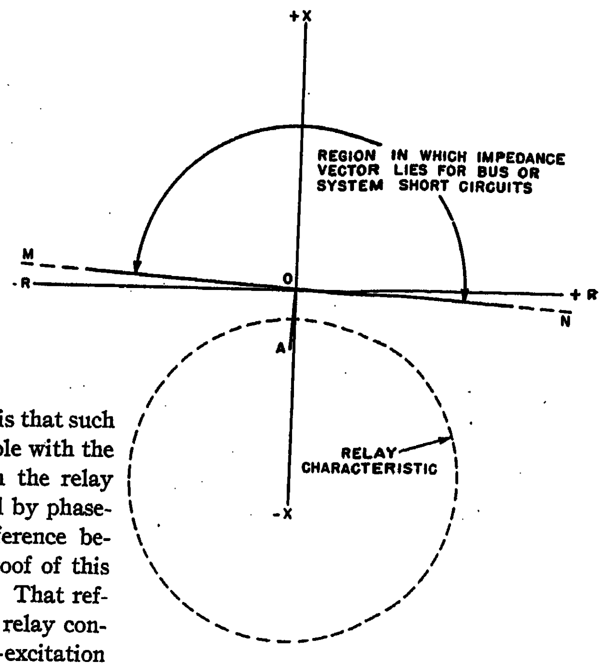

+X
REGION IN WHICH IMPEDANCE VECTOR LIES FOR BUS OR SYSTEM SHORT CIRCUITS
M
-R
O
+R
N
A
RELAY CHARACTERISTIC
-X

Figure 8. (right) External fault impedance region

to be infinitely large, relative to the generator in question.

It will be observed that the system impedance *OB* of Figure 6 has been reduced to zero in Figure 7. The loss-of-synchronism characteristic for $E_A/E_B=1$ is still the perpendicular bisector of the total impedance, which now is reduced to the generator impedance *AO*. Since such a condition of excitation is not at all unusual, it is reasonable to conclude that in the general case the operating area of the relay should lie no closer to *O* than one half of *OA* (or approximately one half of the direct-axis transient reactance of the generator). However, it will be evident that one need not assume an infinite system to see that the operating area of the relay in general should be somewhat below the *R* axis if operation during severe power swings or loss of synchronism is to be avoided.

Thus, while it will be evident that the relay characteristic need not be offset from the origin in those cases where the generator is large with respect to the rest of the system (*AO* is small with respect to *OB*), it has been shown that for general application the offset is necessary. But principally, it has been shown that the proposed shape and location of the relay characteristic is such that the greatest possible selectivity is provided against improper operation during power swings or loss of synchronism.

Effect of System Short Circuits on the Loss-of-Excitation Relay

The question will naturally arise whether any kind of short circuit, including unbalance faults, on the generator bus or in any part of the connected system will cause the loss-of-excitation relay to operate, particularly in view of the fact that a single-phase relay is proposed.

The answer to this question is that such improper operation is impossible with the offset characteristic, and with the relay connected so as to be operated by phase-to-phase voltage and the difference between the phase currents. Proof of this will be found in reference 7. That reference paper proves that for a relay connected the same as the loss-of-excitation relay, the region in which the impedance vector will lie for any kind of fault involving any combination of phases is substantially that indicated on Figure 8 for nonarcing short circuits. The line *MN* is perpendicular to the generator impedance *AO*. For an arcing fault on the generator bus, the impedance can lie slightly below *MN*, but it will be well outside of the dotted relay characteristic.

If the relay were not connected as it is, or if its characteristic were not offset, it could operate for a bus fault, which is another reason for offsetting the characteristic and limiting its size.

The relay can operate for a fault in its generator, but this is of no consequence since the generator would be removed from service anyway by other protective relays.

Effect of Line-Charging Current on the Loss-of-Excitation Relay

The value of *OD* in the relay characteristic of Figure 2 was said to be approximately equal to the direct-axis synchronous reactance of the generator. Actually, under extreme line-charging conditions, this value should be the quadrature-axis synchronous reactance in order to avoid relay operation. If the reduction had to be much more, the line-charging capacity of the generator would be exceeded. It is

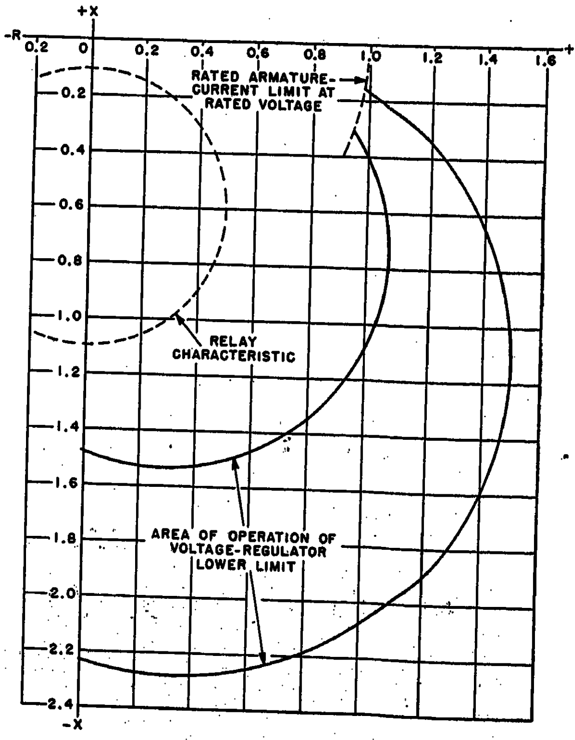


Figure 9. Area of operation of voltage-regulator low limit

possible, however, that in rare cases time delay might have to be inserted to prevent relay operation on transients.

In any case, the adjustment provided in the relay can be used to avoid operation during line charging, and the amount of adjustment necessary will not jeopardize the operation under loss-of-excitation conditions.

Effect of Partial Loss of Excitation on the Loss-of-Excitation Relay

The statement has been made that "field protective relays should be sensitive to any reduction of excitation that will, at any generator load, become unsatisfactory for continued operation of the machine."[4] The question will arise whether the loss-of-excitation relay will fulfill this broad requirement.

Reference 2 establishes suggested lower limits of excitation for normal operation for a turbogenerator which, when converted to the *R-X* diagram, are shown in Figure 9, together with the loss-of-excitation-relay characteristic for the generator involved. It is immediately evident that excitation can be reduced below the suggested limit without benefit of protection by the relay if the proposed relay adjustments are used.

The important point to remember is that the choice of the adjustment of the relay was made on the basis of preventing improper operation under certain conditions. It is possible that, for some applications, the diameter of the circle and its offset could be adjusted to provide protection against partial loss of excitation without the risk of improper operation under any of the other conditions.

A second point to consider is whether immediate tripping of a generator is desirable under such conditions. While there may be some question whether an operator could act quickly enough in the case of complete loss of excitation, it is much more likely that there would be time to act, and possibly to avoid the necessity for tripping the generator when partial loss of excitation occurred. It would seem reasonable to conclude, therefore, that protection against partial loss of excitation might better be provided by a separate relay arranged to sound an alarm.

One type of partial loss of excitation that would require immediate action is complete short circuiting of part of the generator-field poles. This may cause serious vibration. Protection against such trouble is not considered to be the proper responsibility of a loss-of-excitation relay. Relaying means are available for detecting grounds in field windings, which can lead to short-circuited poles. Vibration-detecting means also are available. The steps that must be taken when poles are short circuited are different from those when the entire source of excitation is affected. Consequently, other relaying means should be employed which can differentiate between this type of trouble and any other.

The Reactive-Current Relay

It is interesting to examine the characteristic of the reactive-current relay of reference 3 on the basis of the *R-X* diagram. This relay is a directional type having maximum torque at 90 degree lead. Ordinarily, the operating characteristic of such a relay on the *R-X* diagram would be an infinite number of circles, all passing through the origin and all centered on the *X*-axis, the diameter of each circle being directly proportional to the square of the applied voltage. Through the use of a voltage regulator which maintains substantially constant voltage at the relay over a wide range of supply-voltage variation, a single circular characteristic through the origin is obtained. Adjustment of its diameter is made by means of a tapped coil.

The adjustment chosen for the relay was such that the diameter of its characteristic circle is between 2.0 and 3.0 per-unit ohms. This provides good protection against partial loss of excitation, which is one of the stated objects of the originators of the relay.

Conclusions

While the principal purpose of this paper was to describe a new loss-of-excitation relay, much space has been allotted to other related considerations. The value of the *R-X* diagram as a powerful tool for the analysis of problems of this nature will be particularly evident. The following conclusions may be drawn:

1. The new loss-of-excitation relay will operate before the angular displacement of a generator with respect to its system reaches 180 degrees for any case of complete loss of excitation.

2. The relay will provide greater selectivity than has heretofore been available.

3. Protection against partial loss of excitation can be obtained, but possibly at the expense of selectivity; if such protection is required, it could, and perhaps should, be supplementary.

In conclusion, it may be well to close with what some may consider an obvious reminder, namely, that the best protection against loss of excitation is to try to prevent it. Having to trip a generator is apt to be embarrassing at any time, and it is especially aggravating when made necessary by failure of an auxiliary service. Should more than one generator be affected, the consequences might be far worse. The fact that reliable protective relaying is available should not overshadow the primary importance of a thoroughly reliable excitation system and of personnel well versed in its care and use.

Appendix I. Determination of *R* and *X* for Constructing *R-X* Diagrams for Balanced 3-Phase Conditions

$$R=\frac{V^2W}{VAR^2+W^2}$$

$$X=\frac{V^2VAR}{VAR^2+W^2}$$

where all quantities are actual or per-unit values, and where

R = line-to-neutral (positive-phase-sequence) resistance

X = line-to-neutral (positive-phase-sequence reactance)

V = phase-to-phase voltage

W = 3-phase power (supplied by generator to system)

VAR = 3-phase reactive power (supplied by generator to system)

References

1. Generator Stability at Low Excitation, E. L. Michelson, L. F. Lischer. *AIEE Transactions*, volume 67, part I, 1948, pages 1–9.

2. Underexcited Operation of Turbogenerators, C. G. Adams, J. B. McClure. *AIEE Transactions*, volume 67, part 1, 1948, pages 521–28.

3. Loss-of-Field Protection for Generators, G. C. Crossman, H. F. Lindemuth, R. L. Webb. *Electrical Engineering* (*AIEE Transactions*), volume 61, May 1942, pages 261–66.

4. The Mho Distance Relay, R. M. Hutchinson. *Electrical Engineering* (*AIEE Transactions*), volume 65, June 1946, pages 353–59.

5. Resynchronizing of Generators, C. Concordia, M. Temoshok. *AIEE Transactions*, volume 66, 1947, pages 1512–18.

6. Impedances Seen by Relays During Power Swings With and Without Faults, Edith Clarke. *AIEE Transactions*, volume 64, 1945, pages 372–84.

7. Graphical Method for Estimating the Performance of Distance Relays During Faults and Power Swings, A. R. van C. Warrington. *AIEE Transactions*, volume 68, part I, 1949, pages 608–21.

No Discussion

One Slip Cycle Out-of-Step Relay Equipment

W. C. MORRIS
ASSOCIATE AIEE

WHEN the out-of-step condition develops on a power system, the resulting disturbance may result in a major loss of load. It is, therefore, desirable to be able to recognize such a condition promptly and to take corrective measures to minimize the disturbance. This paper describes a new relay equipment which is capable of recognizing an out-of-step condition before the completion of the first slip cycle, whereas earlier forms of out-of-step relays did not usually give an indication until after the third slip cycle.

Such a relay may be used in a number of different ways:

1. The separation of a system into two or more sections at preselected points so that each section may have sufficient generation to meet its own load requirement.[1]
2. To disconnect a machine which is out-of-step from the system in order to avoid damage to the machine.[2,3]
3. To correct an out-of-step condition in the case of a generator by controlling the input to the prime mover so as to either slow down or speed up the particular machine.[4]

This new relay equipment in addition to recognizing an out-of-step condition more quickly than previous relays, has the further feature that it is capable of determining the relative direction of slip, thus permitting application for case 3 above.

One previous type of out-of-step relay uses three reversals of power as an indication of an out-of-step condition. An overcurrent relay is used as a fault detector so that it is necessary to have higher than normal current and also a power reversal before the reversal is counted. When a fault occurs the current is high and the power may reverse. Then while the fault is still on, one circuit breaker may open and change the conditions so that the power reverses again. Since it is possible to have two power reversals accompanied by high current during a fault, it is necessary to block the relay from operating on the first two power reversals. As there is only one power reversal accompanied by high current each slip cycle, this relay cannot trip on out-of-step until the third slip cycle.

Other previous out-of-step relays use voltage dips or current surges to indicate the out-of-step condition. These types of out-of-step relays also have to be blocked for the first two voltage dips or current surges to prevent them from tripping incorrectly during faults not involving an out-of-step condition.

A new relay scheme that will trip correctly for an out-of-step condition during the first slip cycle and will not trip incorrectly during a fault is based on establishing three areas as shown in Figure 1. During an out-of-step condition the impedance seen by the relays located at O will always pass through each of the three areas in sequence and will be in each area for a time determined by the rate of slip. The out-of-step condition is recognized by the relay equipment through a chain of auxiliary relays which must be picked up in proper sequence and with a certain minimum time required for each step.

By establishing the three areas it is possible for this relay scheme to determine the relative direction of slip. If the slip is in one direction the impedance vector will go through area number 1, then area number 2, and finally area number 3. If the slip is in the opposite direction it will go through the areas in the sequence, area number 3, area number 2, and then area number 1. How these two sequences can be used to set up different circuits is described later.

The relay equipment is composed of two relays. One relay, known as the angle impedance relay, Figure 3, is composed of two induction cup units, one with characteristic AA, Figure 1, and the other with characteristic BB.

These units are similar to standard reactance units, differing only in that their characteristics have been shifted so they are parallel to the system impedance. These same units have been used as blinders for long line applications.[5]

Each unit has two contacts. The A unit has a contact A_1 which is closed for all impedances to the left of characteristic AA and a contact A_2 that is closed for all impedances to the right of characteristic AA. The B unit has a contact B_1 which is closed for all impedances to the left of characteristic BB and a contact B_2 that is closed for all impedances to the right of characteristic BB.

The other relay, known as the "overcurrent and time-delay relay," is composed of one plunger-type overcurrent unit and six telephone-type time-delay auxiliary relays. The overcurrent relay characteristic is shown as a circle in Figure 1 although it is really a family of circles depending on the voltage, because it has a fixed pickup in amperes.

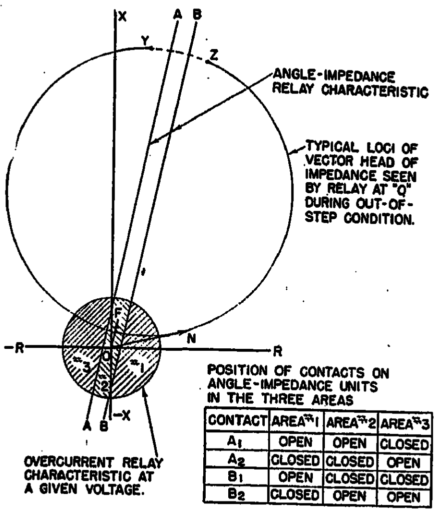
The telephone-type auxiliary relays are used to set up the trip circuit or control circuits when they are picked up in the proper sequence.

How the Two Relays Operate

The contact circuits of the two angle impedance units and the contact and coil circuit of the overcurrent and telephone-type relays are shown in Figure 2. Contacts on the angle impedance units which are closed in each area is shown in Figure 1.

Paper 49-261, recommended by the AIEE Committee on Relays and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 19, 1949; made available for printing September 7, 1949.

W. C. MORRIS is with the General Electric Company, Philadelphia, Pa.


X
A
B
Y
Z
ANGLE-IMPEDANCE RELAY CHARACTERISTIC
TYPICAL LOCI OF VECTOR HEAD OF IMPEDANCE SEEN BY RELAY AT "O" DURING OUT-OF-STEP CONDITION.
F
N
-R
R
O
#3
#2
#1
A
B
-X
OVERCURRENT RELAY CHARACTERISTIC AT A GIVEN VOLTAGE.
POSITION OF CONTACTS ON ANGLE-IMPEDANCE UNITS IN THE THREE AREAS

CONTACT	AREA #1	AREA #2	AREA #3
A_1	OPEN	OPEN	CLOSED
A_2	CLOSED	CLOSED	OPEN
B_1	OPEN	CLOSED	CLOSED
B_2	CLOSED	OPEN	OPEN

Figure 1. Impedance characteristic of out-of-step relay scheme

Under normal conditions the impedance seen by the angle impedance units at location O will be to the right of characteristic BB but outside the overcurrent relay characteristic. When an out-of-step condition appears the impedance vector will move to the left. When it crosses into area number 1, the overcurrent unit will close its contacts. As contact B_2 is closed in this area the telephone-type relay X_1 will be energized and will pick-up providing contact B_2 stays closed for 0.005 second after the contacts of the overcurrent unit closes. When the impedance vector crosses into area number 2, contact B_2 will open and de-energize coil X_1 and contact B_1 will close. As contact A_2 is closed in area number 2 the coil of X_2 will be energized providing X_1 has had time to pick-up while the vector was crossing area number 1. When X_2 picks up it seals in around the contact of X_1 in its coil circuit. When the impedance vector crosses into area number 3, contact A_2 opens and de-energizes X_2 and contact A_1 closes and energizes X_4. The contact of X_4 energizes X_3 providing X_2 had time to pick-up while the impedance vector was crossing area number 2. When X_3 picks up it closes its contacts (studs 2-12) and also seals in around the contact of X_2. When the impedance vector passes out of area number 3 by crossing the overcurrent units characteristic, the contacts of the overcurrent unit will open and de-energize X_3. However X_3 has an adjustable time delay drop-out from 0.5 to 3 seconds. Therefore if the time of one slip cycle is less than the drop-out time setting of X_3, then X_3 will be re-energized before it can drop-out and will remain picked up. If the time of one slip cycle is more than the drop-out time setting of X_3, then X_3 will drop-out after the time delay has expired, but will be picked up again on the next slip cycle. The above cycle was based on one part of the system having fallen behind the other. If this same part of the system starts to go out-of-step by pulling ahead of the other, the impedance vector will go through the three areas in the sequence of area number 3, area number 2, and finally area number 1. This reversed sequence will cause relays X_4, X_5, X_1, and X_6 to operate and the contacts of X_6 (studs 1 and 11) will close.

When a fault occurs the impedance vector changes instantly from some value to the right of characteristic BB and outside the overcurrent characteristic, such as ON, to a new value in area number 2 such as OF. As this change is made instantly neither auxiliary X_1 nor X_4 have a chance to pick-up before the contacts of A_1 and B_2 are both open.

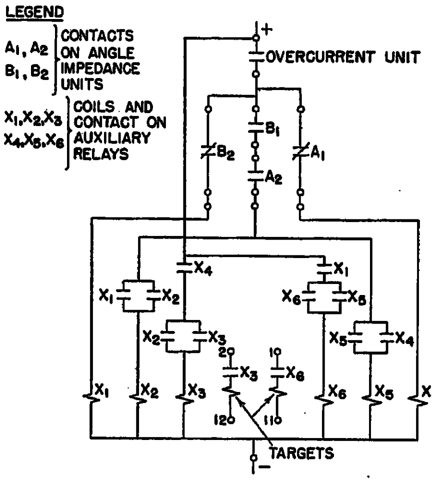

LEGEND
A1, A2 B1, B2 CONTACTS ON ANGLE IMPEDANCE UNITS
X1,X2,X3 X4,X5,X6 COILS AND CONTACT ON AUXILIARY RELAYS
+
OVERCURRENT UNIT
B1
B2
A1
A2
X4
X1
X1
X2
X6
X5
X2
X3
X5
X4
20
10
X3
X6
X1
X2
X3
X6
X5
X4
120
110
TARGETS
-

Figure 2. Control circuit of out-of-step relay scheme

The purpose of the overcurrent unit is to prevent operation on normal oscillations such as caused by the generator hunting. The loci of the vector heads will oscillate back and forth along some line such as YZ, Figure 1. Even though the impedance vector crosses the AA and BB characteristics in the proper sequence, it cannot cause the auxiliary relays to set up the trip or control circuits because this is blocked by the contacts of the overcurrent unit remaining open.

The distance between the two impedance angle relay characteristics BB and AA determines the maximum slip cycles per second at which the relay scheme will operate. The maximum slip cycles per second is determined by the pick-up time of the time delay relays. These relays will pick-up in approximately 0.005 second.

If the impedance vector crosses between BB and AA in less than 0.005 second, the auxiliary relays will not pick up and the relay scheme will not operate. The time required for the impedance vector to cross between the BB and AA characteristic is related to the number of slip cycles per second and the number of electrical degrees between the characteristic by the following equation

$$t=\frac{1}{S}\times\frac{\theta}{360} \tag{1}$$

Where S is the number of slip cycles per second, and θ is the change in the angle of the impedance vector, as seen by the relay, as the vector crosses between the two characteristics. By substituting the pick-up time of the time delay relays (0.005) in this equation, the maximum slip cycles ($S_{\max}$) can be found

$$S_{max}=\frac{\theta}{1.8}\text{ cycles per second} \tag{2}$$

It is necessary to change θ from electrical degrees to relay ohms in order to set the relay. In Figure 4, line R-S is the impedance of the total circuit and point O is the electrical center. β is the angle

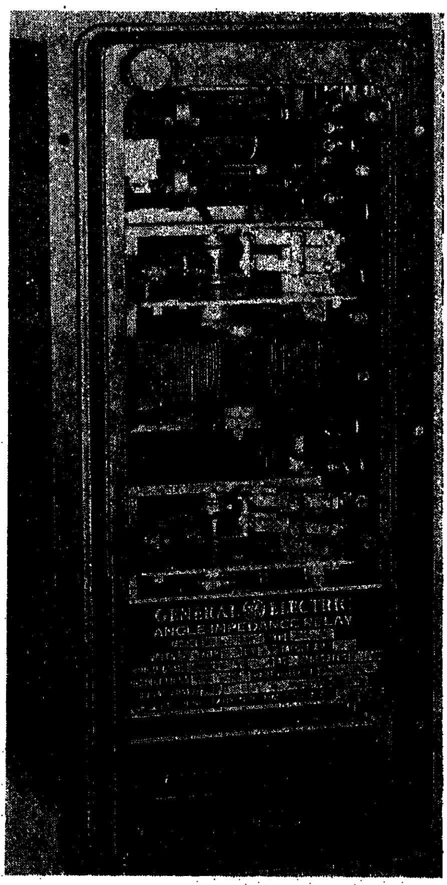


Figure 3. Angle-impedance relay

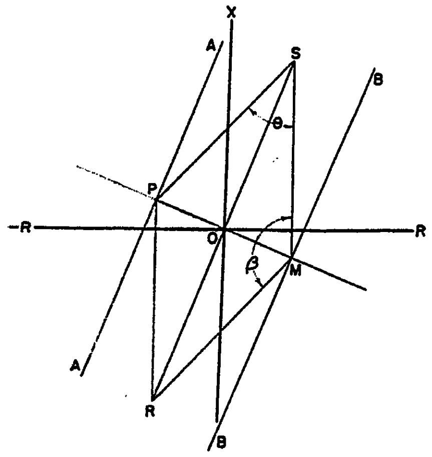


Figure 4. Relation of angle-impedance relay characteristics to impedance seen by relay

between the two ends of the system. The ohm setting of the *B* angle impedance unit is *OM* and ohm setting of the *A* angle impedance unit is *OP*. As both units are set the same $OM = OP$, and $OR = OM \tan \beta/2$. From this equation and equations 1 and 2, it is possible to obtain the curve shown in Figure 5. This shows the maximum slip cycles per second at which the relay scheme will work. By knowing the total ohms of the system and the maximum slip cycles at which the relay must operate, it is possible to determine the relay setting. The relay is adjustable from 0.25 to 2.5 ohms phase-to-neutral in 1 per cent steps.

Limitations

When a generator loses excitation, it will lose synchronism but the impedance values as seen by a relay at the generator terminals do not follow the typical loci of Figure 1. Hence, this out-of-step relay may not operate in the case of loss-of-excitation. To protect the generator against out-of-step due to loss-of-excitation it is necessary to use a different relay known as a loss-of-excitation relay.[6]

This relay scheme will not operate while a fault is on the phases associated with the relay because the impedance vector crosses only one of the two relay characteristics. However, this relay will operate on the first slip cycle after the fault is cleared.

Conclusions

A relay scheme now is available that will correctly detect an out-of-step condition during the first slip cycle. It also will indicate whether one part of a system in running ahead or behind the rest of the system and different control circuits can be used for the two conditions if it is desirable for any particular application.

References

1. Relay Operation During System Oscillations, C. R. Mason. *Electrical Engineering*, July 1937, pages 823–32.

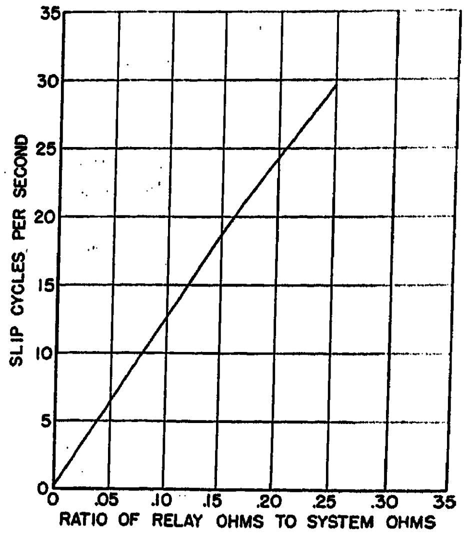


Figure 5. Curve showing maximum slip cycles at which relay will operate relative to relay setting

2. Power System Stability (book), S. B. Crary. John Wiley and Son, Inc., New York, N. Y., 1947, page 239.

3. A System Out of Step and Its Relay Requirements, L. N. Crichton. *Electrical Engineering*, October 1937, pages 1261–7.

4. Governor Performance During System Disturbance, R. C. Buell, R. J. Caughey, E. M. Hunter, V. M. Marquis. *AIEE Transactions*, volume 50, March 1931, pages 354–69.

5. Protective Relaying for Long Transmission Lines, A. R. van C. Warrington. *Electrical Engineering* (*AIEE Transactions*), volume 62, June 1943, pages 261–68.

6. A New Loss-of-Excitation Relay for Synchronous Generators, C. R. Mason. *AIEE Transactions*, volume 68, part II, 1949, pages 1240–45.

No Discussion

Novel Test Circuits for Protective Relays

W. K. SONNEMANN
MEMBER AIEE

IN THE test, calibration, checking, and setting of protective relays, and in the determination of their characteristic operating curves, it is of course necessary to apply to the relays the quantity or quantities which are expected to operate them. When the quantities are rather simple, such as a single variable current value, there is no special problem, particularly in the laboratory. When two or more variable a-c quantities are involved, the problem becomes more complex, particularly when the phase angle between these quantities must be either known or controlled. The solution to such problems may generally be obtained in the laboratory by special circuits and equipment. It is the purpose of this paper to discuss certain of these circuits and the capabilities of certain equipment which the author believes are not common knowledge. As a related item, methods of checking an over-all installation in the field by means of load current and voltage as delivered to the relay from the instrument transformers will be discussed for a particular case which involves sequence components not normally present under load conditions.

Versatility and Limitations of a Variable Ratio Autotransformer

The variable ratio autotransformer,* as a very convenient device for varying a voltage, has received considerable attention and, accordingly, it has enjoyed widespread acceptance. One of its capabilities, however, seems quite generally to have been overlooked; so much so, in fact, that it is not even mentioned in the literature of the suppliers. This is its possibilities as a variable ratio current transformer. The use of the device for this latter function makes it particularly convenient in the test of certain differential relays, as illustrated by the following typical examples.

Type *HCB* Pilot Wire Relay

The circuit of Figure 1 is shown as a typical example wherein a current, I_F, can be made any desired fraction of another current, I_N, and, at the same time, kept in phase with it. This is accomplished with only one load bank which represents another advantage, since, ordinarily, two loads are required to control two currents.

Figure 1 is purely schematic insofar as the pilot wire circuit is concerned, the principal point to the diagram being to illustrate the control of two currents relative to each other. The current circuit is complete, however, and it should be noted that the current I_N flowing in the near relay is given the reference polarity from *A* to *N* (phase *A* to neutral). In the far relay, the corresponding current, I_F, is shown flowing from *N* to *A*. This is the correct polarity to simulate an external phase-to-ground fault on phase *A*. Thus, a set-up is made whereby typical operating characteristic curves, as shown in Figure 2, may be obtained.

The terminals of the variable ratio autotransformer have been numbered for convenience in Figure 1. Terminals *1* and *2* are the normal input terminals, and *3* and *4* are the normal output terminals. Note that the total circuit current, I_N, enters at terminal *3*, which is associated with the moving contact, or brush. If this is adjusted to make contact with the same turn as terminal *1*, then the ratio between I_N and I_F will be substantially 1-to-1, and $I_F=I_N$, the far relay being merely paralleled by the magnetizing impedance of the autotransformer. This would correspond with a dial setting on the device of 100 per cent voltage. For the other extreme, consider the moving contact in the position normally associated with zero voltage output, wherein the moving contact makes contact with the start turn of the winding associated with terminals *2* and *4*. In this case, the current, I_N, passes through a short circuit between terminals *3* and *4*, and I_F must equal zero.

The extremes of $I_F=1\times I_N$ and $I_F=0\times I_N$ have thus been covered. At any intermediate position, such as actually illustrated in Figure 1, the total current, I_N, must divide into two component parts, I_P and I_S, wherein the subscripts *P* and *S* denote primary and secondary, and wherein I_S is identical with I_F. These currents must satisfy the balance of ampere turns between primary and secondary windings, just as in any transformer when magnetizing current is neglected. Thus, I_S times the number of turns between terminals *1* and *3* must be substantially equal to I_P times the number of turns between terminals *3* and *4*. Since the number of turns is adjustable by moving the dial or pointer of the device, a smooth control of I_F with respect to I_N is obtained between the limits of multi-

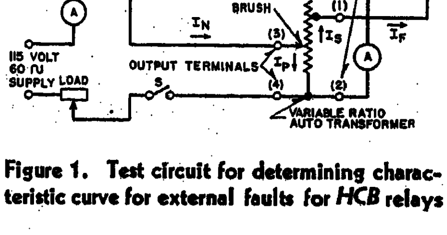


Figure 1. Test circuit for determining characteristic curve for external faults for *HCB* relays

---

* This device is marketed by at least three suppliers. The General Radio Company, Cambridge, Mass., supplies it under the trade name of Variac, and the Superior Electric Company, Bristol, Conn., supplies it under the trade name of Powerstat. The General Electric Company, Schenectady, N. Y., supplies it under the name of Variable Voltage Autotransformer. The author knows of no other manufacturer supplying similar equipment.

Paper 49-262, recommended by the AIEE Relays Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 11, 1949; made available for printing September 7, 1949.

W. K. Sonnemann is with the Westinghouse Electric Corporation, Newark, N. J.

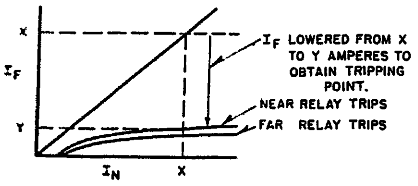


Figure 2. Typical characteristic curve obtained with test set-up of Figure 1

pliers of 1 and zero. Some of these devices have extended windings as illustrated so that, actually, a ratio of I_F to I_N of somewhat more than unity can be obtained.

As the brush contact is moved over the winding, it will short circuit a turn when passing from one turn to the next. If the contact is left in a position where one turn is short circuited, it is theoretically true that the phase-angle error of the device will be affected. However, one turn is so small a percentage of the total winding, and the circulating or short-circuit current in it is so small, that there is no material effect.

Switch *S* is shown to represent schematically a means of throwing the circuit on and off, although a contactor having a contact in each side of the supply line will generally be used. This is desirable because in running curves similar to Figure 2, the higher current values will greatly exceed the continuous current-carrying capacity of the relay and the autotransformer and it is desirable to leave the current on for only the time required to obtain an observation. In this connection, ammeters with an adjustment for presetting the pointer, as in ammeters for measuring inrush currents, are particularly convenient.

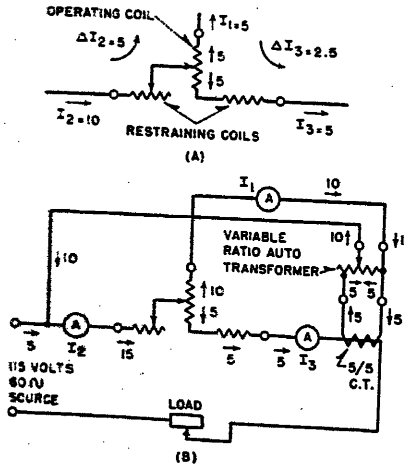


Figure 3. Test connections illustrating the use of the variable ratio autotransformer in step-up ratio

A. Schematic diagram of *CA* relay
B. Connections to develop $\Delta I_2 + I_1$ of (A)

In taking the curve of Figure 2, it is convenient to start with a point wherein the currents are equal, such as $I_N = I_F = X$. This checks a point on the true balance line, wherein neither relay should trip. With this preset value of $I_N = X$, the far relay current is then lowered until a tripping point is reached at $I_F = Y$, this being found by observing the relay action on successive short applications of current. The number of trials necessary will generally be small in number if, after a point or two is obtained, the shape of the curve is predicted and the adjustment for the expected value of $I_F = Y$ is made prior to throwing the current on.

In making any circuit set-up such as in Figure 1, the adequacy of the variable ratio autotransformer should be considered from the standpoint of both voltage and current rating. Considering voltage first, other tests on the type *HCB* relay, for example, have shown that the voltage required to pass up to 80 amperes, 60 cycles, through it from phase *A* to neutral requires from 20 to 40 volts, depending upon the relay taps in use. This is much less than the normal value of 115 volts which a device rated at 115 volts can develop as a counter voltage between the normal input terminals *1* and *2*. The voltage capacity of the device is therefore adequate for the test of type *HCB* relays as per Figure 1, and it can be assumed with safety that the magnetizing current drawn will not be high enough to introduce a material phase-angle error.

The second consideration of current rating should be viewed in the light of the momentary duty required for the test at hand. Information obtained from one manufacturer of these devices indicates that they should not be operated at momentary currents higher than five times the normal rating. One such device rated at 18 amperes continuous has been in use in the laboratory in circuits similar to Figure 1 and to as high a current as 80 amperes, and has been so used for a number of years without showing any signs of distress. Based on the supplier's recommendation of a five times figure, the 18-ampere device could have been used to 90 amperes, so that the actual testing left a margin of safety of 10 amperes. For current values higher than 80 amperes, the author uses a device rated at 40 amperes continuous.

Type *CA* Percentage Differential Relay

Figure 3 illustrates an application wherein the variable ratio autotransformer is used to step up the current which it controls. Figure 3(A) illustrates the basic problem in the test of a 50 per cent differential transformer type *CA* relay in use on the 5/10 auto-balance taps. Thus, the particular current values of $I_2 = 10$, $I_3 = 5$, and $I_1 = 5$ amperes represents a true balance for normal operation. To check the percentage characteristics on one side, the current, $\Delta I_2 = 5$ amperes, must be added to I_2, representing a 50 per cent increase in I_2. To check the percentage characteristics on the other side, the current, $\Delta I_3 = 2.5$ amperes, must be added to I_3, representing a 50 per cent increase in I_3. Either of these increments must be added into the original balanced condition, requiring the splitting of the current $I_2 = 10$ amperes into two equal parts. The solution to the first problem only, that of adding $\Delta I_2 = 5$ amperes to the initial condition, is illustrated in Figure 3 (B).

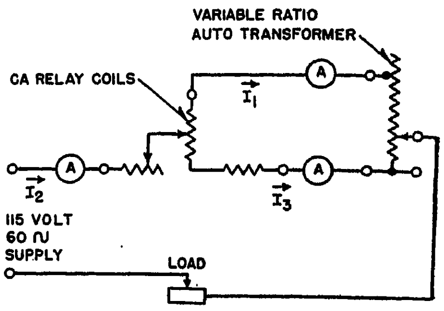


Figure 4. Circuit diagram for arrangement alternate to Figure 3(B)

In Figure 3(B), the 5/5 current transformer is used for insulating purposes, as a separate controllable current is thereby obtained for reintroduction into the relay circuit. In this case, the capacity of the autotransformer and the current transformer to develop the necessary voltage should not be overlooked. In general, the limits of satisfactory operation will be reached at much lower current values than when a step-down ratio is used, as in Figure 1.

An alternate method of accomplishing the same results as in Figure 3(B) is

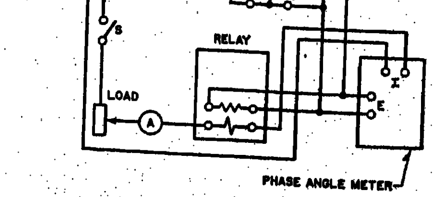


Figure 5. Circuit for determining the phase-angle characteristic of a relay in terms of current and voltage

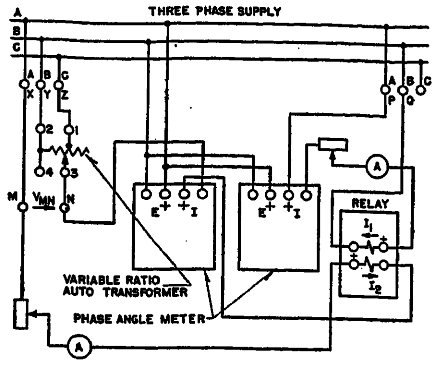


Figure 6. Circuit for the determination of the phase-angle curve of a relay operating on two currents

shown in Figure 4. It is simpler through the omission of the 5/5 current transformer, but it is more difficult to use for those readings when I_3 is the reference current. This is because an adjustment to raise the value of I_1 must necessarily decrease the value of I_3, thus requiring a readjustment of the load. On the other hand, the circuit is convenient when the relay is to be checked for its characteristic involving ΔI_3, Figure 3 (A), for I_2 is then the reference current and is not materially disturbed by an adjustment in the variable ratio autotransformer.

PHASE-ANGLE CURVES

The circuit of Figure 5 shows a set-up for determining the phase-angle characteristic of a relay in terms of the current leading or lagging a reference voltage. It is probably well known in the art, but a trouble spot not so well known is the possibility of distortion of the voltage wave, which, in some cases, leads to erroneous data. The variable ratio autotransformer is shown as a means to adjust the reference voltage to the desired value. Since it has an iron core, there are, of necessity, harmonics in its exciting current. These harmonic components must flow through the impedance of the phase shifter, thereby introducing a harmonic voltage drop in the phase shifter. It follows, therefore, that even with perfect sine wave supply at the input terminals of the phase shifter, the output voltage used at terminals *1* and *2* of the variable ratio autotransformer must contain harmonics, and these will correspondingly show up at the same percentage value at terminals *3* and *4* of the autotransformer. These harmonics affect the instrument indications and may affect the operation of the relay, leading to erroneous data.

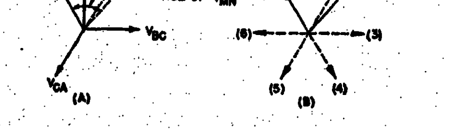


Figure 7. Vector diagrams for Figure 6

The problem of harmonics just discussed occurs in varying degrees. Some phase shifters have more internal impedance than others, and the exciting current of the variable ratio autotransformers depends upon size and rating. In many cases, provided the phase shifter is adequate, the harmonics may be reduced to a negligible factor by using a 230-volt variable ratio autotransformer with a phase shifter having nominal voltage output of 115 volts. By this means, the exciting current is greatly reduced through working the iron core at one-half its normal flux density. In any case, it is desirable to check the wave form of the voltage with an oscilloscope to see if the problem exists. In the more difficult cases, it becomes necessary to replace the variable ratio autotransformer with the old-fashioned potentiometer.

PHASE-ANGLE CURVES BETWEEN TWO CURRENTS

If it is desired to take a phase-angle curve on a relay which operates on two currents, the usual voltage phase shifter, as shown in Figure 5, is generally inadequate because of its limited current rating. The variable ratio autotransformer comes to the rescue through providing a means of getting a current at any desired phase angle from a 3-phase source, as shown in Figure 6. If of adequate rating, say 18 or 20 amperes continuous, its ability to furnish much larger currents momentarily makes it possible to take data to the higher values of current usually encountered in relay work.

Two phase-angle meters are shown in Figure 6 because the author knows of no commercially available instrument which will measure the angle between two currents directly. It is necessary, therefore, to obtain the value of the phase angle between the two currents by taking the difference between their phase angles read with respect to a common reference voltage. An alternate method to the use of two phase-angle meters as indicated is to use a single instrument in connection with a switching arrangement so that the phase angles of the two currents may be read one at a time.

As shown connected in Figure 6, the variable ratio autotransformer acts as a voltage divider on phase *BC*. As the brush contact is moved over the winding, the potential of the brush contact, corresponding to output terminal *3* and reference terminal *N*, can be made to assume any potential on a straight line between phase terminals *B* and *C*, or even beyond, with the extended winding as shown. Thus, the voltage, V_{23}, between terminals *2* and *3* is some function of V_{BC}, and may be expressed as $V_{23} = FV_{BC}$. The circuit voltage, V_{MN}, between terminals *M* and *N* used to supply the relay current, I_2, may be determined by inspection of the circuit as follows

$$V_{MN} = V_{AB} + V_{23}$$
$$= V_{AB} + FV_{BC}$$

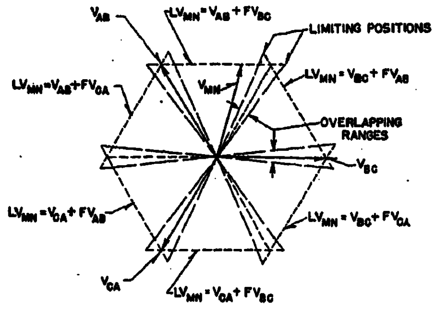

V_{AB}
$LV_{MN} = V_{AB} + FV_{BC}$
LIMITING POSITIONS
$LV_{MN} = V_{BC} + FV_{AB}$
$LV_{MN} = V_{AB} + FV_{CA}$
V_{MN}
OVERLAPPING RANGES
V_{BC}
$LV_{MN} = V_{CA} + FV_{AB}$
$LV_{MN} = V_{BC} + FV_{CA}$
V_{CA}
$LV_{MN} = V_{CA} + FV_{BC}$

Figure 8. Vector diagram showing control of V_{MN} by separately interchanging *X, Y, Z* leads, and *M, N* terminals of Figure 6

Figure 7 shows the vector diagrams for the circuit of Figure 6. The voltages V_{AB}, V_{BC}, and V_{CA} are the three phase-to-phase voltages of the supply. The locus or range of adjustment of V_{MN} is shown in Figure 7(A), wherein it is shown that a range of slightly more than 60 degrees may be obtained by using the extended winding of the autotransformer. The corresponding range of I_2 is shown in Figure 7(B), assuming substantially unity power factor for the current circuits.

Figure 7(B) also shows six different possible positions for the reference vector, I_1. As shown solid for position (1), it is drawn for the connections as shown in Figure 6, where lead *P* is connected to terminal *A* and lead *Q* is connected to terminal *B*. There being two circuit leads *P* and *Q*, and three supply terminals *A*, *B*, and *C*, there are, of course, six combinations possible, remembering that *P* and *Q* may be interchanged when re-

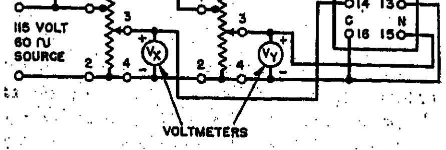


Figure 9. Test diagram for *HVS* relay

quired. There being six different possible phase positions for the reference vector, I_1, and these positions being 60 degrees apart, and there being a range of adjustment of I_2 of slightly more than 60 degrees, it becomes apparent that readings may be taken at any desired phase angle between I_1 and I_2 "around the clock," from 0 to 360 degrees.

If desired, the same effect can be obtained by leaving leads *P* and *Q* connected as shown, and altering the connections with leads *X*, *Y*, and *Z*, and interchanging the connections to terminals *M* and *N* as required. A vector diagram showing the control thus obtained on V_{MN} is shown in Figure 8 wherein LV_{MN} indicates the locus of the extremity of the V_{MN} vector. In this diagram the range of V_{MN} is extended both ways from the central 60-degree sector, remembering that the connections to terminals *1* and *2* of the autotransformer may be reversed, thus placing the extended winding "on the other end." There are six sectors indicated, and to obtain any one of them, the connections to be used are determined from a consideration of the subscripts appearing in the equation given for LV_{MN} for that sector.

The vector diagram of Figure 8 also suggests that, in the event a voltage phase shifter of the usual type is not available, tests may be made on any relay using voltage and current at any desired phase angle by using terminals *M* and *N* of Figure 6 to energize the voltage coil of the relay. This requires reconnecting and using only one phase-angle meter, of course, and will require a separate device for control of the magnitude of the voltage to be applied to the relay potential coil.

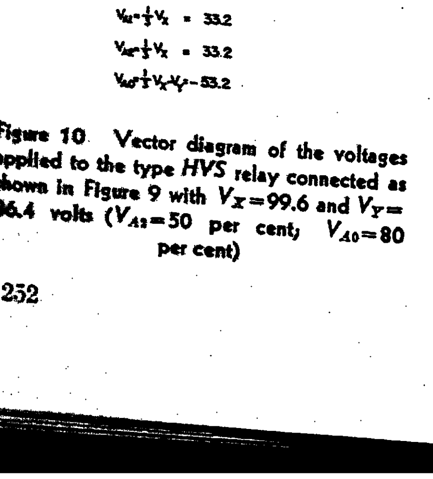


Figure 10. Vector diagram of the voltages applied to the type *HVS* relay connected as shown in Figure 9 with $V_X=99.6$ and $V_Y=86.4$ volts ($V_{A2}=50$ per cent; $V_{A0}=80$ per cent)

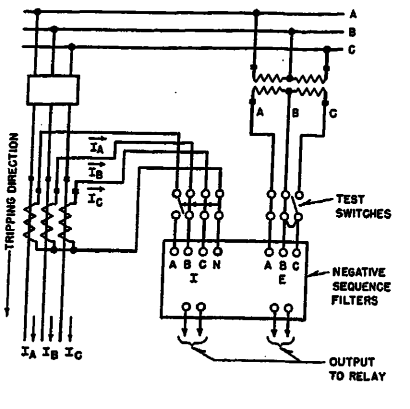


Figure 11. Abbreviated diagram of negative sequence filter and relay connections showing operation of test switches for checking purposes

Type *HVS* Relay

Appropriate methods and circuits for testing relays which operate on sequence components are rather generally "not in the book," except as brought out in instruction leaflets. An interesting case where sequence components are made to order for test purposes is illustrated by the type *HVS* relay. This relay, a single phase-to-ground fault-detecting relay, operates in response to a restraining force from negative sequence voltage, and an operating force from zero sequence voltage, neither of which are present under normal system conditions. Furthermore, the relay has only four voltage terminals, for phases *A*, *B*, *C*, and neutral terminal *N*, so that, somehow, separately controllable negative sequence and zero sequence voltages must be applied to these four terminals if the relay is to be calibrated. Figure 9 shows how this may be done with a single-phase supply.

It is apparent that on a normal 3-phase system, if a solid short circuit occurs on two terminals, such as *B* and *C*, that, at the point of fault, one of the three phase-to-phase voltages disappears, and the other two collapse. The end result is that, to all practical purposes, there is at the point of fault one single-phase voltage between terminal *A* and short-circuited terminals *B-C*. Note that in Figure 9, relay terminals *B* and *C* have been short-circuited together, and a single-phase voltage, V_X, applied between terminal *A* and short-circuited terminals *B-C*, thus simulating a solid phase-to-phase fault. This condition furnishes the required negative sequence voltage to the relay accompanied by positive sequence voltage to which latter voltage the relay does not respond. It is independent of the potential of the neutral terminal, *N*, which is proved by the following analysis in terms of phase-to-neutral quantities.

By inspection of the circuit, it is determined that, insofar as the relay terminals are concerned

$$V_{AN}=V_X-V_Y \tag{1}$$

$$V_{BN}=-V_Y \tag{2}$$

$$V_{CN}=-V_Y \tag{3}$$

These equations are written with the reference polarity given by the + and − marks on the diagram. The negative sequence component of these phase-to-neutral voltages is determined in the usual way.

$$\begin{aligned} V_{A2} &= {}^1/_3[V_{AN}+a^2V_{BN}+aV_{CN}] \\ &= {}^1/_3[V_X-V_Y+a^2(-V_Y)+a(-V_Y)] \\ &= {}^1/_3[V_X-V_Y(1+a+a^2)] \end{aligned}$$

$$V_{A2}={}^1/_3V_X \tag{4}$$

Equation 4 shows that the desired value of negative sequence voltage can be obtained by setting the voltage, V_X, to a value of $3V_{A2}$.

The zero sequence voltage is determined by adding equations 1, 2, and 3, from which

$$3V_{A0}=V_X-3V_Y$$

$$V_{A0}={}^1/_3V_X-V_Y \tag{5}$$

Equation 5 shows that if V_Y is made zero, V_{A0}, being then equal to $1/3V_X$, also is equal to V_{A2}, and a top limit is reached for a positive value of V_{A0}, using the circuit shown. If V_Y is made equal to $1/3V_X$, then $V_{A0}=0$ and the neutral is not displaced with respect to the collapsed voltage triangle. If V_Y now is further increased, equation 5 gives a negative value for V_{A0}, which simply means that V_{A0} and V_{A2} will be 180 degrees out of phase. The 180 degree phase relationship is of no consequence for this particular relay, and does not thereby spoil the desired end result of being able to make V_{A0} greater than V_{A2}.

Numerical values are given in Figure 10 for a particular calibrating point, together with appropriate vector diagram.

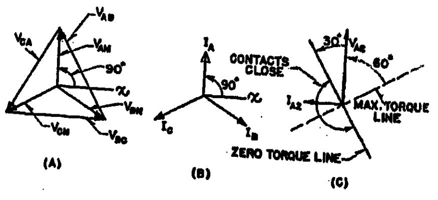


Figure 12. Reference vectors for Figure 11

A. Reference 3-phase voltage of the bus
B. Current vectors for normal balanced 3-phase load
C. Assumed phase-angle characteristics of the negative sequence relay

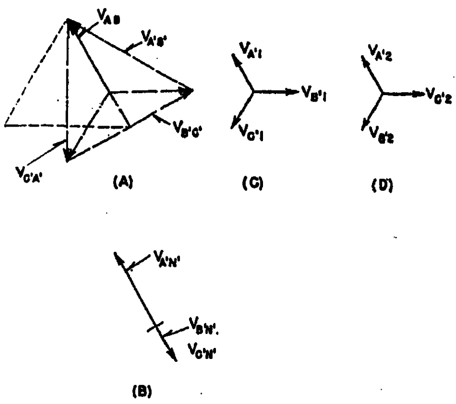


Figure 13. Vector analysis of the voltage conditions of Figure 11

FIELD TESTING WITH LOAD CURRENT AND VOLTAGE

The last test circuit discussed covered means of establishing negative and zero sequence components of voltage in what is essentially a laboratory setup. Going a step beyond this, it is certainly desirable to have some means of checking the overall installation of a relay of the sequence type in order to make sure that the relay and all its external connections are correct. When the relay ignores positive sequence, an illustrative example being the type *CRS* negative sequence directional relay, the fact that it has no response to normal load is inconclusive. Temporary alterations in the external connections can be made, however, as shown in Figure 11, so that sequence components artificially produced from the load currents and voltages are presented to the relay. Since these are derived from the primary quantities, the external connections are thereby checked. In making a test as illustrated in Figure 11, it is necessary to have available instruments not shown in the diagram to determine the magnitude and relative phase angle of the load current flowing at the time of the test. These instruments may be switchboard instruments already installed and connected to the same or other instrument transformers if properly located in the primary circuit. The important thing is that the phase angle, and preferably also the magnitude, of the current I_A, I_B, and I_C flowing in the line must be known, as will be shown from the following diagrams.

Figure 12 gives the basic information required for a starting point. The voltage vector diagram, Figure 12(A), showing phase-to-neutral and phase-to-phase voltages is taken as the reference diagram for phase position. The current vector diagram, Figure 12(B), shows the line current vectors in their proper position with respect to the given voltages for assumed balanced unity power factor load current flowing away from the bus and out over the line in question. This diagram is drawn separately from the voltage diagram in order to keep from having too many vectors in the same picture. It is tied in with the voltage vector diagram, however, through the 90-degree angle which I_A and V_{AN} each make with the X-axis as shown. Figure 12(C) shows the inherent phase-angle characteristics of the relay, wherein V_{A2} and I_{A2} are the negative sequence voltage, in terms of phase-to-neutral quantities, and the negative sequence current applied to the input terminals of the filter. This picture shows the zero torque line leading the voltage by 30 degrees, a typical characteristic. Note that the 180-degree zone in which the current may lie and close the relay contacts is substantially on the out-of-phase side. This makes the characteristic opposite in nature to the usual directional element characteristic. This is necessary because negative sequence power flows away from the fault rather than toward it.

Figure 13(A) shows the voltages involved with the temporary connections of Figure 11. The dotted voltage triangle comes from Figure 12(A), but only V_{AB} is in use. Since terminals *B* and *C* are shorted together, this voltage also is V_{AC}, or $-V_{CA}$ insofar as the filter is concerned. The identity of $V_{AB}=V_{AC}$ corresponds to the collapse of the dashed line triangle $V_{A'B'}$, $V_{B'C'}$, $V_{C'A'}$ which would occur with a solid phase-B′ to phase-C′ fault on such a system. The phase-to-neutral voltages which correspond to this collapse are shown in Figure 13(B) wherein $V_{A'N'}={}^2/_3V_{AB}$ and $V_{B'N'}=V_{C'N'}={}^1/_3(-V_{AB})$. These voltages are analyzed into positive and negative sequence components as shown in Figure 13(C) and (D), wherein $V_{A'2}={}^1/_2V_{A'N'}={}^1/_3V_{AB}$. The apparent negative sequence voltage presented to the filter now is determined, and as soon as the apparent negative sequence current is determined, operation of the relay can be predicted.

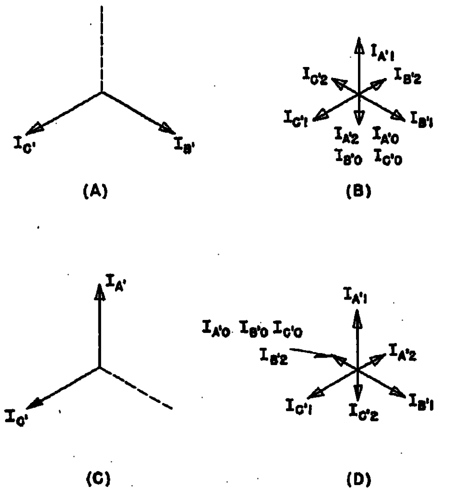


Figure 14. Vector analysis of currents applied to filter

A. Load currents, I_B and I_C, applied
B. Sequence components for (A)
C. Load currents, I_A and I_C, applied
D. Sequence components for (C)

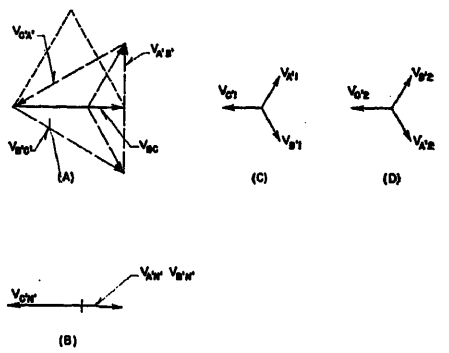


Figure 15. Vector analysis of voltage V_{BC} applied from terminals *A*, *B* to terminal *C*

The currents, $I_{B'}$ and $I_{C'}$ of Figure 14(A), which were taken from Figure 12(B), are the only two currents flowing into the current filter, and must come out of the box at the neutral terminal. Insofar as the filter is concerned, it appears that the system is experiencing a two phase-to-ground fault. An analysis by means of symmetrical components resolves the currents into the sequence components as shown in Figure 14(B). It will be found that $I_{A'2}$ occupies a position 180 degrees out of phase with the actual current I_A, and is $^1/_3$ of the magnitude of I_A. The vectors for the sequence components have been drawn accordingly.

All the vector diagrams were drawn with respect to the *X*-axis as a reference. It is therefore determined by inspection that $I_{A'2}$ of figure 14(B) leads $V_{A'2}$ of Figure 13(D) by 150 degrees. Referring to Figure 12(C), it is seen that when I_{A2} leads V_{A2} by any angle from 30-degrees leading to 210-degrees leading, the contacts will close. The angle of 150 degrees being within this range, it is concluded that with unity power factor load as depicted by Figure 12(A) and (B) and with the temporary connections of Figure 11, the relay contacts should close, which should be verified by observing the contact action.

The above test is not limited to unity power factor load conditions, but is easily interpreted for other power factors. The current, $I_{A'2}$, of Figure 14(B) will shift in the leading or lagging direction in step with any shift in the actual load currents, Figure 12(B). It follows that the contacts should close for load current power factor angles ranging from 0-degree to 60-degree leading, and from 0-degree to 120-degree lagging.

A supplementary test is in order, as indicated by the vectors of Figures 14(C) and (D). In this case, the current circuit test switches of Figure 11 are operated to deliver I_A and I_C to the current filter, with the current, I_B, by-passed back to the current transformer. The sequence components, by analysis, now appear as shown in Figure 14(D), and it is found that $I_{A'2}$ now lags $V_{A'2}$ by 90 degrees. This is a contact opening condition as determined from Figure 12(C), and the relay contacts should open.

The connections at the voltage filter now may be changed to short-circuit A and B terminals together with switch A open and switch B and C closed. The apparent voltages are analyzed in a manner similar to Figure 13, the results being shown in Figure 15. In this case, the negative sequence voltage, $V_{A'2}$ of Figure 14(D) is 180 degrees out of phase with its former position shown in Figure 13(D). The relay contacts should therefore operate in reverse to the action observed previously with the two current connection tests of Figure 14.

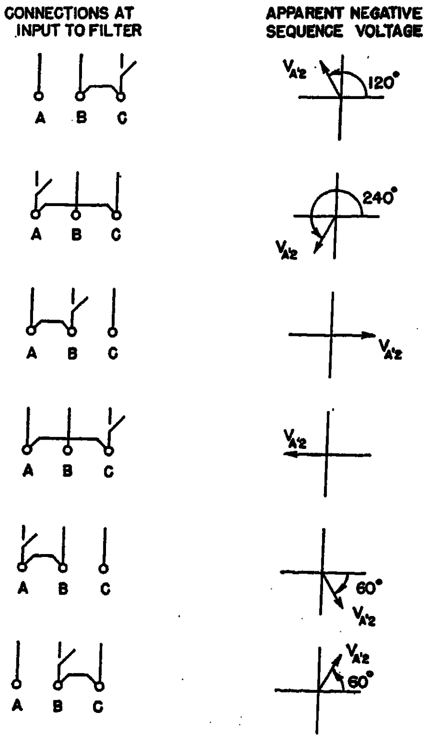


Figure 16. The apparent negative sequence may be obtained in any of six phase-angle positions with the connections as shown

The apparent negative sequence voltage may be shifted to any of six positions 60 degrees apart by choosing different combinations of shorted terminals and supply voltage. This is shown in Figure 16. Again, there are three different current test switch combinations representing two phase-to-ground faults which may be set up, and three more representing single phase-to-ground faults, each of which involves an apparent negative sequence current. By utilizing several of these combinations, so that each phase of the current and voltage supply is used at least once, the over-all installation can be checked quite satisfactorily.

Summary

It was not the intent of this paper to try to cover the entire subject of relay testing in complete detail, but, rather, to present some new material on the subject. It is hoped that the circuits here presented, together with the analytical methods used therewith, will lead to further new and useful circuits and test methods.

No Discussion

Insulation Level of Relay and Control Circuits

AN AIEE COMMITTEE REPORT

THIS is a report of a Project Committee of the AIEE Relay Committee which was established to study the insulation level of relay and control circuits. Various discussions disclosed that there have been experienced a number of flashovers of relay circuits, which have caused unnecessary interruptions of service. There also have been experiences with over-voltages on control circuits, which have damaged equipment and subjected personnel to hazardous conditions. These discussions indicated the need for determining a suitable standard of permissible voltages on relay and control circuits.

In considering this subject, the circuits can be subdivided into current circuits and control circuits.

Current Circuits

In the case of these circuits it is necessary to consider both the voltages which can be imposed upon the circuits by the current transformers under normal conditions, and under conditions of open circuit. In the former case, the voltages are generally limited to less than a few hundred volts and do not represent an insulation problem. On the other hand, the open circuit voltage of current transformers may reach a value of several thousand volts. Under this condition, it is expected that the operation of any relays associated with that particular current transformer may be improper or incomplete. The general opinion appears to be that it is not practical to design the current circuits to withstand the open circuit voltage of all current transformers, and special means must be used where this protection is desired. The usual method of providing this type of protection is to install a spark-gap across the current transformer secondary terminals. The flashover of these gaps must be sufficiently higher than the maximum voltage expected during system troubles to prevent these gaps from short-circuiting the current transformer and causing incomplete or erroneous relay operations.

Control Circuits

The insulation of control circuits, such as the tripping circuits of protective relays, presents a different type of problem. These control circuits are generally operated from a battery of 120 or 240 volts which is ungrounded. The voltage to ground of this battery is often stabilized by the use of small lamps connected from each terminal to ground. These lamps give visual indication of any accidental ground in the control wiring, and afford a means of dissipating, within limits, high-voltage surges to ground.

The voltage surges which occur in these control circuits are generally the result of opening circuits containing solenoids, such as relay coils and the opening and closing solenoids of circuit breakers. Whenever an inductive circuit is suddenly de-energized, a high-voltage will be generated. This is due to the sudden collapse of the magnetic field of the solenoid, which generates a voltage according to the equation $E = -Ldi/dt$. In the case of closing solenoids of circuit breakers considerable energy is available, with the result that the opening of such a coil circuit may produce severe surge voltages. Following are the principle troubles which may be caused by these surge voltages:

1. The destruction of the grounding lamps.
2. The breakdown to ground of the trip wire or trip coil of another circuit breaker, resulting in an unwanted opening.
3. Where bus protective schemes using instantaneous overcurrent relays are employed, the breakdown between a control circuit and relay coil circuit may permit the solenoid to discharge through the relay coils to the current transformer ground and cause all of the circuit breakers on the bus to open.
4. Operating and maintenance personnel may receive an electric shock.

It has been the experience that the voltages generated by inductive circuits are sufficiently high to cause difficulties in control circuits. At the same time relatively simple means are available to limit the magnitude of these voltages. It, therefore, becomes necessary to determine a standard which can be followed in the design of a control circuit. In order to obtain information on the experience of various power companies, a questionnaire was circulated among several companies. Following is a summary of this questionnaire and the answers received:

Question number 1. What standard test voltage should be used in relaying associated with power switchgear?

Of the 14 companies reporting, 13 indicated that a manufacturer's test voltage of 1,500 volts rms would be sufficiently high. The other company suggested a level of 2,500 volts.

Question number 2. Should different test levels be specified for different circuits?

With only one exception, the companies indicated that it would not be practical to apply different test voltages to different circuits, so that it appears to be reasonable to design all circuits of the relay for the same level.

Question number 3. What is the preferred method for taking care of surge voltages in the control battery circuits?

Paper 49-263, recommended by the AIEE Relay Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 18, 1949; made available for printing September 7, 1949.

Personnel of the AIEE Project Committee on Insulation Level of Relay and Control Circuits: E. L. Michelson, *Chairman;* R. E. Cordray, S. Goldsmith, T. R. Halman, V. J. Hayes, W. K. Sonnemann.

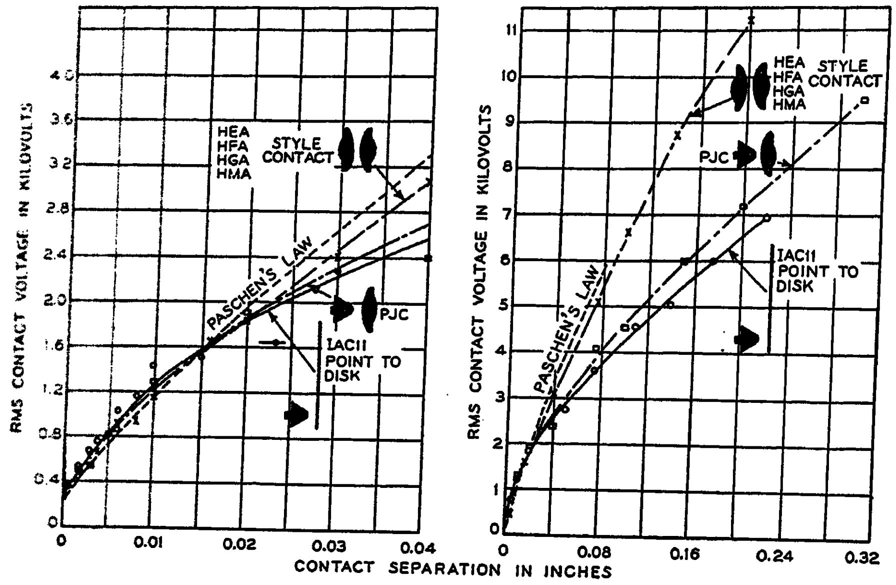


Figure 1. Spark-over voltage of relay contacts for General Electric relays

Note that below 0.1 mil the voltage breakdown increases. This is due to the separation being of the order of the "mean free path" of the electrons and hence an increase in field gradient to produce ionization by collision

All companies indicated that surge voltages should be controlled at their source rather than by designing relays for high insulation levels.

Question number 4. What test voltage is used during maintenance periods on equipment which has been in service?

The test voltages used vary considerably with the different companies. The 500-volt megger test is the most widely used. Some companies use a 1,000-volt megger, others, 220-volt, 1,000-volt or 1,500-volt alternating current.

As indicated under Question number 3 it was the consensus that surge voltages on the d-c control circuits should be limited by the use of special equipment on the solenoids, which are the source of these high voltages. Various methods are available to reduce the magnitude of the voltage, as for example, by installing a resistor across the solenoid to provide a discharge path when the circuit is opened. Discussions within the Project Committee disclosed that Mr. T. R. Halman, of the Detroit Edison Company, had done considerable work along this line. This resulted in the presentation of a paper given before the AIEE in 1948.[1] The paper describes in detail the source of high voltages in control circuits, the troubles caused by these voltages, and methods of their suppression. Following are the conclusions which are stated in the Halman-Harris paper:

"1. There is a high voltage produced by the decay of flux when an inductive device is de-energized. Uncontrolled, it may injure personnel, interrupt service, and damage equipment.

"2. The magnitude of this voltage depends on the value of the energizing voltage, the size, shape, and material of the magnetic circuit, the number of coil turns, and the speed of de-energizing. The discharge voltage of a small relay is often higher than that of a large circuit breaker closing solenoid.

"3. The energy stored in the inductive circuit varies approximately with the size of the device.

"4. A dry-plate rectifier paralleling the coil of each inductive device holds the discharge voltage to a safe value. A slow acting switch, a small capacitor (1 to 4 microfarads), a small resistor, or a gaseous discharge rectifier performs well when used with small low-energy devices such as control relays.

"5. A dry-plate rectifier connected across the control battery with its mid-point grounded prevents the communication of high discharge voltage between circuits.

"6. All these methods of reducing the surge voltage involve slowing the transfer of stored magnetic energy to the electric circuit, which in turn delays the dropout of relays or solenoids. This delay usually is not objectionable but must be evaluated carefully."

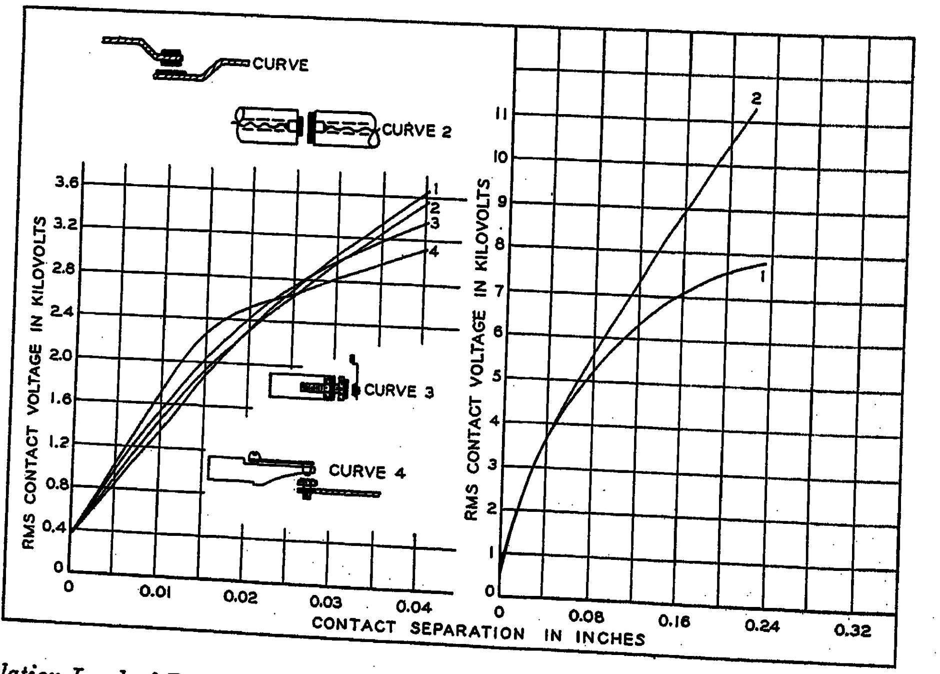
Arcing Across Relay Contacts

One of the vulnerable points in the d-c system is the gap between contacts, particularly those in high-speed relays. In order to obtain high-speed operation, it becomes necessary to use very small spacing between contacts. Discussions within the Project Committee indicated that it was not certain whether a 1,500-volt standard would be sufficiently high to prevent the possibility of contacts flashing-over due to surge voltages. Test data prepared by the General Electric Company and Westinghouse Electric Corporation, as shown in Figures 1 and 2, gives relationships between contact separation and voltage required to cause flashover.

Figure 2 shows the results of several tests made by the Westinghouse Electric Corporation on typical relay contacts in different mounting arrangements. The curve numbers, from 1 to 4 are identified with suitable sketches, drawn approximately to scale, to show the contacts and the method of supporting the contacts.

The results for the contacts of curves 3

Figure 2. Curves prepared by Westinghouse Electric Corporation showing spark-over voltage of relay contacts



and 4 are not shown on the right-hand side of the curve sheet because, at the higher values of spacing and voltage involved, the arc path was frequently between points on the metallic supports rather than straight through the air between contacts. This phenomenon was observed at about 5.6 kv, (0.10-inch separation) for the contacts of curve 3, and about 3.1 kv, (0.04-inch separation) for contacts of curve 4.

Consideration of these curves indicates that there is not a great amount of difference in the arc-over voltage of different forms of relay contacts at the separations used in high-speed relays. However, it should be noted that the form of the contact support and the connections thereto definitely have a bearing on the results. This is particularly noticeable at the high values of curves 1 and 2.

These curves have been drawn through points which deviate from a smooth curve. The amount of deviation seemed to depend upon air currents and temperature of the contacts more than anything else, although no definite data was obtained to prove this.

Relative humidity of the air also was found to be a factor in the results.

An investigation of recommended settings for high-speed relays indicated that 1,500 volts would not be sufficient to cause flashover of these contacts. The recommended gap settings for high-speed relays which operate in approximately 1 cycle varies from 0.015 inch to as much as 0.050 inch.

It can be seen from reference to Figures 1 and 2 that these gaps are sufficient to prevent flashover by a voltage of 1,500 volts.

Other Sources of Surge Voltages

While the principle source of surge voltages in control circuits is the opening of the inductive circuits, other types of surge voltages have been experienced. One case which was reported in the Project Committee was caused by the capacitive coupling between 66-kv cables and the control wiring. Sudden application of voltage to the 66-kv cables caused sufficient transient voltage to flashover the contacts of a protective relay and cause a transmission line to be opened. In this case the condition was corrected by the installation of capacitors in the control circuit.

Standards for Insulation Levels

At the present time, the ASA Standard on Power Relays[2] is being revised. The test voltage for relay circuits, as determined by this standard, will be a factory test voltage of 1,500 volts rms, and a field test equal to 75 per cent of the factory test, which amounts to 1,125 volts rms, or approximately 1,600 volts crest.

The Association of Edison Illuminating Companies specifies for oil circuit breakers that "the inductive kick caused by interrupting the closing currents shall be less than 1,500 volts crest." While the present AIEE Standard on switchgear does not cover this point, it is expected that future revisions will include some limitation on transient voltages.

References

1. VOLTAGE SURGES IN RELAY CONTROL CIRCUITS, T. R. Halman, L. K. Harris. *AIEE Transactions* volume 67, 1948, pages 1693–1701.

2. RELAYS ASSOCIATED WITH POWER SWITCHGEAR, *C37.1-1937*. American Standards Association (New York, N. Y.) 1937.

No Discussion

A New Technique in the Manufacture of Soldered Porcelain Potheads

A. E. PAPP
MEMBER AIEE

J. H. NICHOLAS
ASSOCIATE AIEE

WITH the development of the art of making metal to ceramic seals during the past two decades, there has been an ever-increasing interest by the public utilities of this country in the use of soldered porcelain potheads for terminating important cables. The advantages of bonded metal-to-porcelain seals over the more common gasketed seals in the manufacture of potheads are:

1. Permanent factory assembly and test of all joints between metal and porcelain.
2. Greatly increased resistance to aging, cold-flow, and permanent set.
3. Complete resistance to the chemical action of filling compounds and cable oils.
4. Greater stability through time and range of temperature.

The disadvantages are:

1. High initial cost due to the complicated technique required in manufacture.
2. Limited application due to mechanical and size restrictions.

Soldered porcelain potheads, properly installed, form a hermetic seal on the ends of cables so necessary for the satisfactory performance of paper-insulated cables.

Our earliest Soldertite potheads of about 12 years ago used a solder sealing process developed by a large porcelain manufacturer. The process was found to be satisfactory but rather hard to handle. It also was extremely expensive and limited our procurement of porcelain to one source. Because of these factors we decided to develop our own metal-to-porcelain solder seal.

This new process, we felt, should be adaptable to a wide range of sizes and shapes of porcelain required by the users of our equipment. It should be so devised that it could be used on porcelain produced by any porcelain manufacturer. It should be as simple as possible and more rugged than the other then-known methods. It should be less expensive than the other contemporary processes.

We finally did develop a new process and are using it today. Briefly, the process consists of coating certain areas of a heated, chemically clean glazed porcelain with molten copper sprayed from a metalizing gun; tinning those areas with solder; and then soldering to a metal part. The completed seal consists, really, of three bonds or joints in series. The copper-to-porcelain bond is the most difficult to make and requires a great deal of consistency and control. The copper-to-solder bond is the easiest to make and requires only ordinary care. The solder-to-metal bond is not very difficult to make but it does require manual dexterity and skill that come only with long practice.

Because the initial copper-to-porcelain bond is of the adhesion-cohesion type, it is highly dependent upon the absolute chemical cleanliness of the porcelain. The porcelain is cleaned with detergents and carbon tetrachloride and given a final cleansing in a vapor degreaser. After this cleaning treatment nothing is permitted to touch the bond surfaces until they are sprayed with molten copper. The porcelain then is heated in a chemically clean atmosphere to drive off all of

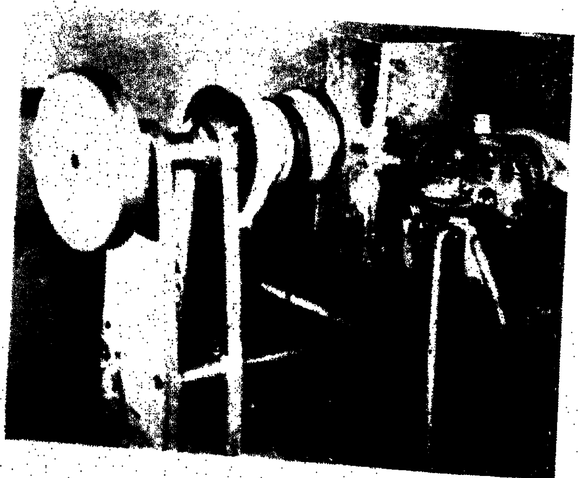


Figure 1 (left). The spray fixture facilitates rapid adjustment of gun-to-porcelain spacing, masking with transite shield and rotation of porcelain

Figure 2 (right). Metalized areas are scrubbed with soldering flux prior to tinning

---

Paper 49-264, recommended by the AIEE Insulated Conductors Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted November 18, 1948; made available for printing September 20, 1949.

A. E. Papp and J. H. Nicholas are both with the G and W Electric Specialty Company, Chicago, Ill.

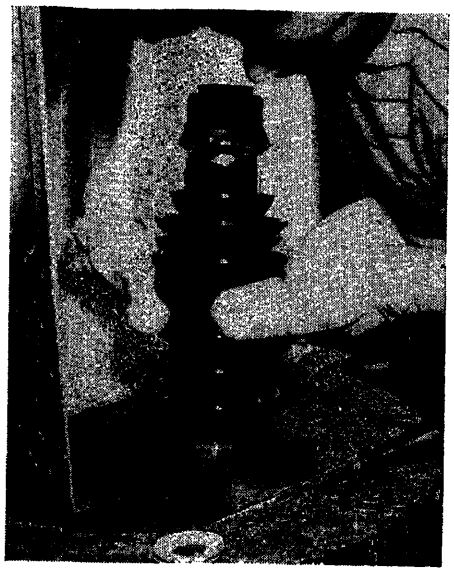


Figure 3. Openings of the porcelain tube are plugged and the metalized areas tinned in a solder pot

the surface moisture and to raise the temperature to such a point that it will withstand the thermal shock of the oxygen acetylene flame of the metalizing gun.

The heating is done in an electric oven that permits close control and recording of temperature. The proper temperature and time cycle for this oven was determined experimentally by testing a large number of units to destruction to establish the optimum conditions for obtaining the best copper-to-porcelain bond. After the heating cycle is completed, the porcelain is placed into a special spraying fixture that exposes only the areas to be metalized to the flame and molten metal spray of the metalizing gun, see Figure 1. The spraying of molten copper onto certain areas of porcelain is done very rapidly and under closely controlled conditions. When the spraying is completed, the porcelain is quickly placed into another oven whose temperature is somewhat lower than that of the first oven and allowed to cool slowly to soldering temperature.

Every step of this metalizing process, from oven to oven, must be rigidly controlled in time, temperature, and technique. The porcelain must be heated gradually and evenly to the proper spraying temperature. The movement of the porcelain from the first oven, to spraying fixture, to the second holding oven must be done rapidly in order to keep the ceramic from fracturing due to the thermal shock of room temperature. The total allowable oven-to-oven time is about 15 or 20 seconds. Because of the short period of time available for molten metal spraying, every bit of equipment that is used is especially designed and arranged so that the operation is done swiftly.

After the metalized porcelain has cooled gradually to a predetermined temperature in the holding oven, the pieces are removed individually, the metalized surfaces are cleaned with soldering flux, see Figure 2, and tinned with molten 50-50 solder of a closely regulated temperature, see Figure 3. Then, before the insulator has a chance to cool, it is immediately soldered to freshly tinned metal parts that are heated to a certain critical temperature. The soldering is done by placing the insulator on end into a tinned metal hood, with a preformed asbestos gasket forming a gap and solder dam, and filling the gap between the tinned porcelain and the tinned hood with molten solder. As this molten ring of solder cools and solidifies, a large soldering iron is used in contact with the top of the molten solder to keep it in a liquid state as the ring solidifies from the bottom up. This same procedure is used for making the solder joint at the wiping sleeve, Figures 4 and 5.

When the soldered porcelain assembly has cooled to room temperature, it is carefully washed and cleaned and given a "bump" test. This test is rather rough and crude but it tells us what we want to know. Essentially it consists of taking the whole soldered assembly and bouncing it from a height of about two feet onto a wooden table about ten times with the axis of the terminal parallel to the table.

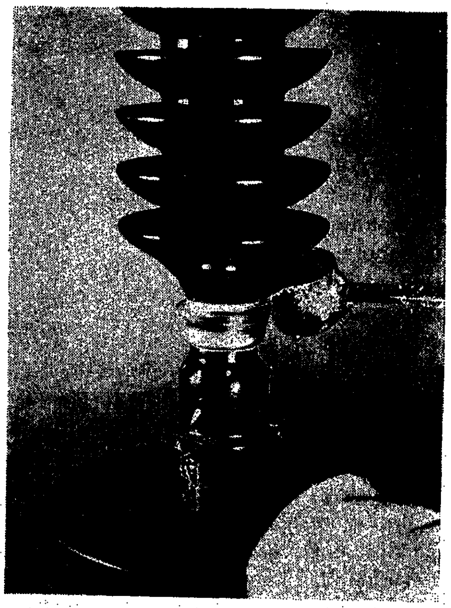


Figure 4. Solder is poured into the gap formed by the asbestos gasket between the hood and porcelain

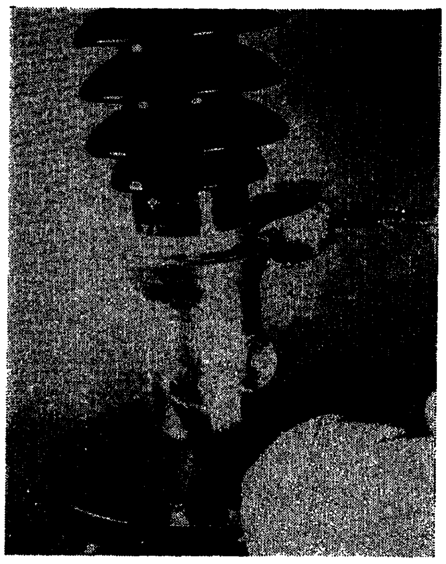


Figure 5. Wiping sleeve is soldered to porcelain as table is rotated

Following the bump test, the pothead is tested for leaks with 30 pounds per square inch air pressure while it is submerged in hot water at about 170 degrees Fahrenheit. The final test is an electrical test in which the inside bore of the porcelain is explored with a grounded probe while the wiping sleeve and conduction glaze area is energized at 20-kv 60-cycle alternating current. We have found that if the potheads withstand the leak test and the electrical test after being bumped, they will perform without difficulty in the field.

The hood and the wiping sleeve are made from copper spinnings and machined bar stock brazed together with silver solder. No castings are used, thus eliminating trouble due to porous castings. The pothead is designed to reduce the amount of heat and time necessary to make a good tight soldering job in the field. The electric connection at the top is made by crushing the hood thimble tightly against the internal cable ferrule by means of the pressure exerted by clamping the aerial lug onto the hood thimble. The internal cable ferrule has several grooves along its surface to permit free venting of the pothead through a small hole at the top of the hood while filling. This small vent hole is closed by a tinned machine screw and can be soldered shut with a soldering iron with no danger of damaging the soldered joint between the metal hood and the porcelain. The wiping sleeve is quite long and thoroughly tinned so that with ordinary care and attention to solder temperature there is little danger of damaging the joint between the wiping

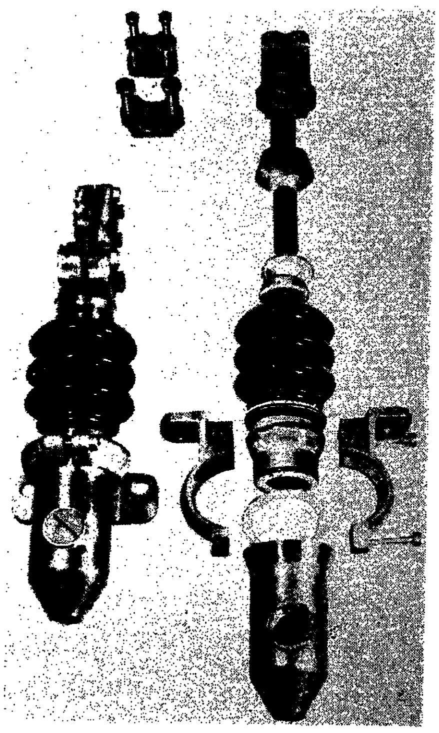


Figure 6. Component parts to complete the Soldertite pothead installed on paper-insulated lead-covered cable

sleeve and the porcelain due to the heat from the wiping operation, Figure 6.

The Soldertite method of attaching metal parts to porcelain is the result of a large number of experiments and tests undertaken to establish a metal-to-porcelain soldered joint that would be hermetically tight under all known service conditions.

The electrical design criteria we established for this new pothead were rather easy to meet even though there were some new principles involved, such as conductive glaze shielding along the lower external cylinder of the bushing terminating under the lowest petticoat. This new principle (for potheads) has the effect of reducing the electrical stress at the terminus of the grading cone on the inside of the pothead porcelain. The mechanical design criteria were something else again and were quite complex in their solution. It was decided that the pothead should remain mechanically perfect and free from leaks at 30 pounds per square inch and vacuum under the following conditions:

1. Repeated instantaneous thermal shocks from 250 degrees Fahrenheit to −10 degrees Fahrenheit.

2. A frigid test for about four hours at −70 degrees Fahrenheit.

3. Abnormal vibration of aerial conductors and pothead mounting structures.

4. Severe shock such as is used in the bump test.

To eliminate "pinch-off" or cracking of the porcelain under the low temperature −70 degrees Fahrenheit test, we had to proportion our metal sections and our tapered sections of porcelain so that the metal parts could contract elastically without rupturing the soldered joint or crushing the porcelain. Then too, these same structures had to be elastic enough to "take" repeated flexings as occur when the pothead is subjected to repeated thermal shocks from 250 degrees Fahrenheit to −10 degrees Fahrenheit. We found that even the shape, size, and condition of the asbestos gaskets affected the performance of the pothead under the rigid requirements that were established.

During the first year of development it was not uncommon to lose nine out of ten insulators during various stages of manufacture and test. We found that too rapid initial heating developed cracks in the porcelain. When the hot porcelain was exposed to room temperature too long, cracking and splitting occurred. A mechanically weak conductive glaze caused cracking. Because it was easier to clean, we found that metalizing adhered to glazed surfaces better than to unglazed ones. We found that in periods of high humidity we were getting porous soldered joints because of the moisture absorbed by the asbestos gaskets turning to steam when hot solder was poured into the joint. We found that to get the perfect solder joint all parts must be hot and freshly tinned to eliminate the oxide film.

Tests to destruction indicate that the soldered metal-to-porcelain joint of Soldertite potheads equals or exceeds the tensile strength of the glaze and the porcelain body. Joints were destroyed by straight mechanical pull tests and by using internal hydraulic pressures up to 1,500 pounds per square inch. The latter tests caused bloating of the metal parts and a resultant peeling action on the metal-to-porcelain joint. Examination of the fractures revealed that in nearly all cases the mechanical failure of good joints occurred in the solder, the copper coating, the glaze, and the body of the porcelain rather indiscriminately, indicating that the various components of the joint were of about equal strength.

However, a nondestructive test that will give an absolute indication of poor bonding of copper particle to glaze is yet to be found. Long time internal pressure tests using kerosene at 180 degrees Fahrenheit and 30 pounds per square inch and hydraulic tests at 300 degrees Fahrenheit with pressure surges of 60 pounds per square inch do not positively reveal poor bonding in all cases. X-ray examination and high-frequency dielectric tests have failed to discriminate between good and bad joints and little hope is held for test results with a Sperry Reflectascope using supersonic vibrations.

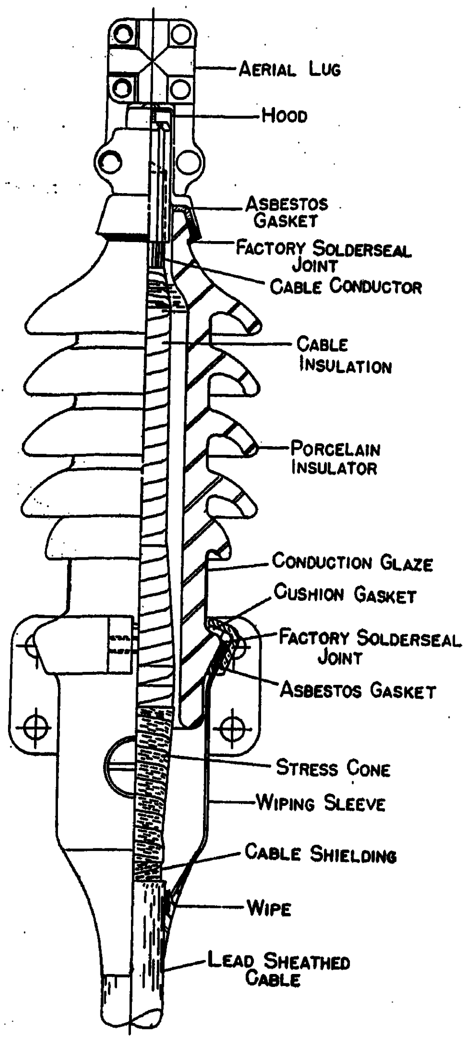


Figure 7. Details of a 15-kv 500,000 circular mils Soldertite pothead installed on paper-insulated, lead-covered cable

So far all that can be done is to test joints to destruction periodically as a check on the process and at the same time rigidly adhere to established procedure and technique.

Soldertite potheads, see Figure 7, using the described method of soldered porcelain technique have been in actual service since 1940 and many of them are installed on gas-filled cables. To date, the service record of this type of construction is excellent. Industry-wide approval is indicated by the more and more general use on all types of cable and by complete standardization on this type of pothead by several large utilities in this country.

Discussion

L. I. Komives (The Detroit Edison Company, Detroit, Mich.): This discussion presents the thinking of engineers of The Detroit Edison Company concerning potheads in general and soldered potheads in particular. We would like to commend the G & W Electric Specialty Company for their progressiveness in working out problems pertaining to potheads especially in trying to eliminate true gaskets.

The G & W design also should be complimented on the electric connection between the ferrule and the terminal connector. This connection is made by crushing the hood against the ferrule when the clamp-type terminal connector is tightened in place. On the pothead we are using, this type of connection also is provided when a clamp-type terminal connector is used.

We believe the paper is very complete in describing the meticulous processes of manufacture, but it seems to us that the importance of control during manufacture, places too much responsibility on the dexterity of the workmen. The illustrations included in the paper show that the manufacturing process is vulnerable to many variables which are independent of the dexterity of the workmen. For instance, the amount of metal sprayed on the porcelain is not accurately controlled, the temperature of the solder, even though controlled, can vary beyond the desired limits after being placed in the ladle, and there are no safeguards against the inclusion of dross in the solder which would result in an inferior seal. In other words detrimental variables can enter the manufacture without being obvious to the workmen.

The ideal type of metal for metal-to-porcelain application is one which has the same thermal coefficient of expansion as the porcelain. We then could expect the two materials to expand in unison, without placing stresses on the metal or the porcelain.

It is recognized that seals-in-series, where one seal depends upon the behavior of another, is not desirous in such a terminal, the sealing ability of which is of greatest importance. The three materials used in the three seals in series have different coefficients of expansion, and in order to evaluate the resulting effects of temperature changes in the range of −10 degrees Fahrenheit to 250 degrees Fahrenheit, we have calculated the dimensional changes of the materials if these materials are unrestrained. We have estimated the outside diameter of the porcelain at the metallization point to be seven inches.

Table I

Material	Circumferential Increase, Inches	Diameter Increase, Inches
Porcelain	0.0114	0.0036
Copper Spray	0.0530	0.0168
Lead-Tin	0.0825	0.0262
Copper Sleeve	0.0554	0.0176

Table I of the discussion shows the copper-spray tends to pull away from the porcelain, the lead-tin tends to pull away from the copper-spray, and the copper sleeve is stressed to contain the lead-tin. We cannot visualize the action of the solder during temperature cycles, but we do know that on a cooling condition the solder will not revert to its original shape after being forcibly deformed during the previous heating, but rather the solder will necessarily be forcibly reshaped or the solder will pull away from the copper sleeve. We believe that a combination, such as this, is conducive to poor sealing and for that reason should be avoided. Inasmuch as we question the life of the seal, we believe it will be a comparative short time when one or the other metal will pull apart to produce an opening for leaking. The life of the seal should be infinite.

There is no provision in the G & W design for determining the level of the compound during filling. We believe that the field workman will either under-fill or over-fill the compound. In the case of over-filling, the overflow will flow out the venting hole, thereby making it very difficult to produce a good soldering job to seal the screw in the venting hole. Over-filling also will cause the compound to coat the ferrule and certain types of compounds will interfere with making good electrical contact when the hood is crushed against the ferrule when the aerial lug is tightened in place. On a well designed pothead of this type, the venting hole should be large enough to determine the level of the compound, and should be located at the level to which the hot compound is poured, filling of course from the bottom upwards. The proper pouring level is thus indicated by the issuance of compound at the venting hole. The venting hole can then be closed and the natural shrinkage of the compound during cooling will provide expansion space for service operation.

Inasmuch as the metal, having the same thermal coefficient of expansion as porcelain is not available for this application, the next best is to seal between the porcelain and the metal with resilient material, such as synthetic rubber, thereby preventing the possibility of overstressing either the porcelain or the metal. Such an assembly makes for a semiflexible joint wherein mechanical shock to the metal is not transferred to the porcelain. Such an assembly also provides for relieving the porcelain of the mechanical loading at the terminal, especially when the porcelain is not truly round at the hardware point. Under heavy loading, the resilient sealing material will conform to the shape of the porcelain, without harmfully spot-loading the porcelain.

The pothead we are using is constructed to provide seals which are dependable, and which are easily tested to insure tightness. The seal between the porcelain and the hardware is effected by the rolled-on process which dependably seals and at the same time provides a joint which inherently is resilient. The seal at the terminal end has been 100 per cent reliable in the field when simple rules of applying are followed. The metal-to-metal joint, between the hooded-type terminal connector and the metal cap, in effect is a ground joint although it is not produced by any special machining. This metal-to-metal joint also acts as the stop to determine the compression of the copper-asbestos gasket which is in parallel sealing relationship with the metal-to-metal joint. To check on the sealing of the metal-to-metal joint, a few months ago, five potheads were drawn at random from stock and tested for seal without the copper-asbestos gasket. All five potheads tested tight.

The metal-to-metal joint also provides additional current carrying ability at this point.

These potheads have means for readily determining the level of the compound during filling or for later inspection. The hole in the top cap is used for this purpose as well as to vent during filling. This hole is sealed with a copper-asbestos gasket and a metal-to-metal ground joint between the plug and the cap.

A. E. Papp: Admittedly, there are operations in the technique that do not entirely eliminate the human element—what operation does? In our opinion, one of the factors that is producing almost 100 per cent satisfactory soldered porcelain seals in our process is the fact that we have given men a pride in their job to do a good job, and have given them a full realization of their responsiblities so that the job content really becomes a psychological factor in maintaining a good spirit and morale.

In the first place, we see no way in making all of the processes involved in the procedure automatic because of the large variation in size and shape of the different types of porcelain, and because of the inspection necessary between each operation. We could go on and on in a long discussion of this subject, but in our opinion we have mechanized as far as we want to go until we come to a point where we are making thousands of exactly the same type and size of soldered porcelain potheads, and then perhaps we can put some automatic machinery to work.

We feel that ladle temperature control is entirely unnecessary, since the ladle is constantly floated in the molten solder bath and is maintained at that temperature. It is removed only from the solder bath to pour the joint between the porcelain and the metal fittings, and the pouring period takes only about three to five seconds. In our opinion we would rather not complicate this very simple and satisfactory operation. Under the temperature limits that we use, very little dross accumulates at the top of the solder pot and the dross that does accumulate is skinned back every time a ladle full of solder is used.

Admittedly, the ideal type of metal to porcelain application is one which has the same thermal coefficient for the metal as that for the porcelain. However, that is over simplifying the problem. With the new metals coming into use we should imagine that such a joint could eventually be produced, but we rather suspect at a cost somewhat out of reason. We have a series of seals between porcelain, copper, solder and then copper again in which thermal coefficients of expansion vary considerably and theoretically to the disadvantage of the joint. But, as usual, the proof of the pudding being the eating thereof, the actual practical experience indicates that we have had not one whit of trouble from this source. What troubles we have experienced have been in the improper cleaning of the porcelain and its preparation for metal spraying. We rather suspect that the seal works in spite of the disadvantageous thermal coefficient of expansion because the pothead in actual service never reaches the temperature at which the solder itself solidifies, namely about 350 degrees Fahrenheit. Mr. Komives' observations regarding the differences in coefficients of expansion and diameter increases of the various materials making up the joint are correct. However we believe the seal works well because the porcelain which changes diameter the least is on the inside of the combination of the series of joints. Because this is true, we believe that regardless of the differences in coefficient of expansion between the copper spray the solder and the copper sleeve and the apparent disruptive force between the copper sleeve and the solder, the sum total stress in the joint, in fact, is entirely compression. The porcelain in effect stays where it is and the metals try to close in on it. However, because the porcelain will not move, all of the metals we believe end up at operating temperatures being in compression, that is, we are convinced that the copper spray, the solder and copper sleeve are all in compression in a radial direction, although admittedly they are in tension circumferentially. Because this is true we make the copper sleeve very light in construction so that it can be deformed as it cools.

We agree with Mr. Komives that the rolled-on type of seal that he speaks of is very effective and efficient. However, we see no particular advantage to it over any other good gasketed type of pothead construction. Gasketed potheads are still very much in the picture and with reasonable installation care, give excellent results.

Design Fundamentals of Potheads 69,000 Volts and Below

J. H. NICHOLAS
ASSOCIATE AIEE

PROPER selection, installation, and utilization of electric equipment for transmission and distribution systems requires a knowledge of the basic functions and design fundamentals of each component.

There has been considerable information published in American technical journals on the theory, design, test, and application of underground cables and cable joints. On the other hand, very little information is available on the termination of these cables in potheads and, therefore, some discussion on this subject seems warranted.

Potheads are usually employed where cables of moderate or high voltage are connected to generators, switchgear, transformers, overhead lines, or sectionalizing equipment. They are designed to provide:

1. A hermetic seal of the cable end to prevent contamination of the cable insulation by moisture or liquid dielectrics used in equipment to which the cables are connected.
2. A protection of the cable insulation against weather and adverse contaminating atmospheric conditions.
3. Physical protection and mechanical support of the cable conductors, insulation sheath, armor, and so forth.
4. Means for making external electric connections with the cable conductors within the pothead housing.
5. Adequate external dielectric strength commensurate with the desired insulation level of the cable system and an internal dielectric strength (including that of the cable end) a safe margin above this level.

Years of development work and operating experience have resulted in improvements in the design of potheads and an understanding of their electrical characteristics. New gasket materials now are in use which apparently have excellent aging characteristics, low percentage of set and cold flow, and high resistance to oils, asphalt compounds, and synthetic dielectrics. The design of the metal parts and methods of making the gasketed joints have been improved and permit easier and more reliable assembly in the field. Potheads having metal parts soldered to the porcelain insulator and requiring no gaskets also have been developed. A discussion of the various types of potheads and the design of their component parts would be quite long; however, some of the factors influencing the design of a pothead for the desired dielectric strength of a cable termination can be generalized and, therefore, the scope of this paper has been limited to this subject.

It is hoped that the following test data and electric field patterns will clearly indicate that the over-all dielectric strength of a pothead installation is not merely a function of creepage distances between live parts and ground or oversized porcelain insulators, but rather a function of proper pothead selection for a given size and type of cable and coordination of the dimensions and location of the stress relief cone with respect to its metal parts.

A basic cable termination has the conductor bared for several inches to permit electric connection to equipment and the conducting jacket, cable shielding or sheath removed for a given distance to provide surface insulation between the live conductor and the grounded sheath. The approximate electric field pattern for this type of termination, indicating the voltage distribution in and along the surface of the insulation when the cable is energized, is shown by Figure 1 (A).

The lines which begin coaxially with the conductor within the conducting sheath are equipotential lines that is, (1/2 and 1/4 equal 50 and 25 per cent respectively of the conductor to sheath voltage.) The lines which extend from the conductor to the sheath are flux lines. These two groups of lines provide a means of visualizing electric fields and when carefully constructed, permit relatively accurate estimates of capacitance and voltage gradients. High concentration of the flux lines upon a small area of a conductor and close grouping of the equipotential lines near this area indicate zones of high electric stress and possible points of insulation failure.

The electric field patterns included in this paper were developed graphically. Although the effect of the different dielectric constants (specific inductive capacitance) of the various insulating materials has not been included, these patterns are quite sufficient to indicate the high-potential gradients possible at cable terminations. They also emphasize the importance of good stress relief cone construction and pothead selection.

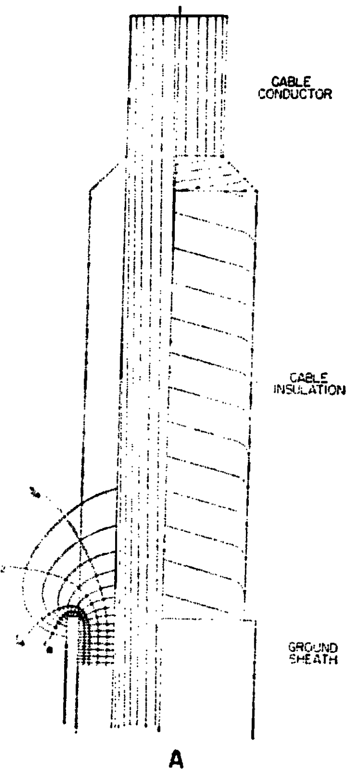
Figure 1(A) indicates that the potential gradients within and along the surface of the cable insulation are not uniform and in this case, approximately 75 per cent of the conductor to sheath voltage appears across the first inch of surface insulation. In the design of cable terminations it is important to realize that the electric field and potential gradients are not a function of the exposed length of cable insulation but mainly the shape, size, and separation of the electrodes.

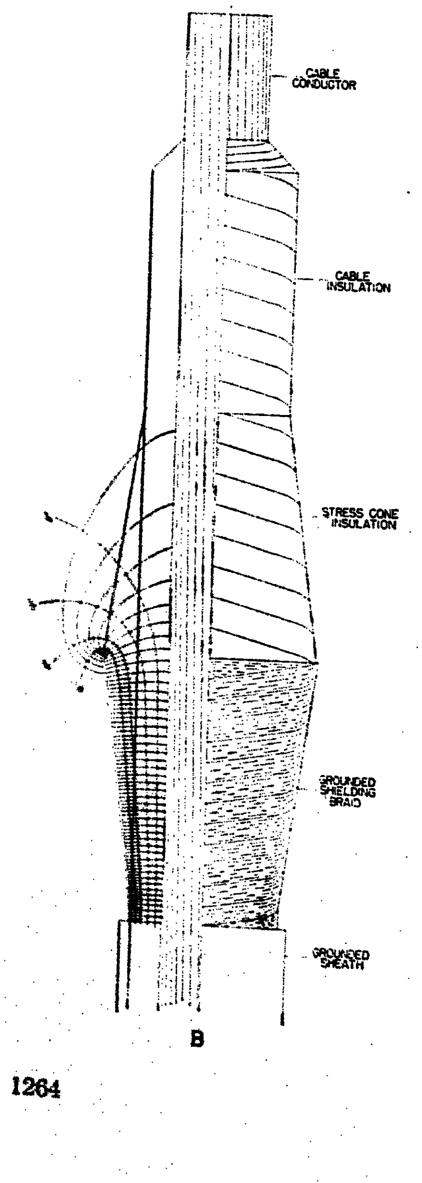
Figure 2 indicates 60-cycle surface flashover values for various lengths of exposed cables in air. Ionization of the air (corona) begins at the sheath end at the same potential (for a given size and type of cable) regardless of the length of

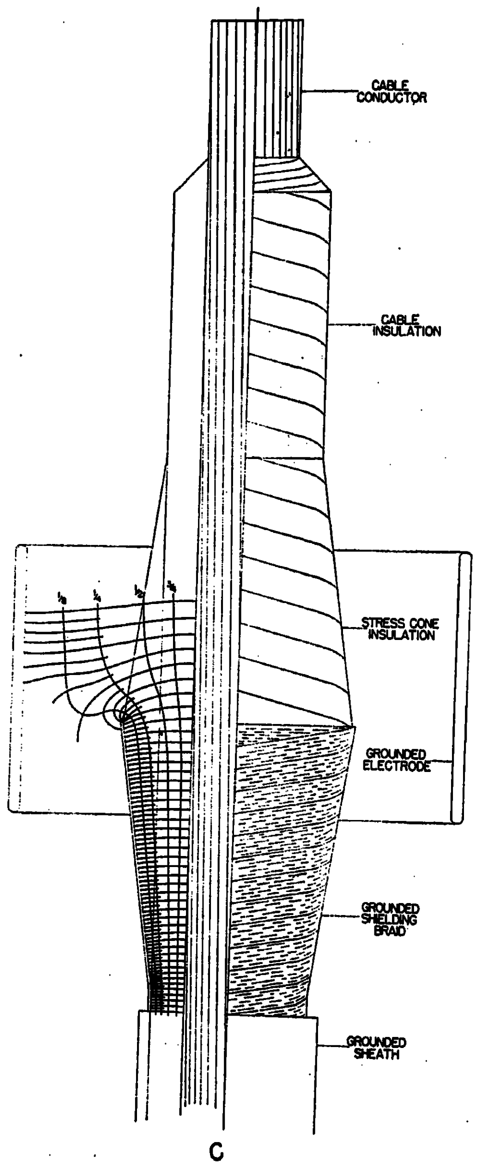
---

Paper 49-265, recommended by the AIEE Insulated Conductors Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17-21, 1949. Manuscript submitted July 1, 1949; made available for printing September 12, 1949.

J. H. NICHOLAS is with the G & W Electric Specialty Company, Chicago, Ill.







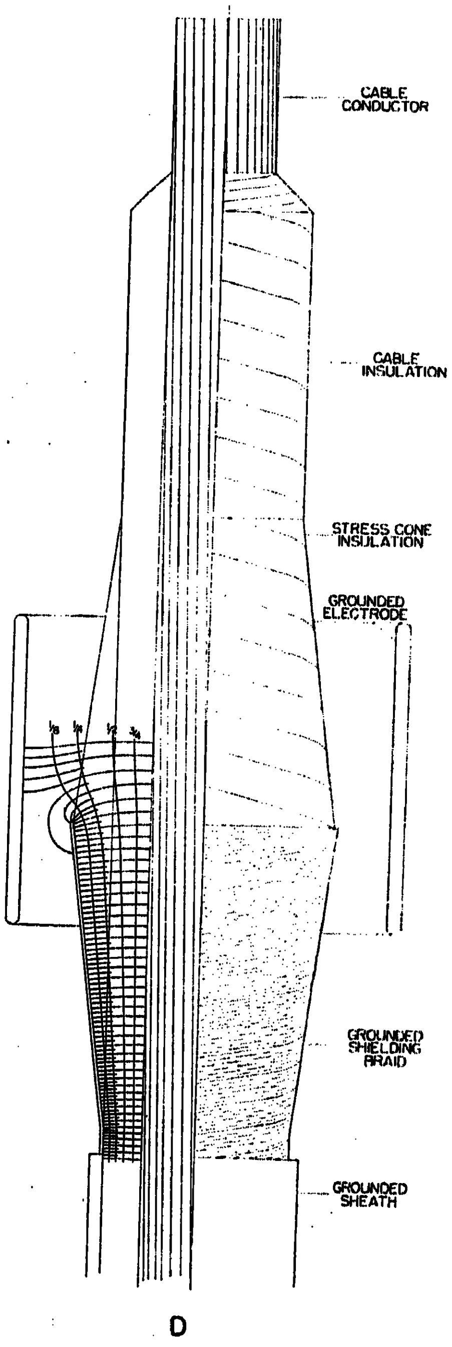


Figure 1. Electric field patterns for various terminations of a lead-sheathed cable having a 750,000-circular-mil conductor and 24/64-inch of insulation

exposed insulation, but some increase in flashover is obtained as the length is increased because energy is required to maintain this ionization resulting in a voltage drop along the surface of the insulation. Also, the ionization in effect, alters the size and shape of the electrode with a consequent redistribution of the electric field. The ionization in these cases began at relatively low voltages (approximately 8,000 volts between conductor and sheath) and is undesirable because of its deteriorating effect upon cable insulations and excessive radio interference.

The cable surface flashover can be materially increased and the effects of weather and atmospheric contamination practically eliminated by enclosing the cable termination of Figure 1(A) within a pothead and filling the internal space with a dielectric medium of greater electrical strength than air such as asphalt compounds or oils, Figure 3. This construction allows considerable reduction in the required length of cable surface insulation and also permits the use of oil-impregnated paper-insulated cables of high-dielectric strength which are greatly affected by moisture contamination. The electric field, however, is not materially altered and, therefore, the operating voltage and cable life will be controlled by the puncture strength of the cable insulation at the termination of the sheath. This value is generally much lower than the puncture strength of the cable proper, see Table I.

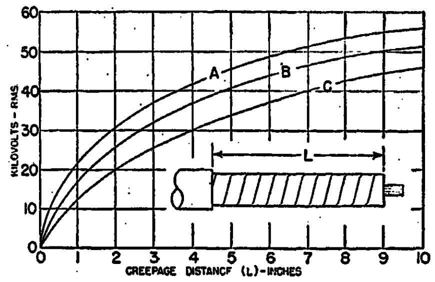


Figure 2. 60-cycle dry flashover values of various length cable ends in air

A. 2/0, 15-kv 22/64-inch rubber-insulated cable
B. 750,000-circular-mil 15-kv 16/64-inch varnish cambric insulated cable
C. 2/0, 15-kv 13/64-inch paper-insulated cable

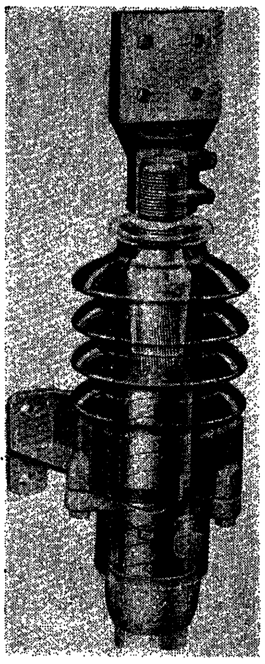


Figure 3 (left). Gasketed-type 15,000-volt pothead for compound-filled single-conductor cable terminations

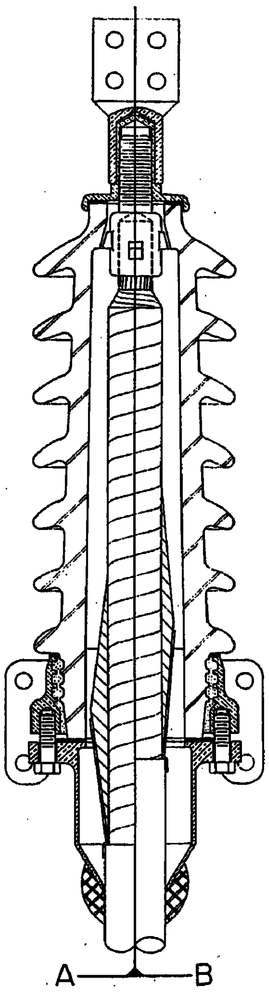


Figure 5 (right). Gasketed-type 34,500-volt pothead with two cable terminations. *A* is preferred because the use of maximum thickness of cone insulation and effective shielding by the pothead metal body results in minimum internal dielectric stresses

The high-potential gradients at the sheath end which produce the low puncture values may be reduced by applying to the cable additional insulation in the form of a double cone with a conducting braid electrically and physically connected to the sheath or cable shielding tape, wrapped up the cone and terminated at a diameter greater than that of the cable insulation. This construction is called a stress relief cone and the associated electric field, increase in dielectric strength and the reduction in potential gradients are evident in Figure 1(B) and Table 1.

Unfortunately, the effectively electrical strength of wrapped insulation in the axial direction is considerably less than in the radial direction, so it is important that the hand-applied insulation of the stress relief cone be carefully applied to form a logarithmic or, more practically, a gradual taper (4 to 7 degrees) since too steep a cone may produce high axial potential gradients with consequent failure within the cone insulation again resulting in low puncture strength. Stress cones for paper- or varnish-cambric insulated cables are generally constructed of tightly-wrapped half-lapped layers of oil-packed *VC* tape. Stress cones for rubber-insulated cables are made with rubber or moisture-free, dry *VC* tape. The resultant puncture strengths are a function of the diameters of the stress cone. See Table 1.

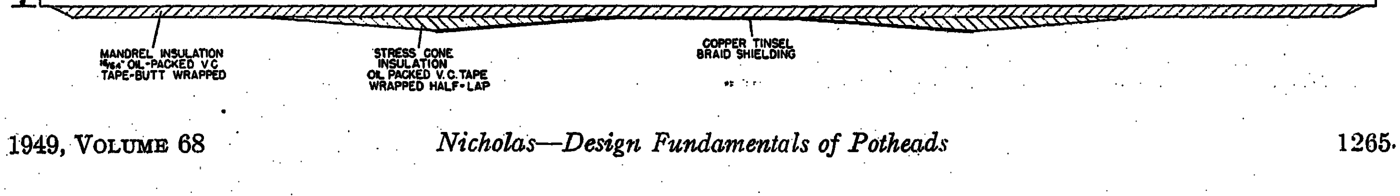
It has been the general practice to make the maximum thickness of stress relief cones for rubber, varnish cambric or solid-type, paper-insulated cables the same as that of the applied cable insulation. In view of the improvement in the quality of cable insulation with consequent reduction in thickness for a given voltage, this practice might result in inadequate stress relief cone design. Stress relief cones for gas- and oil-filled paper cables require special consideration owing to the relatively thin walls of insulation employed.

The electrical stresses at the stress relief cone are not a function of the radial dielectric strength of the cable but of the electrode separation. This being the case each pothead installation should be given proper consideration in order to obtain maximum internal dielectric strength.

It is impractical to establish general rules for stress relief cone design because of the great number of sizes and types of cables which may be used with a given pothead and it is, therefore, desirable to follow the recommendations of the pothead manufacturer.

Some pothead metal parts at sheath potential can be employed to advantage in reducing the axial potential gradients as well as the puncture gradients. Figures 1(C) and 1(D) illustrate the proximity effect of another grounded electrode upon the electric field of figure 1(B). This electrode can be part of the pothead body, a conducting glaze on the external porcelain surface, or an insulated shield mounted about the stress relief cone within the pothead and electrically connected to the sheath. The maximum longitudinal gradients in Figures 1(C) and 1(D) are about one-half and one-quarter, respectively, of the gradients in Figure 1(B). Sixty-cycle voltage-time tests were conducted on carefully insulated test mandrels with different thicknesses of stress cone insulation and various shielding electrodes. The results are tabulated in Table 1 and details of the test mandrels are indicated on Figure 4. All tests were made with the test mandrels immersed in cable oil having a viscosity of 100 Saybolt at 100 degrees Fahrenheit and a

Figure 4. Details of insulated mandrels prepared for tests listed in Table 1


30"
5"
4"
4"
4"
MANDREL INSULATION 16/64" OIL-PACKED V C TAPE-BUTT WRAPPED
STRESS CONE INSULATION OIL PACKED V.C. TAPE WRAPPED HALF-LAP
COPPER TINSEL BRAID SHIELDING

dielectric strength of 28 kv when tested in accordance with American Society for Testing Materials Standards *D117-33*.[1]

A number of tests not included in these data were made for the purpose of checking cable joint and stress cone materials. These tests indicated that very consistent results could be obtained with the hand-wrapped test mandrels. Also these tests indicated that the equivalent one hour radial dielectric strength of these insulated mandrels varied between 360-370 volts per mil for 16/64-inch of insulation.

To provide a general comparison of the dielectric strengths of the various terminations, the actual test voltages have been converted to an equivalent test voltage for a test duration of one hour. This applies the generally accepted theory that the breakdown voltage varies approximately as the inverse sixth, seventh, or eighth root of the time to failure. The eighth root results in the best agreement of these test data.

$$E=V\sqrt[8]{T}$$

E = equivalent test voltage for 1 hour
V = actual continuous test voltage
T = actual time to failure in hours

The shielding electrodes used in tests numbers 7, 8, 9, and 10 consisted of pothead porcelains with a conducting glaze on a portion of the external surface. The conducting area was electrically connected to the shielding braid and mounted coaxially with the test mandrels as indicated in Figures 1(C) and (D).

The use of gas-filled cables introduces an additional problem in the termination of the cable ends since it is generally desirable to fill the pothead with nitrogen at 12 to 15 pounds per square inch pressure. This low-pressure gas does not have the high dielectric strength of asphaltic compounds or oil and, therefore, it is possible that the internal surface dielectric strength may not be adequate to withstand the impulse and switching surge conditions of the cable system. The stress relief cone, therefore, should be modified by wrapping additional insulation over the stress cone of Figure 1(B) so that the shielding braid is effectively terminated within a layer (1/8- to 1/4-inch thick) of a semisolid dielectric of *VC* or paper and oil. This treatment materially increases the surface ionization point at the termination of the shielding braid with consequent increase in 60-cycle and impulse-surface flashover values as much as 25 to 30 per cent. The proximity effect of the shielding electrode can increase the surface flashover an additional 30 to 40 per cent.

The external dielectric strength of the pothead can be affected by the location of the stress relief cone with respect to the external grounded parts. By locating the stress relief cone well up in the porcelain, the external grounded parts can be effectively shielded so that an increase in external flashover value will be obtained. This principle is applied to some high-voltage bushings.[2] With this location in a pothead Figure 5(B) however, the electric field at the stress cone will assume that of Figure 1(B) with possible high-potential gradients at the braid termination. For maximum dielectric reliability of the cable termination, any effective electric shielding should be applied so that the potential gradients of the cable insulation and stress relief cone will be as low as possible since considerable variation in the quality of the hand-applied insulation and stress cone construction must be expected. The preferred location of the stress cone is, therefore, where the shielding braid is terminated below the top of the grounded external parts and as close to them (in a radial direction) as possible (Figure 5(A)). The reduction in potential gradients and increase in dielectric strength indicated in Figures 1(C) and 1(D) and Table I, are thus obtained. Proper clearances, however, should be allotted between the porcelain bore and the stress cone when an asphalt compound is the filling medium as too small a clearance might result in voids having low dielectric strength, especially when the existence of a vacuum in these voids is possible.

It is impossible to have an individual pothead design for each type and size of cable; therefore, they must be designed to handle a range of conductor sizes and different types of insulation. Variation in conductor size can affect the external dielectric strength (flashover) as much as 15 per cent. This effect is subject to considerable variation depending upon the length of the porcelain tube and the size and shape of the pothead electrodes. Flashover values obtained from tests made on potheads without conductors may be as much as 30 per cent above the values obtained when the pothead is assembled with the cable for which it was designed. Because of this fact, it is important that the flashover characteristics be determined with the pothead completely assembled on the size and type of cable for which it was designed. AIEE Pothead Standards Number 48[3] specifies that the electrical design tests be made in this manner. The older practice of using simulated electrodes and no internal conductors has, therefore, been eliminated.

Like cable joints, the actual construction of the cable termination and assembly of the pothead must generally be done in the field under unfavorable working conditions. Therefore, the dielectric strength and hermetic seals of the complete pothead termination are directly dependent upon the assembly procedure and skill of the personnel responsible for making the

Table I. Comparison of Dielectric Strengths of Various Cable Terminations Under 60-Cycle Long-Time Test Conditions

Test Number	Maximum Thickness of Stress Cone Insulation, 64ths Inch	Shielding Electrode Diameter, Inches	Electric Field Pattern	Test Voltage, Kilovolts	Duration of Test, Hours	Test Results	Equivalent Test Voltage on 1 Hour Basis, Kilovolts	Apparent Dielectric Strength Ratio of Termination to Cable Proper (Per Cent)—Note 4
1	None used	None used	Figure 1A	62.5	0.083	Note 1	45.7	51
2	None used	None used	Figure 1A	42.5	2.0	Note 1	47.0	52
3	16	None used	Figure 1B	62.5	1.5	Note 1	65.5	73
4	16	None used	Figure 1B	65.0	0.9	Note 1	62.5	70
5	22	None used	Figure 1B	62.5	3.5	Note 2	73.0	80
6	22	None used	Figure 1B	72.5	0.8	Note 1	70.5	78
7	20	$4\frac{1}{8}$	Figure 1C	72.5	0.8	Note 1	70.5	78
8	20	$3\frac{5}{8}$	Figure 1C	62.5	7.0	No failure	80+	89+
9	20	$3\frac{3}{8}$	Figure 1C	72.5	4.0	Note 1	82	91
10	28	$4\frac{1}{8}$	Figure 1C	67.5	7.5	Note 3	90	100+

Notes:

1. Puncture at terminus of shielding braid—ground potential.
2. Longitudinal failure from terminus of shielding braid to bare conductor.
3. Puncture of mandrel insulation at mid-section—cable proper.
4. Dielectric strength of cable insulation proper was about 360 volts per mil or 90 kv for 16/64-inch of mandrel insulation.

actual installation. A knowledge of the pothead design fundamentals given above plus the use of insulating materials of the highest grade and good installation technique should result in better cable terminations and a more reliable transmission of electric power by means of underground cables.

References

1. ASTM STANDARDS ON ELECTRICAL INSULATING MATERIALS. *Standard Number D117-33.* American Society for Testing Materials (Philadelphia, Pa), September 1933.

2. HIGH-VOLTAGE BUSHINGS DESIGNED TO MEET MODERN SERVICE, T. F. Brandt, H. L. Rorden. *Electric Engineering* (*AIEE Transactions*) volume 60, June 1941, pages 255–60.

3. POTHEADS. AIEE Standard Number 48, January 1948.

Discussion

L. I. Komives (Detroit Edison Company, Detroit, Mich.): The following questions were asked during the discussion period of this paper:

1. The author states that "There has been considerable information published in American technical journals on the theory, design, test, and application of underground cables and cable joints." This is not in agreement with Short's paper[1] in which he says that there is very little information available in this field. Would Mr. Nicholas be good enough to cite articles on which to base the above statement?
2. There are no tests shown in Table I to indicate differences between constructions shown in Figures 1(C) and 1(D). It seems to be desirable to have such test results.
3. We like the construction of the paragraph in which the author mentions that the stress relief cone is buried within a layer of semisolid dielectric of varnished cambric or paper and oil. This in our estimation is a very good construction not only for gas-filled cables as the author seems to think, but also for oil-filled cables and terminals, especially at higher voltages.

REFERENCE

1. A THEORETICAL AND PRACTICAL APPROACH TO THE DESIGN OF HIGH-VOLTAGE CABLE JOINTS, H. D. Short. *AIEE Transactions,* volume 68, part II, 1949, pages 1275–83.

A. L. McKean (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): The author has given an excellent and interesting summary of basic principles. It should be pointed out that these fundamentals of stress cone termination and auxiliary shielding can be safely carried to much higher voltage levels. For example, extensive testing on compression cable at potentials up to 200 kv to ground have been carried on successfully, employing simple stress cones and auxiliary shielding as demonstrated in Mr. Nicholas' paper. At higher voltage levels, more comprehensive capacitor-type designs appear mandatory.

J. H. Nicholas: Mr. Komives is probably justified in questioning the statement that considerable information has been published upon the theory, design, and test of cable joints.

The treatment of cable joint design fundamentals, detailed installation and construction information on a number of typical joints and the relatively large number of references (compared to potheads) included in the Underground Systems Reference Book[1] published by the National Electric Light Association in 1931, may have been somewhat misleading. Especially, since this material dates back to the 1916–1930 period.

A paper, such as Mr. Short's,[2] is certainly needed to indicate present-day practice in joint design.

The electric field pattern of Figure 1(D) was not included for quantitative evaluation but for illustrating the relative proximity effect of a shielding electrode. Pothead designs for 69 kv and below generally do not employ internal shielding electrodes and the tests reported in Table I are intended to illustrate the improvement possible within this voltage classification.

The degree of proximity indicated in Figure 1(D) has been utilized in pothead designs of higher voltage ratings with very encouraging results. This is confirmed by Mr. McKean's comments.

We have been recommending that the embedded type of stress relief cones be used on oil-filled pothead installations for 10 years, and we, therefore, are in full agreement with Mr. Komives on this point.

REFERENCES

1. UNDERGROUND SYSTEMS REFERENCE BOOK. Edison Electric Institute (New York, N. Y.) 1931.

2. A THEORETICAL AND PRACTICAL APPROACH TO THE DESIGN OF HIGH-VOLTAGE CABLE JOINTS, H. D. Short. *AIEE Transactions,* volume 68, part II, 1949, pages 1275–83.

Cable Accessory Design Utilizing New Laboratory Techniques

PHILLIPS N. BOSWORTH
MEMBER AIEE

HAROLD K. FARR
NONMEMBER AIEE

Synopsis: Cable joints and potheads are a vital link in the high-voltage cable circuit for power transmission. Some recent designs of joints and potheads are described with emphasis on the use of the electrolytic field-analyzer for electric stress analysis and of ultrasonic transmission for controlling manufacturing quality.

IN RECENT years the introduction of high-pressure fluid-filled cable systems for power transmission and the trend toward higher voltages have posed new problems in the design and construction of cable joints and potheads. Certain laboratory techniques have contributed much to the solution of these problems by providing the designer with accurate information and the producer with dependable test methods.

Adequate mechanical strength and easy field assembly must be achieved without sacrificing economy. Joints in particular must be designed to the smallest possible dimensions since they are the principal factor in determining costs of manholes in underground installations. The proper electrical strength must be obtained by good stress control and the use of modern dielectric materials. Laboratory methods have helped particularly to satisfy the electrical requirements although these, in turn, stem largely from mechanical considerations.

Before discussing methods of testing and development of cable accessories, a general description will be given of certain modern high-voltage cable accessories.

Joints and Potheads for Modern Cable Systems

Cable systems may be classified according to the operating pressure of the filling medium as low, medium, intermediate, or high pressure. Oil-filled systems may be either low-pressure or high-pressure systems. Gas-filled systems may be any of these classes. Nominal pressure ranges which apply in each case are as follows:

	Pounds Per Square Inch Gauge
Low pressure	0–15
Medium pressure	35–45
Intermediate pressure	100–150
High pressure	180–225

In addition to being either gas- or oil-filled, high-pressure systems may be referred to as self-contained and pipe-cable systems.

The difference in these two types of high-pressure systems basically rests with the fact that the self-contained cable contains a filling medium under pressure from the time it leaves the factory. The pipe-cable is shipped from the factory with a protective jacket and is pressurized after it has been pulled into the steel pipe and the pipe is filled with the filling medium.

Cable Potheads and Joints

A pothead is a device for terminating the end of a cable so that it can be used for power transmission.

Joints in a cable system are used to join together two lengths of cable. The simplest type of joint is the normal joint. Special conditions require variations and complications in jointing procedure. Low-pressure oil-filled cable lines often follow a profile with considerable variations in elevation along its length. Static oil pressures may develop which will expand or burst the lead sheath due to these differences in elevation. Stop joints then may be furnished to sectionalize the cable oil supply so that excessive static pressures do not appear. A stop joint connects two cable ends together but at the same time contains an oil barrier to separate the oil supplies of the two cable ends being joined.

In high-pressure systems the static pressure problem is not present. Stop joints may be used, however, to help control the filling medium and prevent large oil or gas losses under fault or repair conditions. Occasionally it is necessary to join a high-pressure system to a low-pressure system and this requires separation of the two pressure mediums.

In general, stop joints are not common on gas-filled systems. Each layout must be considered on its own merits.

Potheads for High-Pressure Systems

Figure 1 shows representative potheads for a 115-kv high-pressure cable system. Figure 1(A) shows the pothead as used for a self-contained cable system with an outer lead sheath over the re-enforcing tapes. The pothead design is a practical correlation of three sections, the high-pressure section, the low-pressure section, and the electrical stress control section which physically involves portions of the other two sections.

The high-pressure section includes the high-pressure self-contained cable entrance with a double re-enforced wipe shown at *A*, the nonmagnetic support casting *B* and the stop tube *C*. This last unit is a cylinder made by laminating paper impregnated with resin. It is characterized by high mechanical as well as electrical strength and is used here to seal the high-pressure fluid from the porcelain.

The low-pressure section consists of the compartment outside the pressure cylinder and inside the enclosing porcelain

---

Paper 49-266, recommended by the AIEE Insulated Conductors Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 22, 1949; made available for printing September 8, 1949.

PHILLIPS N. BOSWORTH and HAROLD K. FARR are both with the General Electric Company, Pittsfield, Mass.

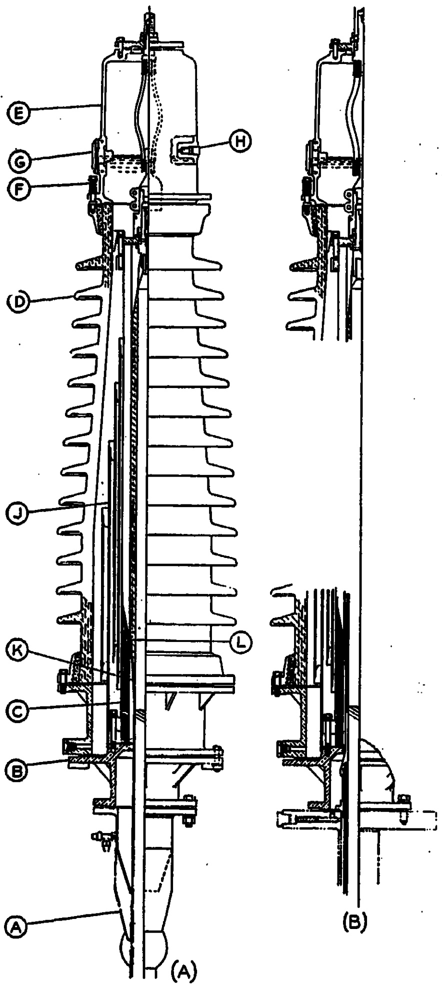


Figure 1. Pothead for high-pressure high-voltage cable termination

A—For self-contained cable
B—For pipe cable

shell *D* and the dome *E*. This section is designed to operate at a gauge pressure which would equal 10 pounds per square inch at a temperature of 75 degrees centigrade and in any case would not exceed 100 pounds per square inch. A pressure relief feature is afforded by the use of the springs *F* to maintain pressure on the gasket seal between the dome and the top of the porcelain. The whole dome will lift at some pressure between 50 and 100 pounds per square inch and reseal when the pressure returns to its normal range. This is provided to protect the low-pressure section from a leak in the high-pressure section. Pressure changes due to normal temperature variations in the low-pressure section are accommodated, without lifting the seal, by an expansion chamber in the dome. The dome also contains an oil-level gauge *G* and an outlet *H* for treating and filling.

The large oil volume in the low-pressure section is broken up as shown at *J* by cylindrical barriers spaced around the stop tube to increase the electrical strength.

This description has been given on the basis of a pothead being installed on a high-pressure self-contained cable. When the pothead is used on a high-pressure pipe cable system, the double wipe at *A* and the wiping sleeve are omitted and the pothead is adapted for mounting directly to a plate on the pipe riser. This construction is shown in Figure 1(B). Except for this mounting provision, the pothead may be the same for both applications.

The function of the stress control section is the termination of the cable ground shield in a way which will avoid setting up dangerous electrical stresses. In the cable proper a high voltage is supported across a relatively narrow insulation space between the conductor and shield because the electric field is uniform and radial, that is, at right angles to the paper layers. At the pothead the ground shield is tapered out gradually in region *K* in Figure 1(A) to the inner surface of the stop tube so as to keep the longitudinal stresses small. This insulation is built up by means of taping applied by hand over the regular cable insulation up to the inside of the ground shield and the stop tube.

Its maximum diameter is made as large as is consistent with easy assembly to insure a tight fit against the inside of the stop tube. This is done to decrease the width and thus increase the electrical strength of the oil channel between the taped section and the tube.

At the line *L* where the ground shield meets the stop tube, the ground circuit is continued into the wall of the tube by means of a conducting impregnant. This is a conducting material which is sprayed on the paper as the tube is manufactured and which hardens so as to produce a tube with a built-in shield. It is shaped so as to terminate the ground shield in the wall of the stop tube where the electrical strength is high. The conducting impregnant is very useful in controlling stresses where properly applied. Its proper use is, however, greatly dependent on experience and improper application can do more harm than good. In this connection, as in any stress control problem, the electrolytic field-analyzer discussed later is of considerable help.

It is apparent that the stop tube is a critical component of the pothead assembly subjected to high stresses both mechanical and electrical. Its manufacture is carefully supervised and checked by electrical and ultrasonic tests as described later.

POTHEADS FOR LOW-PRESSURE SYSTEMS

A new low-pressure high-voltage power cable pothead of the solder seal[1] design is shown in Figure 2. A solder seal is made at each end of the porcelain to the metal parts at the factory. There are no gaskets in the complete pothead assembly, therefore there can be no leaks in service from that source. The mounting clamp location *B* is adjustable up and down the wiping sleeve assembly to compensate for variations in cable cut-off length and mounting heights.

This pothead differs further from the high-pressure pothead described previously in that the stop tube is eliminated and the pressure of the cable fluid is retained by the porcelain shell. The ground shield is tapered outward and the cable insulation built up as in the previous design.

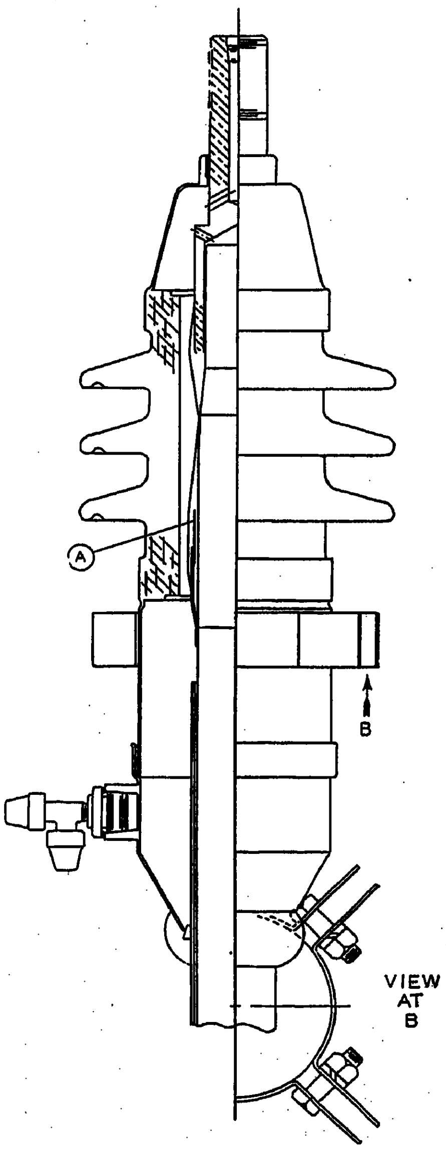


Figure 2. Solder seal pothead for low-pressure high-voltage cable system

Since the stop tube is not used, the end of the ground shield is protected by a semiconducting cloth, *A* in Figure 2, whose resistance is chosen to cause a gradual voltage drop in an axial direction along the cloth and thereby reduce the stress at the end of the ground shield at both 60 cycles and impulse voltages.

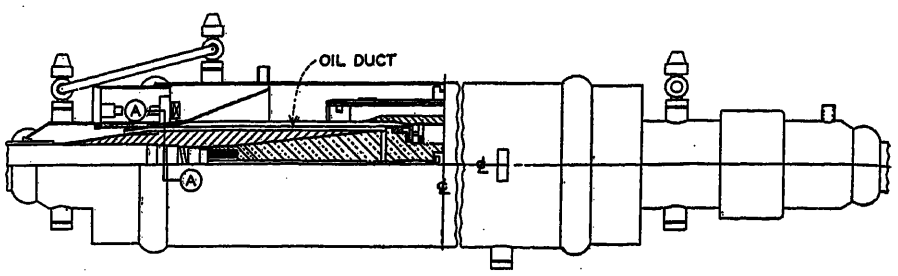


Figure 4. Stop joint for low-pressure oil-filled cable

Joints for High-Pressure Systems

For the purposes of this paper the discussion of joints for high-pressure pipe cable systems will be limited to stop joints. A representative design of such a joint is shown in Figure 3(A).

As mentioned previously, the stop joint separates the cable fluid in either side of the joint. This is accomplished by the use of a stop tube similar to that described in the discussion of the pothead. The two cable ends are brought in from either end of the joint to the connector at the center. The cable shield is cut back to the end of the ferrules, as shown at *A*, to give the required electrical creepage distance from the connector end to the grounded shield. As in the pothead, hand-applied tape wrappings then are built up over this cable end and over the connector to obtain a close fit to the inside of the stop tube. The high-pressure fluid from the pipe is admitted to the inside of this stop tube through the permeable tape and through the by-pass opening shown at *B*. This pressure is sealed inside this tube at the connector by the gaskets at *C* which are compressed against the connector and the tube by tightening the nuts at *D* on the connector. Thus the requirement is met in keeping the fluid on each side of the joint separated. It also can be seen that electrically the stop tube occupies the same critical position that it does in the pothead.

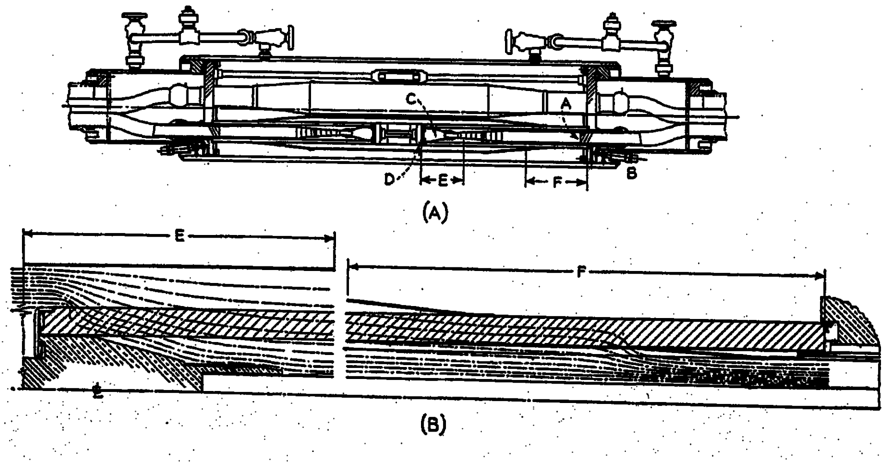


Figure 3. A stop joint for high-pressure pipe cable

A—Mechanical details

B—Electric field plot of regions E and F. The contours are equipotential lines spaced at intervals of 10 per cent of the applied voltage

The stress control problem is somewhat simpler in the stop joint than in the pothead because the ground circuit is not terminated but is carried completely around the joint.

Joints for Low-Pressure Systems

A new design stop joint for low-pressure oil-filled cable systems is shown in Figure 4. The principal feature in this new design is the stop tube which replaces the molded cone-shape barrier used in older designs. A good quality tube made from resin-impregnated paper laminant is strong mechanically and electrically and permits cylindrical taping contours which are easily made during field assembly. This component is like that discussed for the high-pressure joint. However, this low-pressure joint is designed for 15 pounds per square inch gauge pressure, which means lighter-weight parts throughout. It also is for use with hollow-core oil-filled cable so that provision is made for oil flow from the cable core up through radial holes in the connector half, and back through the longitudinal ducts under the stop tube. A discussion of the electric stresses in this duct area is given later in this paper.

Laboratory Techniques

The Electrolytic Field-Analyzer

An instrument of great help to the designer of high-voltage cable accessories is the electrolytic field-analyzer, which provides accurate answers to stress problems whose solution by numerical or graphical methods may be laborious or unreliable. This device, also called electrolytic tank, electrolytic bath, and electrolytic flux plotter, is based on the analogy between the voltage field in a dielectric and that in an electrolyte. Although the principle is familiar to many engineers it has not, perhaps, been sufficiently exploited.

In order to measure voltage stresses with this instrument, a model of the equipment to be analyzed is constructed using electrolytes of different depths or conductivities to represent different dielectrics. The voltages in the model are recorded on paper as a series of equipotential lines or voltage contours whose spacing determines the electrical stress or gradient in each region. Field plots obtained in this way have been applied to the design of high-voltage cable accessories to improve construction, to predict the voltage level and location of insulation failure, to assess the value of high-voltage tests, and as an educational aid.

Analysis of Electric Fields in Cable Joints

Figure 3(B) is a field plot, made by means of an electrolytic field-analyzer, which shows the voltage distribution in a high-voltage high-pressure cable stop joint. The contour lines are traces of surfaces of constant voltage drawn at intervals of 10 per cent of the total voltage. As all the insulating materials are laminated cylindrically, the electrical strength is high in a radial direction. The important quantity is, therefore, the component of stress in the direction of the cable axis, because this stress acts to cause failure along the laminations in which direction the electrical strength is lowest. It would be expected, for example, that failure would occur first along the outer surface of the laminated stop tube in region *G* of Figure 3(B) since the spacing of the

Table I. Breakdown Stresses for Stop Joints

Design	Applied Potential at Failure under Impulse Test, Kilovolts	Maximum Stress Along Stop Tube Surface at Failure, Kilovolts Per Inch Inner Surface	Outer Surface
A	325	139*	33
B	210	160	680*
C	350	140*	350
D	above 700	above 49	above 140

* Indicates location of failure.

equipotential lines along the surface of the stop tube is closest in this region. By measuring this spacing on the field plot, it is possible to calculate the maximum stress in kilovolts per inch along either surface of the stop tube for any voltage applied to the cable joint.

This was done for a series of high-voltage high-pressure gas-filled stop joints of different designs, one of which is that shown in Figure 3(A). These units also were given high potential tests under impulse conditions using the standard 1.5 by 40 microsecond wave. The crest value of the impulse wave sufficient to cause failure is given in Table I for each of the designs except for *D* which was subjected to 700 kv without failure. The maximum stress or voltage gradient to which each surface of the stop tube was subjected at the crest of the wave also is listed in the table.

It will be seen that two of the joints failed along the inner surface when the stress in that region reached about 140 kv per inch. The other two did not fail along the inner surface but were subjected to 49 and 160 kv per inch respectively. This implies that the average electrical creepage strength along the inner surface of all four stop tubes is about 150 kv per inch. Failure occurred along the outer stop-tube surface in only one joint and this was at a stress of 680 kv per inch. None of the other stop tubes were subjected to this stress although the outer surface of one reached 350 kv per inch without failure. Thus, the outer surface of the stop tube appears to be at least four times as strong for creepage as the inner surface. This may be explained by the fact that it is possible to wrap the tape much closer to the outer surface. In any case, the designer must take particular care to keep the stresses small along the inner surface of the stop tube. Design *D*, which shows very low stresses even at 700 kv, was developed after a study of field plots and high potential tests on units *A*, *B*, and *C*. This illustrates what can be accomplished by careful design guided by results obtained from the field-analyzer.

Field Stresses in Oil Ducts

The electrolytic field-analyzer also has been applied to the study of electrical stresses in the ducts used in cable stop joints to introduce the cable fluid. As indicated in Figure 4, these ducts run parallel to the cable axis between the inside of the stop tube and the outside of the built-up taping. They are formed by pressboard spacers which also run lengthwise along the inside of the stop tube. Figure 5 shows a field plot and the mechanical arrangement of the ducts at section *AA* of Figure 4. The field plot indicates that the maximum stress appears across the center of the duct. The voltage drop across this space is plotted in Figure 6 for joints with ducts of constant height but varying in number and width. The solid curve was obtained with four ducts by varying the width of the individual ducts. For the dotted curve the total duct area was varied by adding spacers of constant one-half inch width so as to change both the size and the number of ducts.

These curves show that the stress increases with the duct area. Comparison of the ordinates of the two curves taken at the same abscissa, however, shows the decrease in stress obtained, for a given total duct area, by breaking this area up into a number of smaller ducts.

This oil duct study illustrates the value of the field-analyzer as an educational device. Before the field plots were obtained, a fear was expressed that the abrupt change in dielectric constant across the radial wall of the duct might lead to excessive electrical stresses. The smooth flow of the voltage contour lines across the boundary, however, is convincingly shown by the field plot which indicates that no excessive stresses occur at the walls of the duct.

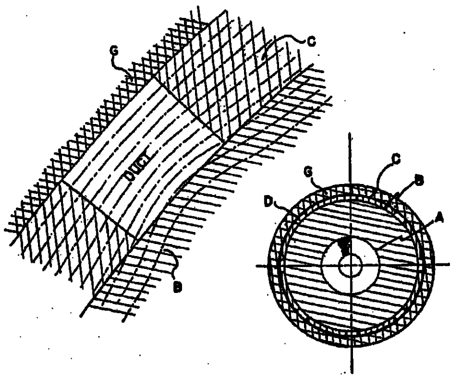


Figure 5. Fluid ducts in a stop joint. Field plot in region of one duct (left). Mechanical details (right)

A—Cable conductor
B—Paper insulation
C—Pressboard spacer
D—Fluid duct
G—Ground shield formed by conducting portion of stop tube

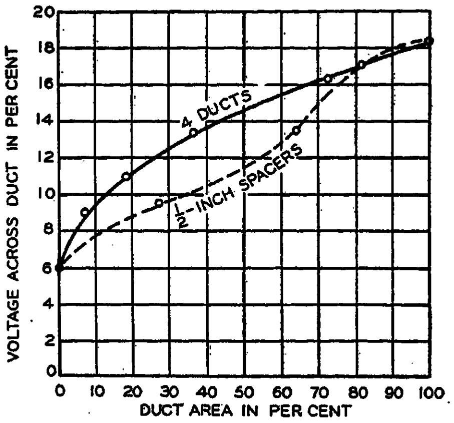


Figure 6. Electrical stresses in oil ducts, a résumé of data obtained from field plots like that of Figure 5

Ordinate is the voltage across the duct expressed in per cent of the total voltage applied to the stop joint. Abscissa is the actual duct area expressed as per cent of the total available annular area for fixed duct height

High-Voltage Testing of Stop Tubes

In order to subject stop tubes to a high potential test before assembly, a high 60-cycle voltage is applied to a tube immersed in oil with electrodes connected to opposite ends. To evaluate this test, field plots were made of the test set-up and of the assembled pothead. These plots showed that the electrical stresses on the stop tube under high-potential test were much different from those in the assembled pothead at the same applied voltage. Practically, it is difficult to arrange a high-potential test that will not be unrealistic in the sense that the tube is overstressed in some regions and understressed in others. Furthermore, a high-potential test of one-minute duration is not equivalent to long-time operation at normal voltage. These considerations have led to the adoption of additional tests on stop tubes.

The Ultrasonic Transmission Test

One of the most important factors affecting life and dielectric strength of solid laminated insulating materials is the possibility of small openings or poor bond between laminations. Cylinders of moderate wall thickness can be fabricated with considerable assurance that they will be free of such defects. Stop tubes used in high-pressure cable systems, however, may have a wall thickness of as

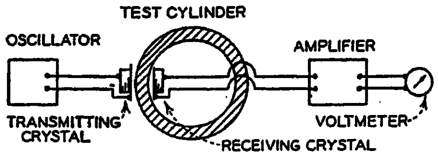


Figure 7. Ultrasonic transmission test for laminated stop tubes

much as 1 inch. They are, consequently, much more difficult to manufacture without defects. Because of the high mechanical stresses as well as for electrical reasons, a sensitive test for the detection of poorly bonded areas in such cylinders is particularly desirable. Such a test makes it possible to reject weak materials before assembly and aids in the development of improved materials.

The measurement of the transmission characteristics of a material for ultrasonic waves constitutes one such test. When a very high frequency sound wave is sent through a homogeneous material of moderate thickness in a direction perpendicular to the plane of the laminations, it emerges from the far side with little decrease in amplitude. In a region where any laminations are not well impregnated or well bonded, however, the transmitted amplitude will drop sharply. This so-called ultrasonic transmission test is one of several familiar techniques[2] for applying ultrasonic waves to the examination of the interior of opaque materials. Its present interest lies in its use as an indication of dielectric strength.

The ultrasonic transmission test has been applied to pressure stop tubes used in cable joints and potheads. To insure that the material will be free of interior corona, this test seems to be superior to any other applicable to these units. A stop tube is subjected during operation to full line-to-ground voltage from end to end.

Because poorly bonded laminations result in a weakness to voltage stress applied parallel to the laminations, their detection is more important in stop tubes than in materials subjected to purely radial stresses.

The test set-up is shown schematically in Figure 7. An electric oscillator of frequency approximately one megacycle per second is used to drive a crystal transducer which converts electric into mechanical vibrations. After the waves pass through the wall of the test cylinder, a second transducer converts the energy back to electric oscillations that are amplified and read on an output meter. Transducers and test piece are immersed in ordinary transil oil, which is an efficient sound transmission medium and which obviates the necessity of direct contact between transducer and test cylinder. The use of high frequency and small crystals (about 5/8-inch square) results in a narrow beam of energy which permits the detection of flaws covering only small areas.

The equipment is arranged to read transmitted amplitude as per cent of the value obtained when the beam passes directly through the oil with no test material in place. Paper laminates of good quality up to 1 inch in thickness made with a resin binder show transmissions in the range of 20 to 50 per cent of that for oil alone. Where there are any cracks or openings, however, the transmission usually falls to a value below 1 per cent. The cylinder may be scanned by dropping it 1 inch at a time and rotating it 1 revolution at each stop.

In order to establish the significance of the ultrasonic readings for dielectric materials, tests were made comparing dielectric strength with ultrasonic transmission. One-inch-long sections were cut from rejected stop tubes at interesting points where a careful ultrasonic check had been made. The resulting ring-shaped sections were given a 60-cycle voltage breakdown test between 12-inch diameter parallel plate electrodes in transil oil at room temperature with voltage increasing at the rate of 3 kv per second. A uniform stress was applied parallel to the laminations of the sample so as to induce breakdown along any openings between laminations. The end surfaces of the sample were given a thin coat of grease to exclude oil from such openings and maintain the conditions under which the ultrasonic test was made.

The ultrasonic transmission values were found to correlate well with both location and voltage level of the electrical failure. When the samples failed through the interior, it was usually in the region where the ultrasonic transmission was lowest. In Figure 8 the voltage at breakdown is plotted on a linear scale against the minimum value of ultrasonic transmission observed for each sample and plotted on a logarithmic scale. Most of the experimental points fall close to a curve which represents a constant dielectric strength of 53 kv rms per inch for ultrasonic transmission values in the range of 0.005 per cent to 1 per cent and then rises to about 110 kv per inch. At transmission readings around 10 per cent and above, the strength of the material is actually higher than that shown because the samples failed by flashover along the surface instead of through the interior.

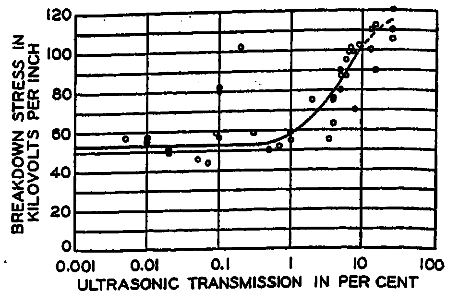


Figure 8. Experimental correlation between ultrasonic transmission through laminated stop tubes and electrical strength for breakdown parallel to the lamination

The flat portion of the curve corresponding to very low transmission values represents breakdown through the air in an interior crack. The voltage of 53 kv per inch agrees with Peek's values[3] for breakdown in air between large spheres 1 inch apart. Those experimental points which are far from the curve probably represent samples in which the crack was not continuous from one side to the other.

It is evident that the ultrasonic transmission test can be very valuable as an indication of the dielectric strength of certain materials. The equipment is rather simple and involves no high-voltage hazard. The test is nondestructive and gives a localized reading for every region of a sample. Its principal disadvantage is that its application and interpretation are simple only for samples of simple shape such as cylinders and plate stock. For more complicated shapes the test may be impractical.

Conclusion

The foregoing discussion has indicated some of the mechanical problems of high-voltage high-pressure design of cable accessories and certain electrical problems which arise from the mechanical requirements and from the high voltage. Some of the laboratory techniques which help to solve the electrical problems were seen to be: the electrolytic field-analyzer which helps the engineer to design apparatus with the lowest possible electrical stresses on the insulation and gives quantitative information as to the actual stresses that will exist in the completed equipment; and the ultrasonic transmission test which provides a sensitive indication of the presence of flaws that would affect the dielectric strength of solid insulating materials.

It is felt that the engineer must be prepared to take advantage of such modern techniques in order to build high-voltage cable accessories which will be reliable and economical.

References

1. Soldered Ceramic-to-Metal Seals, A. L. Jenny. *Product Engineering* (New York, N. Y.), December 1947, pages 154–57.

2. Ultrasonics (book), Benson Carlin. McGraw-Hill Book Company, New York, N. Y., 1949.

3. Dielectric Phenomena in High Voltage Engineering (book), F. W. Peek. McGraw-Hill Book Company, New York, N. Y., third edition, table XXIII, 1929, page 121.

Discussion

L. I. Komives (The Detroit Edison Company, Detroit, Mich.): The methods proposed by the authors to test high-voltage cable accessories are very interesting and should be very useful. Both the electrolytic field analyzer and the ultrasonic transmission test should help the terminal designer in designing trouble-free terminals for higher voltage cables. The following questions were brought up in connection with this paper:

1. In Figure 1 a very interesting spring-loaded gasketing *F* is shown. Why not carry this idea further and have the porcelain itself supported by spring loading to carry high pressure inside of the porcelain? Such terminals have been used by The Detroit Edison Company experimentally for four or five years and in actual service for nearly a year.

2. In text and in Figure 2 when discussing potheads for low-pressure systems, the authors mention that "There are no gaskets in the complete pothead assembly, therefore there can be no leaks in service from that source." This is a very admirable construction and we like this feature very much.

As a matter of fact, in Detroit we apply this same principle to the high-pressure cable systems and we eliminate gaskets shown halfway between *B* and *A*. The two other gaskets shown at *B* and at the top of the terminal under *F* are spring loaded. We would like to know how does one assemble the pothead shown in Figure 2? Does the wiping sleeve slide over the stress cone? If so, should not the opening in the wiping sleeve be larger? This is not quite clear from the drawing and description.

3. We are very interested in the ultrasonic transmission problem. It is felt that in order to be useful, ultrasonic transmission values should be obtained or established transwise (not radially and not exactly longitudinally) across the plastic tubes. We also feel that such tests should be made on the complete stop tube assembly ready to use, not on the plastic tube alone, as we have had sad experiences with such constructions where weaknesses did not show up until the stop tube was completely assembled.

4. In Figure 5 the constant stress lines showing an increase in the oil duct would indicate that the authors are right when they say that a better distribution is obtained if one breaks this area up into a number of smaller ducts. We are wondering whether one would have to make these ducts so small as to impede oil flow in order to produce a decided decrease in stress.

E. R. Thomas (Consolidated Edison Company of N. Y., Inc., New York, N. Y.): The use of electrodes in electrolytes has been used by other experimenters to map the equivalent electric fields in the dielectric for cable and joint configuration where only a single permittivity of dielectric was being considered. The authors are to be complimented on extending this technique to explore equivalent electrical stresses in complex insulation made up of different permittivities. The authors state a model is constructed using electrolytes of different depths or conductivities to represent the different dielectrics. It is obvious that different depths of the same unit conductivity of electrolyte could represent differences in permittivity, but it is not very clear how use can be made of different unit conductivities of electrolyte to represent different permittivities, as this would seem to introduce retaining boundaries. The resistivity of these boundaries would affect the measurements. I would appreciate if the authors would more fully explain the details of the actual laboratory equipment utilized.

The measurement of ultrasonic transmission through laminated insulation to determine the presence or absence of voids seems to be a very useful method of nondestructive testing. For the record, it would seem desirable to indicate whether the percentage indications on the output meter are in terms of the percentage energy transmitted, or whether it reads in percentage of amplitude of the wave transmitted. The statement that laminates of good quality showed transmission ranges of from 20 to 50 per cent of that for oil alone seems to be a rather broad range of values. Is it not true that the percentage of transmission through a particular laminate will be a function of its thickness compared to the length of the path through oil, as well as the relative density of the laminate compared to the density of oil? This latter would to a large extent determine the amount of energy which would be reflected at the oil-laminate boundary. In order to equate this mechanical measurement, it would seem desirable to include the terms regarding density of the material under test and the length of paths through oil and the material under test.

E. J. Merrell (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): Pothead design presumably is done within the framework of the AIEE Standard for Potheads.[1]

I should like to ask Mr. Bosworth and Mr. Farr whether, in view of their findings, the test voltages included in this standard are applicable without reservation to pressure-tube type terminals. The voltages specified, it seems, might introduce maximum tangential stresses in such terminal structures of a magnitude greatly in excess of those found in low-pressure designs without pressure tubes, possibly of an order closely approaching the intrinsic dielectric strength of the materials employed.

We have been unable to find specific information relative to the safe direct voltage which may be applied to potheads during routine field tests on cable. I wonder if the authors could enlighten us. Also, would d-c testing of potheads perhaps possess an advantage over customary a-c tests?

[1] Standards for Potheads. AIEE Standard Number 48, January 1948.

E. H. Grosser and H. L. Pedley, (Commonwealth Edison Company, Chicago, Ill.): This paper presents certain new features in the design of high-voltage cable joints and terminals, together with the laboratory methods for establishing the validity of the new designs. We are interested primarily in the application of these features to the design of joints for low-pressure oil-filled cable.

In considering the stop joint design, we believe that the use of the conducting material applied to the surface of the paper, which is subsequently formed into the paper tube, is a novel and apparently workable method of controlling the ground potential. It also permits fastenings for the clamping ring to extend into the end of the stop tube without distorting the electric field.

The study of closely spaced pressboard spacers between tape and stop tube shows that improved control of stresses in this critical region is possible. It may increase the difficulty of assembling the tube over the cable end on large conductors.

The new oil-filled stop joints with cylindrical stop tubes are longer and larger in diameter than stop joints previously made with the conical tubes. While the joint is easier to tape uniformly, the cost of the manhole to accommodate these larger joints is increased.

In the low-pressure pothead illustrated in Figure 2, the soldering of the solder-sealed top cap directly to the terminal stud as shown entails a large amount of heating, particularly for studs on large conductors, with the consequent risk of damaging the solder seal at the top of the procelain. We have such troubles on potheads which were similarly solder-sealed between stud and cap. Perhaps this trouble would not occur in this design with the thinner and longer spun cap.

C. S. Schifreen (Philadelphia Electric Company, Philadelphia, Pa.): This paper is a very instructive disclosure of the types of techniques so far developed as aids for proving the designs and constructions of joints and potheads for high-voltage cable systems. The ultrasonic test goes farther than the electrolytic field analyzer since the former permits a quality check of the finished manufactured component while the latter is at best an auxiliary tool to assist in preliminary design. The paper, unfortunately, is very vague in its reference to the technical features of the field analyzer. From the authors' example summarized in Table I, however, it does appear possible to use an analog at extremely low voltages to analyze stresses at levels many times higher than the values used in the analyzer itself. It would be most instructive to see the results of similar comparisons between impulse breakdown tests and stresses based on the analyzer when applied to potheads and stop joints in the same voltage class but in which conducting materials were incorporated in the stop tube to produce a built-in shield.

It is desirable, of course, to secure the most economical cable installations consistent with reliable performance. There is danger, however, that the manufacturer's effort to design for the "smallest possible dimensions" may encroach upon essential factors of safety which generally are related to the length of the joint. Never in my experience has the length of the manhole been a design limit of any consequence. Accordingly, I suggest that the manufacturer determine the dimensions needed to provide electrical strength at least equal to that of the cable. The application engineer then will provide the required structure. After all, the cost of the joint and pothead materials and manholes is only about 5 per cent of the installed cost of the cable system.

P. N. Bosworth and H. K. Farr: The discussions presented indicate sincere thought and interest in the problems of accessory design and manufacture. Like the paper, the discussions naturally divide themselves between considerations of design and of laboratory techniques. This closure will deal with the various points in that order.

D-c testing of power cable has been used for many years and has proved valuable in uncovering high resistance incipient faults before actual failure occurs under alternating voltage. In a cable the stress is essentially radial so that a direct voltage will search the fault without the disadvantages of large capacity effects experienced with alternating voltage. The situation in a joint or pothead is entirely different. The important consideration in these devices is the field distribution due to capacitive voltage division. The direct-voltage pattern is one of a pure resistance divider and does not stress the device in a manner similar to alternating voltage. For this reason a d-c test is not as realistic in a joint or pothead as it is in a cable.

It is, therefore, difficult to assign definite values of test voltage to a device without first carefully taking account of all influential factors in the design with seasoned judgment.

In joints or potheads utilizing annular fluid duct spaces for fluid flow, definite improvement in electrical strength can be realized if these channels are kept small as indicated by Figure 6 in the paper. However, for a construction of the type shown in Figures 4 and 5, the improvement cannot be more than 12 per cent. The correct design is in the direction of the smallest fluid space consistent with adequate fluid flow. Assembly will present no problem if the proper design is made.

Solder seals are the future goal of progressive designers. There is no reason why they cannot be applied to large devices after the technique and field experience has been gained on the smaller equipments. Figure 2 is an example of one application by one manufacturer on a line of potheads through 69 kv. For assembly, the stud is first soldered or pressure-attached to the cable end. The wipe sleeve containing the treating fitting then is passed over the cable end and wiped to its proper position on the cable sheath. After the cable end has been reinforced, the procelain assembly is lowered on to the wipe sleeve and two soldering operations are performed, one to the wipe sleeve at the bottom and one to the stud at the top. The parts are so proportioned that these soldering operations do not endanger the solder seals to the procelain if usual care is taken to protect them.

Center clamping of procelain parts to gasketing surfaces has many advantages worth considering. Much work is being done at present in this and other directions to produce a suitable high-pressure high-voltage pothead that is economical and simple to assemble in the field.

A detailed discussion of the technique of the electrolytic field analyzer falls outside the scope of the present paper. Another paper covering this subject is planned for future presentation.

A method of accurately representing controlled resistance material in the model has not yet been developed. Shields made of conducting materials incorporated in the stop tubes are, of course, represented in the electrolytic model as full conductors. Design *D* in Table I does include a test ground circuit which is carried through the stop tubes by means of a conducting impregnant. The use of ultrasonic vibrations as a test for the dielectric strength of resin-impregnated laminates is sufficiently new that much data is still needed. There would seem, however, to be little value in a test with transmission in a direction other than that normal to the laminations, because flaws never develop in the radial direction. Because of the complexity of the structure it is not possible at present to interpret transmission readings taken on complete joints or potheads.

The authors neglected to mention in the paper that the ultrasonic transmission values reported were expressed in terms of amplitude of the vibrations transmitted through the cylinder under test. The transmitted energy is, therefore, proportional to the square of these readings.

The data presented in connection with the ultrasonic transmission test apply only to material of one type and to structures whose shape and thickness fall within a certain range. Because of the effect of reflections at the surfaces and of losses in the interior of the material, additional data might be needed if the tests were to be extended to other shapes or materials. In spite of rather wide variations in the transmission through normal materials the test is significant because a delaminated area usually results in such a large change in transmission as to be unmistakable.

The authors wish to thank those presenting discussions for their interest and for the useful suggestions made.

A Theoretical and Practical Approach to the Design of High-Voltage Cable Joints

H. D. SHORT
MEMBER AIEE

THE design of high-voltage cable joints on a theoretically rigorous basis has not hitherto been published in a full or comprehensive manner, as a search of the literature on the science of cable making reveals. In contradistinction there exists a wealth of recorded information by many eminent authorities on the theory, design, behavior, and performance in duty of high-voltage cables per se. Indeed it would seem, at least for the lower working voltages, that the designs of cable joints have not always been granted the consolatory devices of scientific theory; and that precedent and empirical rule and rote have in some respects governed their construction and usage.

As an example, there is today a great deal of well-deserved consideration given to the breakdown strength of cables under internal transient surges and assimilated atmospheric transient surges (more commonly referred to as impulse tests). But the effects of a joint on the isotropic nature of the distributed transmission characteristics of the cable system have not received much attention. Notwithstanding this fact, most cable engineers are well aware of the influence of a discontinuity on the propagation of electromagnetic waves; and of how reflections do occur at these discontinuities, which are really localities having innate characteristics at variance with those of the main portion of the line.

A change in the over-all attenuation constant of the cable line at such a juncture consequent upon the joint having, let us say, a smaller capacity (owing to its diameter being greater than that of the adjacent cable) could alter not only the transmission characteristics but also the current rating.[7] Just how important or how great are the magnitudes of these phenomena would have to be ascertained from a mathematical investigation of the general case, and the adaptation of the related results to particular events.

To the author's knowledge no one has put these matters under observation in investigating the behavior of joints in service, or their impulse voltage breakdown strength properties. Nonetheless a study in these directions has by intent been omitted; leaving it, as is probably best, to be dealt with at some later time by way of a separate contribution to the whole question.

The more immediate phase of the subject is a formal design method for the joint proper and in this respect the present paper is more distinctly concerned. And in being so concerned the author has not overlooked the fact that sensory preceptions and old customs are sometimes better than logical reasoning. A rigid analytical approach has been followed and as a consequence formulas have been developed for several types of joints. While no claim is made for the originality of most of the formulas, two of them (for example, that for a joint design composed of heterogeneous materials), are novel to the art and have been found useful in practical applications.

Symbols

d = diameter over the conductor
d_f = diameter over ferrule (assumed herein $>d$)
$r = d/2$
$r_f = d_f/2$
D_t = diameter over cable insulant
D_j = diameter over joint insulant
$R_j = D_j/2$
$R_t = D_t/2$
k = dielectric constant, with subscripts to correspond to the different dielectrics referred to
E = operating voltage to neutral, or radial potential drop
C = electrostatic capacity, with subscripts to correspond to the different dielectrics referred to
g_c = radial stress at surface of conductor in the cable
g_p = longitudinal stress along the stress control cone profile
g_f = radial stress at surface of ferrule
g_t = longitudinal stress along the stepping profile

The Design Problem

The jointing of a single-conductor paper-insulated cable presents the simplest introduction to the problem while at the same time distinctly exposing the principal characteristics of design to be studied. These characteristics are fundamental and germane to all types of joints; and form the criterions of successful performance in duty. We will therefore, in what follows, confine our thinking to the single-conductor joint.

A cable joint is made up of three elemental components. First, there is the ferrule (or connector) which joins together electrically and mechanically the conductors of the two cable ends, see Figure 1. Secondly, there is the tapering down of the insulant on each cable (called

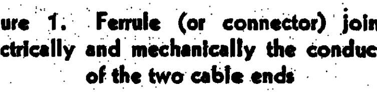


Figure 1. Ferrule (or connector) joining electrically and mechanically the conductors of the two cable ends

---

Paper 49-267, recommended by the AIEE Insulated Conductors Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 18, 1949; made available for printing September 15, 1949.

H. D. SHORT is with the Canada Wire and Cable Company, Toronto, Ontario, Canada.

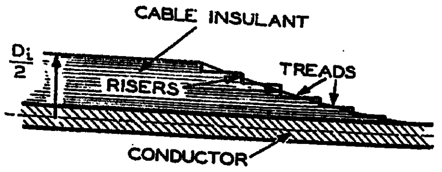


Figure 2. The "stepping" of the cable insulant of one of the cable ends in the joint adjacent to the ferrule

the cable insulant) to some predetermined dimensions. This is called the "stepping" of the insulant; a term derived from the usual practice, with an insulant composed of helically-wrapped tapes, of achieving the taper by removing individual tapes to form (in the plane of this piece of paper) a set of steps having "risers" and "treads" from the level of the conductor surface to that of the cable insulant surface, see Figure 2. In applying the insulant over the ferrule (called the joint insulant) it is paramount that it be well blended into the cable insulant in order that the over-all distribution of the system insulant is maintained as homogeneous as possible; stepping of the cable insulant is one of the most common ways of accomplishing this result. One other scheme[3,2] has been employed abroad but to the author's knowledge has not been followed here; its main feature emphasizes the blending of the cable and joint insulants.

Some degree of discontinuity must, of course, occur along the stepping (and also at the ends of the ferrule, on the assumption that $d_f > d$), distorting the otherwise uniform radial electric field between the conductor and its zero potential electrode (or the ground electrode of the field). As a consequence, the electric field is no longer everywhere radial (as would obtain at the center of two concentric cylinders) but has components some of which are longitudinal to the cable axis and therefore parallel to the plane of the paper tapes (that is, to the plane in which the paper fibers lie). This brings to bear the fact that the long-time voltage breakdown strength of impregnated papers in cable form, normal to the paper, and at atmospheric pressure, is 15 times greater than that in the longitudinal plane of the paper.[3,5] Thus it becomes a desideratum to so portion the stepping that the longitudinal component of the electric field (called the longitudinal stress) along the treads (and at the ferrule ends) is well below the 1/15th value. Under transient surge voltages this same failing in regard to longitudinal stress is exhibited,[3,6] so that from the point of view of assimilated transient surges (impulse voltage strength) the longitudinal stress, particularly along the treads (g_l), must not be overlooked (as Brazier, Halperin, and Shanklin have shown).[3,6]

In the third place, but by no means the least, comes the stress control cone, see Figure 3. Jointing is almost always carried on in the field which, together with the foregoing stress consideration in respect of the weakness of the tapes in the longitudinal direction, necessitates the building up of the joint insulant to some diameter greater than that of the cable insulant. The well-known exponential distribution of the electric stress between two concentric cylinders illustrates the point to those familiar with the proposals in hand. Additionally it may be stated that tapes hand-applied in the field are not susceptible to being wrapped with the tension and degree of uniformity feasible under cable-making practice. The resultant joint insulant diameter makes necessary a transition between this diameter and the cable insulant diameter as Figure 3 shows, the bounding curve of which is called the stress control cone. Here again, as along the stepping treads, the radial consistency of the electric field is disturbed and longitudinal stresses come into play. So once again they must be accommodated and limited to a value at which incipient deterioration[4] to the insulating tapes along and within the transitional boundary A-B, Figure 3, is amply prevented. In all high-voltage engineering it is an ineluctable rule that ionization must not be allowed to become malignant.

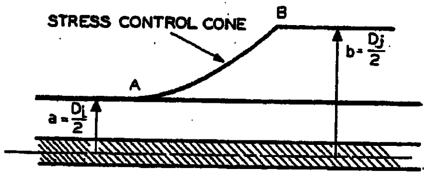


Figure 3. The stress control cone profile (the transitional bounding curve between the cable insulant diameter and the joint insulant diameter)

Having arrived at a conception of the physical nature of the problem, it remains to determine the proper profile and relative dimensions of the two transitional boundaries (or curvatures), namely, the stepping and the stress control cone. As in both instances the longitudinal stress is the criterion, then the corollary follows that these profiles will be functions of the longitudinal stress along them. It, of course, will be comprehended readily that any longitudinal stress within the confines of these profiles must, from the laws of electrostatics, be less than that at any point lying on the profile.

The Development of the Formulas for the Longitudinal Stress Along the Stress Control Cone

In Figure 4 there is depicted one-half of a joint, symmetrical about its (transverse) center-line axis. The electric field throughout the transitional curve A-B of the zero potential electrode (or ground electrode of the field) in passing from the cable insulant diameter D_i to the joint insulant diameter D_j is in reality three-dimensional. However, it is a very difficult matter to handle the three dimensions of this field relation simultaneously as extreme rigor would demand. By taking any two dimensions of the field together at one time renders a resolution of the electric field to be more readily attained, and leads to sufficiently accurate results for all practical purposes. Then in any plane containing the longitudinal axis of the joint, the electric field may be ascertained by the ordinary exponential formulas that obtain from a consideration of the radial attributes of concentric circles in a second plane normal to the former. Two particular cases, in respect of the stress control cone, now present themselves, which collectively represent all the variables to be encountered in this phase of the design problem. These two propositions are treated separately as the homogeneous case and the heterogeneous case; and in such the meaning of the chosen terminology is clearly revealed.

The Homogeneous Case

Under the predetermined conditions of homogeneity it is implied that the cable insulant and the joint insulant are of identical materials, and have, therefore, the same dielectric constant. A cross section of the joint in the plane containing the longitudinal axis of the joint is seen in Figure 4 relative to the arbitrarily

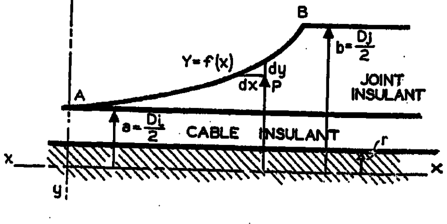


Figure 4. One half of a joint proper symmetrical about its longitudinal and transverse axes (minus the stepping), having identical materials for the joint insulant and the cable insulant—the homogeneous case

chosen x, y axis of the Cartesian co-ordinate system. In this picture the stepping of the cable insulant is omitted, as it is to be assumed that the stress control cone does not overlap the cable insulant; which, at least theoretically, is the right order of things.

The conductor will be at some potential relative to the zero potential electrode, and hence the radial potential drop between the conductor and the zero potential electrode will be the same value throughout the joint and the cable. Then we may proceed as in the following if the profile surface of the stress control cone is maintained at zero potential, by means of the usual shielding; the profile surface thereby becoming a part of the zero potential electrode.

Let

E = the radial potential drop between the conductor and the zero potential electrode

k = dielectric constant of the cable and joint insulant

At the point A in Figure 4

g_c = the radial stress at the conductor surface in the cable.

$$=\frac{E}{r\log_e\frac{D_t}{d}}\text{ volts per unit of length (radially through the insulant)}$$

Point P of Figure 4 is any point close to the transitional curve A-B within the joint insulant. A cross section of the stress control cone through this point in the plane normal to that containing the joint longitudinal axis is shown in Figure 5.

C_1 = capacity of layer 1, that is between the conductor and the cylindrical surface through the point P

$$=\frac{k}{2\log_e\frac{y}{r}}\text{ statfarads per wire unit of length (or to neutral)}$$

C_2 = capacity of layer 2, that is, between the cylindrical surface through the point P and the cylindrical surface of the stress control cone profile

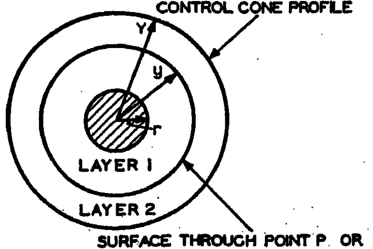

SURFACE OF STRESS CONTROL CONE PROFILE
Y
y
r
LAYER 1
LAYER 2
SURFACE THROUGH POINT P OR THE CIRCLE ON WHICH P LIES

Figure 5. A cross section of the stress control cone (in the plane normal to the cable or joint longitudinal axis) through the point P shown in Figure 4

$$=\frac{k}{2\log_e\frac{Y}{y}}\text{ statfarads per wire unit of length (or to neutral)}$$

It can be shown that the potential drop across layer 2 is some portion of E the magnitude of which is inversely proportional to its capacity and the reciprocal sum of both capacities C_1 and C_2. That is

$$E_2=\frac{1/C_2}{1/C_1+1/C_2}E$$

$$=\frac{1/k\log_e Y/y}{1/k\left(\log_e\frac{y}{r}+\log_e\frac{Y}{y}\right)}$$

$$=\frac{\log_e Y/y}{\log_e Y/r}E$$

If P is infinitely close to the stress control cone profile then g_p = longitudinal stress at the point P along the elemental longitudinal distance $dx=dE_2/dx$ (that is, rate of change of E_2 with respect to x).

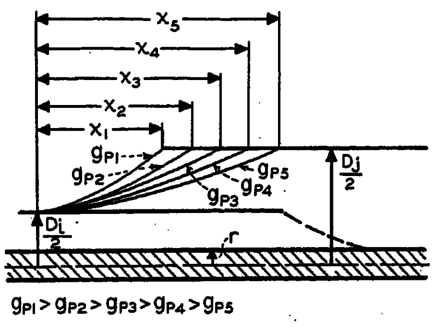


Figure 6. A family of stress control cone profiles for various values of g_p in arbitrary units; showing the practical significance of g_p in determining the over-all length of the joint

Hence we can write

$$g_p=\frac{dE_2}{dY}\frac{dY}{dx}$$

$$=\frac{d}{dY}\left(\frac{E\log_e Y/y}{\log_e Y/r}\right)\frac{dY}{dx}$$

$$=E\frac{d}{dY}\left(\frac{\log_e Y/y}{\log_e Y/r}\right)\frac{dY}{dx}$$

$$=E\left(\frac{\log_e Y/r\,\frac{d}{dY}(\log_e Y/y)-\log_e Y/y\,\frac{d}{dY}(\log_e Y/r)}{(\log_e Y/r)^2}\right)\frac{dY}{dx}$$

$$=\frac{E}{Y}\left(\frac{\log_e y/r}{(\log_e Y/r)^2}\right)\frac{dY}{dx}$$

Now if P actually lies on the profile

$y=Y$

and we have

$$g_p=\frac{E}{Y}\frac{1}{\log_e\frac{Y}{r}}\frac{dY}{dx}$$

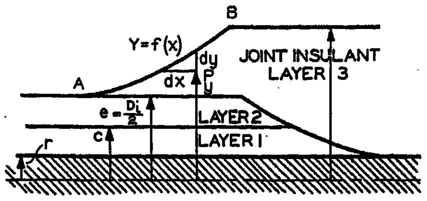


Figure 7. One half of a joint proper symmetrical about its longitudinal and transverse axes, having a variety of materials for the joint insulant and the cable insulant—the heterogeneous case

An integration of this expression between the limits of $D_i/2$ and Y yields the following equation for g_p

$$g_p=\frac{E}{x}\log_e\left(\frac{\log_e Y/r}{\log_e D_i/2r}\right)$$

Knowing r, D_t and E, and choosing a suitable value for g_p, the x and y co-ordinates of the stress control cone may be computed from this equation and its curve drawn to reveal the proper profile at the transitional region. The curve is, obviously, exponential and for different values of g_p a series of curves may be developed having A as the origin. Such a series is shown in Figure 6, from which it is evident that the lower the value of g_p the longer the stress control cone profile becomes and consequently the longer the joint must be. This is certainly a fact of practical significance.

HETEROGENEOUS CASE

Secondly under this heading we shall study the case where the cable insulant is graded with two layers of dielectric material having the usual difference in their dielectric constants, and the joint insulation is of a still foreign type of material, as shown in Figure 7. As in the preceding case it will be assumed that the stress control cone does not overlap the stepping of the cable insulant.

Again as in the section on the Homogeneous Case we have P as any point close to the transitional curve A-B, and

E = the radial potential drop between the conductor and the zero potential electrode

k_1 = dielectric constant of layer 1

k_2 = dielectric constant of layer 2

k_3 = dielectric constant of layer 3

C_1 = capacity of layer 1

$$=\frac{k_1}{2\log_e\frac{c}{r}}\text{ statfarads per wire unit of length or to neutral}$$

C_2 = capacity of layer 2

$$=\frac{k_2}{2\log_e\frac{R_t}{c}}\text{ statfarads per wire unit of length}$$

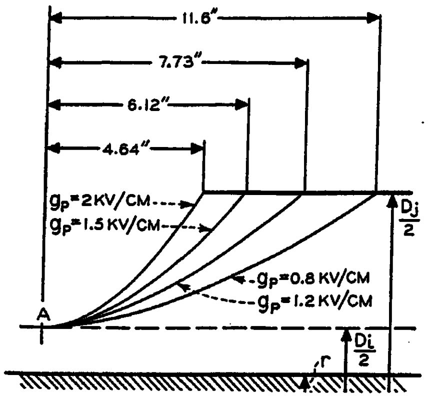


Figure 8. A family of stress control cone profiles for a 1-conductor 350,000-circular mil concentric-stranded paper-insulated lead-covered 69-kv grounded-neutral solid-type cable of conventional design having a graded paper insulant thickness of 0.750 inch and varnished cambric joint insulant in a direct buried installation (Winnipeg, Canada, direct buried installation)

C_3=capacity of layer 3

$$=\frac{k_3}{2\log_e \frac{y}{R_i}}\ \text{statfarads per wire unit of length}$$

C_4=capacity of layer 4 (the hypothetical layer above the point P)

$$=\frac{k_4}{2\log_e \frac{Y}{y}}\ \text{statfarads per wire unit of length}$$

E_4=potential drop across layer 4

$$=\frac{1/C_4}{1/C_1+1/C_2+1/C_3+1/C_4}E$$

Letting

$1/k_1=\gamma$
$1/k_2=\alpha$
$1/k_3=\beta$
and $k_3=k_4$

we have

$$E_4=\frac{\beta\log_e Y/y}{\gamma\log_e\frac{c}{r}+\alpha\log_e R_i+\beta\log_e\frac{Y}{R_i}}E$$

$$=\frac{\beta\log_e Y/y}{\log_e\frac{(c^{\gamma-\alpha}R_i^{\alpha-\beta}Y^{\beta})}{r^{\gamma}}}E$$

Now letting

$$\frac{1}{c^{\gamma-\alpha}R_i^{\alpha-\beta}}=\rho$$

we find

$$E_4=\frac{\beta\log_e Y/y}{\log_e\frac{Y^{\beta}}{\rho r^{\gamma}}}E$$

As in the preceding case we can write

$$g_p=\frac{dE_4}{dY}\frac{dY}{dx}$$

$$=\frac{d}{dY}\left(\beta E\frac{\log_e Y/y}{\log_e Y^{\beta}/\rho r^{\gamma}}\right)\frac{dY}{dx}$$

$$=E\frac{d}{dY}\left(\frac{\log_e (Y/y)^{\beta}}{\log_e Y^{\beta}/\rho r^{\gamma}}\right)\frac{dY}{dx}$$

$$=\frac{E\beta}{Y}\left(\frac{\log_e (y^{\beta}/\rho r^{\gamma})^{\beta}}{(\log_e Y^{\beta}/\rho r^{\gamma})^2}\right)\frac{dY}{dx}$$

if P actually lies on the profile y=Y and hence

$$g_p=\frac{E\beta}{Y}\ \frac{1}{\log_e Y^{\beta}/\rho r^{\gamma}}\ \frac{dY}{dx}$$

which upon rationalizing becomes

$$g_p=E\frac{1}{Y\log_e\frac{Y^{\beta}}{(\rho r^{\gamma})^{1/\beta}}}\frac{dY}{dx}$$

Integrating both sides of this expression yields

$$g_p=\frac{E}{x}\log_e\left(\frac{\log_e Y/(\rho r^{\gamma})^{1/\beta}}{\log_e R_i/(\rho r^{\gamma})^{1/\beta}}\right)$$

For the heterogeneous case the x and y co-ordinates of the stress control cone profile may be computed, from the above equation for any given g_p, to result in a practically uniform longitudinal stress along the profile. Figure 8 shows a family of such profiles with g_p assuming various values for a 69-kv single conductor solid-type cable joint having varnished cambric tapes as the joint insulant.

The Development of the Formulas for the Stepping of the Cable Insulant

In essence the cable insulant stepping is but a stress control cone in reverse as it were—this is logical because the physical aspects of the two propositions are alike in all respects. They do, nonetheless, differ in the magnitude of the dimensional quantities and of the stresses involved. Here one is confronted more directly with the stresses at the surface of the conductor and the ferrule; whereas in the case of the stress control cone one has to deal more with the stresses at the surface of the cable and joint insulants, which are however of much smaller consequence. Because of these circumstances the ferrule stress and the concomitant conductor stress, must be taken into account, as will be subsequently done.

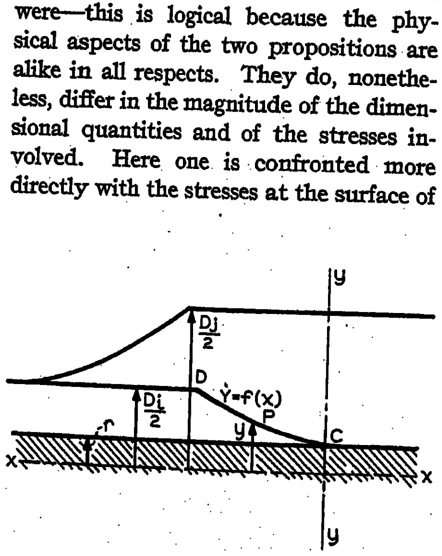


Figure 9. One half of a joint proper symmetrical about its longitudinal and transverse axes showing the transitional bounding curve *C-D* (the stepping profile) between the cable insulant diameter and the conductor diameter to which the actual stepping should conform within certain limits

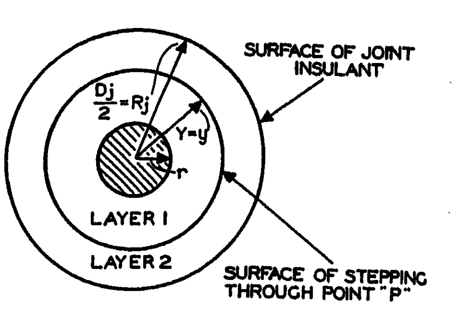

SURFACE OF JOINT INSULANT
Dj/2=Rj
Y=y
r
LAYER 1
LAYER 2
SURFACE OF STEPPING THROUGH POINT "P"

Figure 10. A cross section of the stepping profile (in the plane normal to the cable or joint longitudinal axis) through the point *P* shown on Figure 9

As before the three dimensional aspects of the electric field throughout the transitional curve *C-D* of Figure 9 in passing from the conductor diameter d to the cable insulant diameter D_i is, once again, determinable more readily by working with any two dimensions at a time. Figure 9 shows one-half of a symmetrical joint in the plane containing the longitudinal axis of the joint, wherein the cable insulant is considered to be of all the same grade of paper and thence homogeneous in respect of the dielectric constant. The material composing the joint insulant may however be such as to have the same or different dielectric constant as the cable insulant. That the presumption of a homogeneous cable insulant is warranted for all practical intents may be discerned in the following formula development. Here it will be observed that the radial potential drop between the stepping pattern and the zero potential electrode is the main determinant of the longitudinal stress along the treads. Grading of the cable insulant has no serious influence on this potential drop, as a fuller pursuit of the matter would disclose.

The conductor will be at some potential relative to the zero potential electrode (the ground potential of the field) and the radial potential drop between the conductor and the zero potential electrode is constant throughout the joint. Then with the surface of the joint insulant maintained at zero potential in accordance with the more usual practice (commonly referred to as full shielding of the joint, or a fully shielded joint) the following obtains.

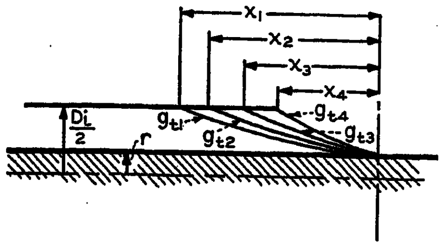


Figure 11. A family of stepping profiles for various values of g_t in arbitrary units showing the practical significance of g_t in determining the over-all length of the joint

E = radial potential drop between the conductor and the zero potential electrode
k_1 = dielectric constant of the cable insulant
k_2 = dielectric constant of the joint insulant
g_c = stress at the surface of the conductor in the joint

$$=\frac{E}{r\log_e\frac{D_j}{d}}\text{ volts per unit of length}$$

Point P in Figure 9 is any point on the transitional curve, C-D. A cross section of the stepping profile through this point, in the plane normal to that containing the joint longitudinal axis, is given in Figure 10.

k_1 = dielectric constant of cable insulant (and that of layer 1)
k_2 = dielectric constant of joint insulant (and that of layer 2)
E = potential drop between the conductor and the zero potential electrode
C_1 = capacity of layer 1

$$=\frac{k_1}{2\log_e\frac{Y}{r}}\text{ statfarads per wire unit of length or to neutral}$$

C_2 = capacity of layer 2

$$=\frac{k_2}{2\log_e\frac{R_j}{Y}}\text{ statfarads per wire unit of length or to neutral}$$

E_2 = potential drop across layer 2

$$=\frac{1/C_2}{1/C_1+1/C_2}E$$

$$=\frac{1/k_2\log_e R_j/Y}{1/k_1\log_e Y/r+1/k_2\log_e R_j/Y}E$$

THE HOMOGENEOUS CASE

Here the joint and cable insulants are identical materials and therefore have the same dielectric constant. Hence, $k_1=k_2$ and we can write

$$E_2=\frac{\log_e R_j/Y}{\log_e Y/r+\log_e R_j/Y}E$$

$$=\frac{\log_e R_j/Y}{\log_e\frac{Y}{r}\frac{R_j}{Y}}E$$

$$=\frac{\log_e R_j/Y}{\log_e\frac{R_j}{r}}E$$

Now

g_t = the longitudinal stress at the point P for the elemental distance dx, or the longitudinal stress along the elemental distance dx of the tread at P

$$=\frac{dE_2}{dx}$$

from which it follows that

$$g_t=\frac{dE_2}{dY}\frac{dY}{dx}$$

$$=\frac{d}{dY}\left(\frac{\log_e R_j/Y}{\log_e\frac{R_j}{r}}\right)\frac{dY}{dx}E$$

$$=\frac{E}{\log_e\frac{R_j}{r}}\frac{d}{dY}\left(\log_e\frac{R_j}{Y}\right)\frac{dY}{dx}$$

$$=\frac{E}{\log_e\frac{R_j}{r}}\frac{d}{dY}(\log_e R_j-\log_e Y)\frac{dY}{dx}$$

$$=\frac{E}{\log_e\frac{R_j}{r}}\left(-\frac{1}{Y}\frac{dY}{dY}\right)\frac{dY}{dx}$$

$$=\frac{-E}{\log_e\frac{R_j}{r}}\frac{1}{Y}\frac{dY}{dx}$$

integrating this expression between the limits Y and r yields

$$g_t=\frac{-E}{x\log_e\frac{R_j}{r}}\left(\log_e\frac{Y}{r}\right)$$

THE HETEROGENEOUS CASE

In this instance the joint and cable insulants are of different materials having unequal dielectric constants.

Letting

$1/k_1=\gamma$
$1/k_2=\alpha$

we may write

$$E_2=\frac{\alpha\log_e R_j/Y}{\gamma\log_e Y/r+\alpha\log_e R_j/Y}E$$

$$=\frac{\alpha\log_e R_j/Y}{\log_e\left(\frac{Y}{r}\right)^{\gamma}\gamma+\log_e\left(\frac{R_j}{Y}\right)^{\alpha}}E$$

$$=\frac{\alpha\log_e R_j/Y}{\log_e\left(\frac{Y}{r}\right)^{\gamma}\left(\frac{R_j}{Y}\right)^{\alpha}}E$$

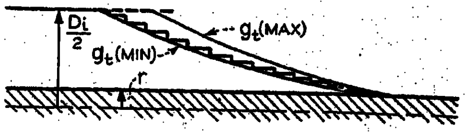


Figure 12. Stepping profiles for maximum and minimum values of g_t, showing a possible relationship to the actual stepping

$$=\frac{\alpha\log_e R_j/Y}{\log_e\frac{R_j^{\alpha}Y^{\gamma-\alpha}}{r^{\gamma}}}E$$

As in the preceding section above

$$g_t=\frac{dE_2}{dY}\frac{dY}{dx}$$

$$=\frac{d}{dY}\left(\frac{\alpha\log_e R_j/Y}{\log_e\frac{R_j^{\alpha}Y^{\gamma-\alpha}}{r^{\gamma}}}\right)E\frac{dY}{dx}$$

$$=\alpha E\log_e\left(\frac{R_j}{r}\right)^{-\gamma}\times\left(\frac{1}{Y}\frac{1}{\left(\frac{\log_e R_j^{\alpha}Y^{\gamma-\alpha}}{r^{\gamma}}\right)^2}\right)\frac{dY}{dx}$$

Now letting

$$\left(\frac{R_j}{r}\right)^{-\gamma}=\left(\frac{r}{R_j}\right)^{\gamma}=a$$

and

$$\frac{(R_j^{\alpha})}{(r^{\gamma})}=b$$

we may write

$$g_t=\alpha E\log_e a\left(\frac{1}{Y}\left(\frac{1}{\log_e b\;Y^{\gamma-\alpha}}\right)^2\right)\frac{dY}{dx}$$

integrating this expression between the limits Y and r yields

$$g_t=\frac{\alpha E\log_e a}{x(\gamma-\alpha)}\left(\frac{1}{\log_e br^{\gamma-\alpha}}-\frac{1}{\log_e bY^{\gamma-\alpha}}\right)$$

Using either of these equations for g_t, depending upon the conditions in hand, and by choosing suitable values for g_t a family of exponential curves (having C as origin) can be produced depicting several possible stepping profiles. Such a family of curves is shown in Figure 11 from which it is evident that the greater g_t is, the steeper the slope of the curve, and hence the shorter the stepping. This means that with higher values of g_t the joint length is reduced.

Now by electing a maximum and minimum value of g_t two curves will result, for any given joint design. Between these two curves the actual risers and treads can be disposed to produce the proper stepping. Such a procedure is shown in Figure 12, wherein it is seen tht the juncture of the risers and treads are points on the curve of minimum g_t. They, of course, do not need to be on this curve, but could be displaced somewhere between the two curves, or below the minimum curve. However, as the stresses at these points, in respect of impulse strength especially, are the most onerous, it would seem best to position them on or below the curve of minimum g_t.

The practical significance of this

method of determining the stepping profiles lies in the fact that a relatively constant longitudinal stress along the treads may be accomplished, and hence a shorter stepping profile employed than if designed upon other principles.

The Determination of the Joint Diameter

The diameter over the joint insulant (D_j) is a comparatively simple parameter to calculate if a value has been assigned to the stress at the surface of the ferrule, g_f. In terms of this value of g_f this diameter may be found from the following long established equations

$$g_f = \frac{E}{r_f \log_\epsilon \frac{D_j}{d_f}} \text{ volts per unit of length, (radially through the insulant)}$$

then

$$\log_\epsilon \frac{D_j}{d_f} = \frac{E}{r_f\, g_f}$$

Since r_f, d_f, g_f, and E are known the value of D_j may be calculated. To determine the proper value of g_f compatible with sound design is, however, a matter of some burden. For, as we have seen in the preceding discussions, the length of stress control cone profile, the length, and the shape of the stepping profile are all indirect functions of g_f.

Intermingled with these considerations is the radial impulse voltage time-to-breakdown strength characteristics of the joint which also must be taken into account in arriving at the proper value of g_f. Conventional practice seems to be based upon a value of g_f varying from 40 to 70 per cent of the stress at the surface of the conductor in the cable, g_c; the actual value depending upon the circumstances involved.

Having decided upon the proper g_f all other features of the joint design become readily computable by use of the formulas already developed herein.

References

1. Uber Abrundungen Bei Muffen Und Endverschlussen Metallisierter Kabel Und Die Hier Auftretende Langsbeanspruchung Der Geshichteten Isolation, F. Loebner. *Archiv fur Electrotechnik* (Berlin, Germany), volume 17, number 2, 1926.

2. Manufacture and Installation of a 33,000 Volt Type H.S.L. Submarine Cable, K. Takalashi. *International Conference on Large High Voltage Electric Systems* (Paris, France) volume II, 1931, page 505.

3. Joints, Sealing Ends and Accessories for Pressure Cable, L. G. Brazier. *Journal*, Institution of Electrical Engineers (London, England) volume 93, part II, October 1946.

4. The Breakdown Mechanism of Impregnated Paper Cables, D. M. Robinson. *Journal*, Institution of Electrical Engineers (London, England) volume 77, 1935, page 90.

5. Modern Supertension Cables, C. C. Barnes. *The Electrician* (London, England) July 18, 1947.

6. Impulse Strength of Insulated-Power-Cable Circuits, H. Halperin, G. B. Shanklin. *AIEE Transactions*, volume 63, 1944, pages 1190–97.

7. Investigation of High-Tension Cable Joints, E. W. Davis, G. J. Crowdes. *AIEE Transactions*, volume 44, 1925, pages 600–11.

8. Esterified Styrenated Insulation in the Power Field, T. R. Scott. *International Conference on Large High Voltage Electric Systems* (Paris, France) 1939.

9. The Jointing and Terminating of High Voltage Cables, W. Holttum. *Journal*, Institution of Electrical Engineers (London, England) volume 80, 1937, page 517.

10. Cable Terminations, D. B. Irving. *Journal*, Institution of Electrical Engineers (London, England) volume 92, part II, number 26, April 1945, page 73.

11. Some Developments in the Routine Pressure-Testing of High Voltage Cables, E. A. Beavis. *Journal*, Institution of Electrical Engineers (London, England) volume 66, 1928, page 1086.

12. La Technique Actuelle Des Transformateurs De Courant a Tres Haute Tension, C. Breson. *Revue Generale de l'Electricite*, volume 54, 1945, page 326.

13. Condenser Cones for Cable Testing, J. K. Webb. *Electrical Communication* (New York, N. Y.) volume 15, 1937, page 354.

14. Standardization of Impulse-Voltage Testing, T. E. Allibone, F. R. Perry. *Journal*, Institution of Electrical Engineers (London, England) volume 78, 1936, page 257.

15. The High-Pressure Gas-Filled Cable, C. J. Beaver, E. L. Davey. *Journal*, Institution of Electrical Engineers (London, England) volume 91, part II, 1944, page 35.

16. The Impulse Electric Strength of High-Voltage Cables, R. Davis. *Journal*, Institution of Electrical Engineers (London, England) volume 89, number 7, part II, February 1942.

17. A Problem in 33 Kv Jointing, W. Hill. *Proceedings*, Institution of Electrical Engineers (London, England) volume 96, part I, number 97, January 1949, page 25.

18. The Cleveland 66 Kv Cable Joint, H. L. Wallan. *Electrical World* (New York, N. Y.) volume 82, 1923, page 1058.

19. A New 132,000 Volt Cable Joint, D. M. Simmons. *AIEE Transactions*, volume 46, 1927, pages 240–47.

20. Joints in High Voltage Multi-Conductor Cable, T. F. Peterson. *AIEE Transactions*, volume 46, 1927, pages 963–74.

21. The 60,000 Volt Underground Network of the Union d'Electricite, E. Mercier. *Journal*, Institution of Electrical Engineers (London, England) volume 65, 1927, page 499.

22. Nomogram for the Calculation of Condenser Bushings, F. Cerovsky. *Elektrotechnicky Obsor* (Czechoslovakia) volume 30, number 16, August 1947, page 298.

23. Condenser Type of Insulation for High-Tension Terminals, A. B. Reynders, *AIEE Transactions*, volume XXVIII, part I, 1909, pages 209–68.

24. N.E.L.A. Underground Systems Reference Book. *Edison Electric Institute* (New York, N. Y.) Chapter V, Section EEE, 1931, page 143.

25. The Impulse Strength of High Tension Cable Installations, C. Held, H. W. Leichsenring. *International Conference on Large High Voltage Electric Systems* (Paris, France) Paper number 207, 1939.

Discussion

L. I. Komives (The Detroit Edison Company, Detroit, Mich.): Mr. Short's remark that there is a scarcity of impulse test data on joints is well taken. I would like to remind him that we have done considerable work in Detroit in this respect and this data was given to Mr. Halperin and Mr. Shanklin who presented it to the Institute in May 1944.[1] We hope that others who have similar data will come forward and tell us where this data is available.

In the paper, the author says that "reflections do occur at these discontinuities which are really localities having innate characteristics at variance with those of the main portion of the line." We think that joints can be made to resemble adjacent cable quite closely. We are using such joints in our 120—138-kv gas pressure cables as well as in duct splices made for lower potentials. In duct splices we use very thin connectors and build the insulation up to only slightly larger than the cable insulation and we know that Cleveland goes a step further and by brazing the copper conductor they get the diameter down to even less than we do. However, it must be kept in mind that the discontinuities which Mr. Short is talking about are much more important at higher potentials.

The connector diameter shown in Figure 1 is an example of the use of too large connectors. It seems that a revision of National Electric Light Association connectors should be made very soon in the light of more recent developments. We feel that sector connectors should be designed for mechanical forces rather than electrical properties (conductivity) and the same applies to round connectors. If there is a local heating due to insufficient conductivity across the connector, it is not as bad as figures would indicate because copper is a very good heat conductor and hot spots are dissipated to a large extent longitudinally.

The example shown in Figure 2 is an indication that a thickly insulated conductor should be stepped with a larger number of steps than shown on the example. We think that radial and longitudinal breakdown should be about even on tests, and for design purposes impulse testing can be used which shortens the time considerably. In order not to arrive at erroneous conclusions, one should take into account the experience of the General Cable Corporation as described by Mr. Atkinson and Dr. Simmons in a paper entitled Oil-Filled Cables and Accessories,[2] in which it is said: "As a result, tests involving both radial and longitudinal stresses are more likely to produce longitudinal failure when made at very high voltages for short time and radial failures when made as long-time tests at lower voltages."

The ratio between insulation thickness and longitudinal leakage distance in high-voltage joints in use at Detroit at the present time is 1/28.

Mr. Short mentions "that tapes hand-applied in the field are not susceptible to being wrapped with the tension and degree of uniformity feasible under cable making practice." When talking about hand-applied insulation not being equivalent to factory insulation, one must not forget the exposure to moisture during field operations which is quite as important as tension or degree of uniformity.

In Figure 3, the profile shown may be very good theoretically, but in practice a straight line from B to A is just as good and easier to obtain in field.

In practice one finds that the starting point of cable insulation tapering is usually under the stress control cone, not outside as shown in Figures 6 & 7. This could be shown in this manner so as not to be misleading, especially on Figure 6 where various lengths of stress control cone are shown.

Referring to Figure 12, approach to logarithmic profile may be achieved by the use of thinner tapes near the conductor and removing the same number of tapes for each step. The removal of the same number of tapes for each step may be quite a bit easier on the splicer. It is very difficult to specify diameters, especially with sector-shape conductors.

High-voltage joints with a value of g_f equal to 86 per cent of the stress at the surface of the conductor are giving satisfactory service.

REFERENCES

1. IMPULSE STRENGTH OF INSULATED-POWER-CABLE CIRCUITS, Herman Halperin, G. B. Shanklin. *AIEE Transactions*, volume 63, 1944, pages 1190–97.

2. OIL-FILLED CABLE AND ACCESSORIES, R. W. Atkinson, D. M. Simmons. *AIEE Transactions*, volume 50, December 1931, pages 1421–29.

E. R. Thomas (Consolidated Edison Company, New York, N. Y.): I am very glad that Mr. Short has raised the question of changes in surge impedance introduced by cable joints used on cable systems. This, to me, is a relatively important matter which has been overlooked to a large extent in the usual designs of high-voltage cable joints. I cannot agree with Mr. Atkinson that because the lengths of power cable usually used on 60-cycle systems are but a very small fraction of a wave length corresponding to that frequency, that the matter of discontinuity in surge impedance is of small importance. Mr. Atkinson's statement would be true if we were concerned with only steady-state operating conditions insofar as they would affect terminal conditions, but the propagation of an electric wave along a cable conductor is essentially at the same velocity whether you are talking about 60-cycle frequency or about many thousands of cycles of frequency. Therefore, discontinuity will always produce an electric field distortion at the point of discontinuity, and this will alter the stresses at this point to a value different than would be calculated by theory which neglects traveling waves. When we consider the superposition of transient waves traveling along the cable system due either to sudden changes in the values of current being carried or to sudden application of a voltage transient, then we deal with wave fronts having components of frequency a great many times higher than the normal power frequency. This is one of the reasons why many of the operating companies are turning more and more to the use of surge testing of components of the system as providing a better index of the suitability of that component to function under the operating conditions normally imposed on it in such a system.

It would seem to me axiomatic that in the design of any cable joint that if means are taken to preserve the surge impedance of the connection through the joint, a fundamentally better operating component will result.

A. L. McKean (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): Mr. Short's paper is a welcome contribution to the electric cable field and helps bridge the gap between experience and theory. The logarithmic formula which Mr. Short has amplified so extensively has been a basic design tool for many years in our own high-voltage research and development on cable joints and terminals, and we should like to testify briefly to its practical significance in such work.

It has been found, for example, that proper design, with careful attention to selection of gradient and length of stress cone, permitted the use of a thin wall terminal made of varnished cambric tape and Oil Stop compound, which may be slipped over the end of the cable, and used over and over again. These terminals have proved of special value in our long-time load cycle aging tests of 17-kv paper cable which we test at 57 kv over a period of time varying from a few weeks to a year. Before the logarithmic stress relief cone was adopted, a comparatively heavy or large diameter stress cone had consistently failed to withstand the severe stress concentrations incurred by poor basic design.

E. H. Grosser (Commonwealth Edison Company, Chicago, Ill.): The author presents an interesting analysis of longitudinal stresses in cable joints. Basically, he shows a means of controlling such stresses adjacent to the surface of the shielding cone by adjusting the shape of the cone to produce uniform longitudinal stresses. He thus arrives at a cone of exponential shape.

In actual practice, using ordinary construction methods with either varnished cambric or crepe-paper tapes, it is not feasible to construct stress cones having the desired theoretical shape. We arrive at either a single straight line slope or, at best, a so-called double slope. Recognizing this practical limitation it has been our practice in Chicago, in the design of high voltage cable accessories, to design for a known maximum value of longitudinal stress at the point where the stress cone begins, and to adjust the angle of the stress cone accordingly. This approach takes into consideration the probability that the weakest path longitudinally within a cable joint is the junction of the factory-applied cable insulation and the hand-applied tape. If this method of design is followed, it imposes a limitation on longitudinal stresses at the point where they are likely to do the most harm. Obviously, average longitudinal stresses also must be taken into consideration by proper dimensioning of the leakage distance.

A method is shown in the paper of arriving at uniform stresses along the steps by a control of the stepping profile. This appears to be a refinement which is not justified practically except in extreme cases. An exponential type of stepping was used on one of our 66,000-volt duct splices where a rigid control of diameter was necessary because of space limitations. Even here, the value of this refinement appears doubtful.

The British practice, referred to in the paper, of designing joints for radial stresses as high as 70 per cent of maximum radial stresses in cables, appears to be less conservative than American joint design practice. The latter, however, is based largely, if not almost wholly, on the use of varnished cambric tape for joints on paper-insulated cable, whereas the British practice refers to joints insulated with the same kind of paper tape that is used in the cable. It is possible, of course, that our joints can be operated satisfactorily under higher stresses than are customarily used. It appears that more data need to be accumulated on this subject.

R. W. Atkinson (General Cable Corporation, Bayonne, N. J.): The general method used by Mr. Short for calculating stresses in a joint and for determining its dimensions are simple and adequately accurate and have been effectively used in designing many of the high-voltage joints that are in service.

Mr. Short says that it is "assumed that the stress control cone does not overlap the cable insulant; which, at least theoretically, is the right order of things." Reading this statement in terms of the figures, it is understood that the thought is that the stress control cone should not overlap the stepping of the factory insulation. If correctly interpreted, this idea cannot be taken as generally true. Indeed, in many of the joints in service, the stress control cone does overlap the steps of the cable insulant by a major portion of its length. A requirement that there be no such overlap would greatly weaken, or lengthen, these joints.

Mr. Short quotes authorities giving a factor of 15 for the relative strength of tape insulation normally and longitudinally of its plane. For tapes hand-applied over stepped insulation, as shown in his designs, and in terms of average stress, this figure is quite acceptable. In terms of the strength of the factory insulation the value will be 30 or more. Thus, where full benefit is to be taken of taping methods which permit obtaining in the field-applied insulation a radial strength comparable to that of the factory insulation, the corresponding much higher ratio must be used in determining the permissible longitudinal stresses.

At the beginning of his paper Mr. Short introduces some incidental remarks indicating that electrical reflections may occur at a joint as a result of the discontinuity of the electrical properties at the joint. He says that the magnitude of such phenomena would have to be investigated from a mathematical standpoint. Fortunately this has already been done. The classic work of Professor M. I. Pupin[1,2] on the famous Pupin loading coils for telephone circuits showed that a circuit will pass a wave with no significant reflection providing the spacing between coils does not exceed about 10 per cent of the wave length. And, of course, the discontinuity due to having nearly all of the line inductance lumped in wound coils is very great compared with some local variation in capacitance per unit length. We are thus conservative in applying Pupin's data to a joint in a power line. The wave lengths to be considered are for the power frequency and for surges. At 60 cycles, the wave length is about 1,500 miles. For a standard surge we may take the wave front of $1^1/_2$ microseconds as approximately equivalent to a wave length of about 3 microseconds or 1,500 feet. Ten per cent of even this latter figure is 150 feet which is the length below which discontinuities would not cause reflections at frequencies significant in power work. It is thus completely adequate to do as the author has done, namely, to design the joint (very short compared with 150 feet) purely on the basis of electric stresses, radial and longitudinal. The discontinuities are important only as they affect these stresses, especially the latter.

To be sure, electrical discontinuities made in cable sheaths at joints for the purpose of controlling sheath losses may be troublesome for carrier current use. Also for a cable to be used for transmitting radar frequencies in the range of centimeter lengths, irregularities even in the internal structure of the joint would be significant. However, these things are another story and not related to the subject at hand.

The question has arisen many times concerning the possible effect of reflections in power cable joints and thus it seems desirable to point out with emphasis that this is not a practical problem in determining dimensions and insulation of such a joint.

References

1. Propagation of Long Electrical Waves, M. I. Pupin. *AIEE Transactions*, volume XVI, 1899, pages 93–142.

2. Wave Transmission over Non-Uniform Cables and Long Distance Air-Lines, M. I. Pupin. *AIEE Transactions*, volume XVII, 1900, pages 445–512.

H. D. Short: Mr. Komives has suggested the disclosure of impulse test data in relation to high-voltage cable joints. Such information would be extremely useful in joint design. I would like to see such data correlated specifically with the full physical dimensions and constructional details of the particular joint designs, including, for example, the stress control cone and stepping profiles and their relation to one another in the joint proper. To say that a joint withstood a certain test voltage either on an impulse test or a voltage-time-to-breakdown test does not mean very much to one concerned with high-voltage joint design unless the complete details of the joint's dimensions and construction are at hand.

I agree with Mr. Komives that it is possible to make joints which possess characteristics closely resembling those of the adjacent cable. However, I think this matter is worthy of more study and the disclosure of impulse data mentioned above would be of material help in these directions.

Figure 2 of the paper is intended only to illustrate what is meant by the term "stepping"; it has been drawn to an arbitrary scale and does not reveal the author's opinions in regard to the number of steps or the optimum dimensions of the risers and treads in any particular instance. The formula of g_t given in the paper clearly shows that the cable insulant thickness is one of the criterions of the stepping profile. Mr. Komives also refers to Figure 12, and I am completely in conformity with his thoughts in regard to the practice of removing an equal number of tapes at each step. Indeed, the use of short treads and risers of heights equal to the thickness of one tape (that is, one paper tape of the cable insulant) permits closer adherence to a given stepping profile as determined by the paper's formula for some specific value of g_t.

In reference to Mr. Komives' remarks concerning Figures 6 and 7, there is no question but that the stress control cone should not overlap the stepping profile from a theoretical aspect. As Mr. Komives says, very often in practice one does not find this theory accepted; however, this practice does not invalidate the theory nor a contention that it is better to avoid this overlapping in an actual joint. I am of the opinion that a more economical joint for any predetermined set of conditions can be made by not overlapping the stepping profile, but this can only be proved by test data on comparative designs and I know of no one who has such data. Glovers and Enfield[1,2] in England do not allow the stress control cone to overlap the stepping profile in their high-voltage designs, and there is at least one of the highest voltage installations in the United States wherein this practice is followed.

Mr. Komives cites a statement ascribed to Mr. Atkinson and Dr. Simmons. I am very glad that Mr. Komives has inserted this reference as it serves to corroborate some of the premises which prompted the publication of the paper. However, I think it should be stated here that it is not tractable with available data to infer that there exists an established or empirically authenticated relationship between short-time tests at high voltages and long-time tests at low voltages in respect of high-voltage joints. Mr. Komives mentions a ratio of longitudinal leakage distance to joint insulant thickness of 28. Assuming that the correct interpretation has been put on this ratio, it is difficult to accept such "in puris naturalibus" as a design criterion. It is nonetheless interesting information and no doubt based upon experience in service and test. I should like to know if it is related to any flashover value; for example, so many centimeters per kilovolt to neutral. The value of g_f equal to 86 per cent of g_c quoted by Mr. Komives appears to be much higher than is conventional here or abroad for high-voltage cable joints. But then any specific value of such must be interpreted in the light of the joint design, the type of joint insulant and degree of jointing skill employed, and last but by no means least, the desired factors of safety relative to atmospheric and internal surges. As pointed out in the paper, the determination of the proper or most suitable value of g_f is a matter of some burden. The factors involved are many and need more clarification and emphasis from experimental data; meanwhile, to achieve the impulse voltage breakdown strength usually dictated by present trends, a value more closely akin to 50 per cent of g_c seems to be indicated.

I am indeed grateful to Mr. Thomas for his remarks confirming my postulations in regard to the importance of the characteristic (or surge) impedance of joints introduced into a cable system. His able amplification of the thoughts I had in mind when preparing the paper is a most salutary contribution. I too find it difficult to agree entirely with Mr. Atkinson's discussion in these respects. Mr. Atkinson refers to the "classic work" of Professor Pupin; surely he is not overlooking the mathematical "locus classicus" of Oliver Heaviside.[3,4] He did the classic work which permitted Pupin to reduce to practice the distortionless line. It seldom, if ever, happens in a high-voltage cable line that the joints are as close as Mr. Atkinson says is required for the spacing of loading coils towards assimilating Pupin's uniform distribution of the inductance parameter. If what he says is correct, then his later deductions militate against his premise since joints are nearly always spaced more than 10 per cent of a wave length of 1,500 feet apart, and the length of the discontinuity is not significant in this argument. Indeed, using Mr. Atkinson's example and spacing the joints 375 to 400 feet apart results in a 1/4 wave length between joints which, of course, is an ideal condition for complete reflection. Simple calculations show, assuming a joint characteristic impedance twice that of the cable, a not too unrealistic value, that at a junction of a cable and joint and transmitted or refracted portion of the voltage wave could under the worst conditions be of the order of 30 per cent greater than the incident wave. In the case of a cable connected to an overhead line there is no question but that the cable will flatten the wave front and increase the peak value of the wave; and the shorter the cable the greater the peak value of the wave becomes in comparison to the incident wave.

In reference to Mr. Atkinson's mention of the relation of the ultimate breakdown strength of paper in the normal direction to that in the plane of the fibers, I am not too sure that I comprehend his meaning. If he means that the diameter of the joint insulant and the longitudinal stresses should be such as to allow greater factors of safety than obtain in the adjacent cable then I am in agreement with him; but I cannot categorically agree that the factors of safety in the joint should be twice that in the cable as circumstances alter cases. I must also take the opposite point of view to Mr. Atkinson when he says "a requirement that there be no such overlap would greatly weaken, or lengthen, these joints." My related remarks on Mr. Komives' discussion are appropriate here.

Mr. Grosser refers to their method of attaining the stress control cone profile using varnished cambric and crepe-paper joint insulation. With such hand-applied insulation it is not easy to arrive at the true exponential profile consequent upon some chosen value of g_p. Nonetheless it is quite feasible to very closely approach this boundary curve by means of a succession of straight lines; not more than three straight lines will most always reproduce very closely the exponential profile to within practical application, it being all too obvious how this can be achieved when one recalls that a curve is but a series of straight lines. Mr. McKean's experience is pertinent to this matter and emphasizes the author's opinions of the desirability of achieving as nearly as possible the theoretically correct control cone profile. With preformed preimpregnated paper rolls it is a simple matter to arrive at the actual exponential profile that the theory predicts. I am wondering if Mr. Grosser has any data to substantiate his reflection that the "exponential type of stepping" used on their 66-kv duct splices appears to doubtful value. Mr. Grosser opines that a value of g_f equal to 70 per cent of g_c appears to be less conservative than American design practice. This is interesting in view of Mr. Komives' remarks in these respects; I presume though that Mr. Grosser is not including the 66-kv duct splices he mentioned earlier. Undoubtedly the use of paper-joint insulation in contradistinction to varnished cambric permits the use of higher values of g_f and concomitantly smaller joint diameters; but here again more experimental data would seem worthwhile, as Mr. Grosser has suggested.

I appreciate Mr. McKean's remarks in substantiation of the practical significance of the work embodied in the paper.

REFERENCES

1. THE HIGH-PRESSURE GAS-FILLED CABLE, C. J. Beaver, E. L. Davey. *Journal*, Institution of Electrical Engineers (London, England), volume 91, part II, number 10, February 1944.

2. GAS COMPRESSION CABLE, F. W. Main. British Electrical Industry Overseas Manual (Benn Brothers, Ltd., London, England), 1949, page 61.

3. ELECTROMAGNETIC THEORY, Oliver Heaviside. Ernest Benn Ltd., London, England, volume 1 1893.

4. THE ELECTRICIAN, Oliver Heaviside. Ernest Benn Ltd., London, England, June 17, 1887.

Transient Temperature Phenomena of 3-Conductor Cables

F. O. WOLLASTON
MEMBER AIEE

TRANSIENT temperature phenomena are of fundamental importance in two fields of cable engineering, namely the calculation of short-time overload ratings and the design of oil-filled cable lines. Sufficient fundamental theory has been published to cover the subject quite adequately. However, much remains to be done in developing practical methods of applying rigorous solutions to common problems and in making the methods available to engineers generally. The solution of transient problems, although inherently difficult, is capable of much simplification through methods which are familiar in other technical fields. For example, in transmission line engineering, the laborious task of calculating the electrical performance of long lines has been greatly simplified by the production of accurate performance charts.

In the field of cable engineering, the calculation of steady-state ratings has received considerable attention and several convenient handbooks on the subject are available. Unfortunately similar work has not been undertaken for short-time ratings of cables and for oil demands of oil-filled cables. Consequently, very few utilities have engineers on their staffs who are capable of handling these problems. The oil demands of oil-filled cables might seem to be a subject of little interest to most utilities. However, a surprisingly large number of utilities have had occasion to consider seriously the installation of oil-filled lines. In such lines the size of the hollow core in the conductor, the number and location of stop joints, and the type and location of reservoirs are major factors in design which are dependent wholly or partially on the oil demand of the cable. Where engineering competence in oil-demand calculations is lacking, the utility must perforce rely entirely on the cable manufacturing companies to carry out its design studies. This situation must surely be rather unsatisfactory to the utility and also places a considerable burden on the engineering staff of the manufacturer.

Where engineering competence is lacking in the calculation of short-time ratings for cables, the utility cannot take full advantage of the substantial economies which can be achieved by the intelligent use of the short-time overload capabilities of underground lines. The overloads which cables can carry for one or two hours are frequently quite large and can be made use of in the same way that overload capabilities of such apparatus as transformers and rectifiers are utilized.

This paper describes the methods which the author and his co-workers have developed for the practical application of rigorous theory to the routine calculation of short-time overload ratings of solid-type and oil-filled cables installed in ducts and for the oil demands of Pirelli-type oil-filled cables installed in ducts. In a previous paper[1] a treatment of the problems of single-conductor cables was given. In the present paper a novel method of extending this treatment to the problems of 3-conductor cables will be given and the methods of making routine calculations will be described in detail. These methods have been applied to all the practical problems of this type which have occurred in the last 20 years in a large utility company operating one of the most extensive cable systems on the American continent. They have proved convenient and fully adequate. It is hoped that they will be of value to other engineers. The author has not had occasion to solve such problems for pipe-type cables or cables buried directly in the earth and the paper does not deal with them.

The particular methods to be described have certain advantages not found in other methods. A different solution for the problem of cable temperatures under variable loading has been published by E. Church.[2] This solution is rigorous but does not cover the problem of oil demands. An approximate solution for the problem of cable temperatures under variable loading has been published by W. B. Kirke.[3] It is not applicable to the problem of oil demands and has the further disadvantage of introducing certain approximations which are not present in rigorous solutions. A rigorous solution which covers both transient temperature problems and oil demand problems has been published by G. B. Shanklin and F. H. Buller[4] but convenient methods of making numerical computations have not been made available. The present method is rigorous, it is applicable to both transient temperature problems and oil demand problems and considerable simplification of computing procedure has been worked out. Although its use in transient temperature problems entails more computing labor than the approximate method noted in reference 3, the facility gained in handling oil-demand problems more than compensates for the extra effort.

Temperature Transient of 3-Conductor Cables

The first problem which will be dealt with is the temperature change of a 3-conductor cable produced by a change in loading. The ampere loading of a cable

---

Paper 49-268, recommended by the AIEE Insulated Conductors Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted May 25, 1949; made available for printing September 1, 1949.

F. O. Wollaston is with the British Columbia Electric Railway Company, Ltd., Vancouver, B. C., Canada.

The author wishes to express his appreciation to Mr. E. H. Grosser, Jr., for his assistance in calculating several of the temperature transients given in the paper and to Mr. L. R. Horne for his assistance in the preparation of the numerical example.

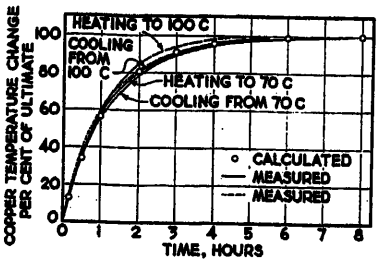


Figure 1. Attainment curve for 3-conductor type-H cable

Cable has 500,000-circular mil compack sector conductors, 125 mils insulation, 117 mils sheath

can change either abruptly or gradually. The abrupt change is easier to solve mathematically and is used here. Since it is heat loss, not loading, that controls the temperature, the problem is worked out in terms of losses. The losses are assumed to have been constant for sufficient time to allow the cable to reach a constant temperature. The loss then changes abruptly by one watt and produces a temperature transient.

This problem has been solved rigorously for single-conductor cables, as noted earlier. However, it has not been solved rigorously for 3-conductor cables, for the reason that no satisfactory mathematical formula for the thermal resistance of the insulation has been developed. It is therefore necessary to adopt the device of converting a 3-conductor cable into an equivalent single-conductor cable, after which the single-conductor solution can be applied. Formerly, the usual method of doing this was to treat each core of a 3-core cable as though it were a separate single-conductor cable. It was assumed that in shielded-type cables the heat in each core flowed uniformly in all directions from the conductor to the metallic shielding tape, which in turn conducted it with negligible thermal drop to the sheath. This permitted the calculation to be carried out as though only one phase of the cable were present, the thermal resistances of the sheath and duct being multiplied by three to adjust them to the single-phase basis. For want of a better method, the same procedure was proposed for belted cables, with the expectation that it would not be very accurate.

These assumptions were reviewed in 1942 when starting design calculations on the first 3-conductor oil-filled lines to be installed by the company with which the author was then associated. Common sense indicated that they were not true for belted cables with sector-shaped conductors. The insulation between conductors is relatively thin compared to the size of the conductors, since such cable is used only in the lower voltage classes. Thus it forms a thin sandwich between masses of metal which are at the same temperature. It seemed inconceivable that any appreciable amount of heat would flow into the insulation between the conductors. Accordingly it was concluded that the three conductors and the insulation between them could be considered as a sort of composite single conductor. This is the concept that is developed in this paper and is used as a means of converting 3-conductor cables into equivalent single-conductor cables.

That this concept was applicable to shielded-type solid cable with sector conductors was already known. Halperin[5] had originated it in connection with studies of the measured values of insulation thermal resistance of such cables. He found that the values measured in his tests were substantially higher than would be obtained by calculation using the usually accepted method, that is, the method of reference 6. He showed that the formula for single-conductor cables could be made to give acceptable results, provided an empirical correction was applied. The correction was less for cables with thin insulation and relatively large conductors of pronounced sector shape than for thicker insulation and more rounded conductors.

These results established the soundness of the basic idea of the composite single conductor. The idea could not be used for direct calculation of the insulation thermal resistance or of problems involving the insulation thermal resistance, since the proper correction factors to apply for various types and designs of cables had not been determined. However, it was seen that if the correct value of the insulation thermal resistance were already known, it could be used to calculate the diameter of an equivalent single conductor which would have the correct ultimate temperature rise. It seemed evident, too, that the calculated temperature transient of the equivalent cable would be very nearly correct. Even if the insulation thermal resistance were not accurately known, the best available value could be used. In that case the results would still be the best that the accuracy of the available data would permit. Trial calculations were made for several cables whose temperature transients were known from tests. An example for a shielded cable is given in Figure 1 and for a belted cable in Figure 2. The agreement between the calculated temperature transients and the measured transients is quite sufficient for all practical purposes. The results proved the value of the method and it was adopted as standard.

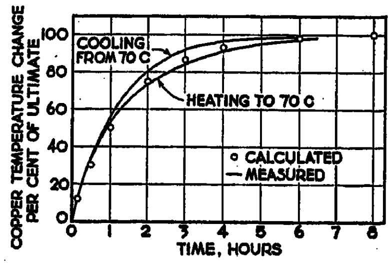


Figure 2. Attainment curve for 3-conductor belted cable

Cable has 500,000-circular mil standard stranded sector conductors, 141 mils conductor insulation, 78 mils belt insulation, 125 mils sheath

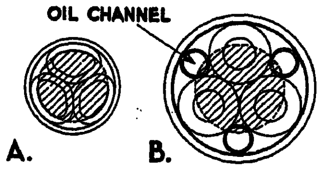


Figure 3. Equivalent single conductors for 3-conductor cables

Shaded areas delineate the equivalent single conductors

A. 500,000-circular mil type-H cable, compack sector conductors, 125 mils insulation, 117 mils sheath

B. 450,000-circular mil 69-kv oil-filled cable, compack round conductors, 315 mils insulation, 156 mils sheath

The author is indebted to the Commonwealth Edison Company for the test curves in Figures 1, 2, and 5. In Figure 5 the test data shown are averages. To bring out a point of interest, Figures 1 and 2 show details of certain individual test runs on cables 5 and 6 respectively of Figure 5. All tests were made with the cables installed in the open air of a testing laboratory. It is noticeable that the measured heating curves in Figures 1 and 2 do not coincide with the cooling curves, nor do the heating curves for the cable of Figure 1 coincide in the two test runs to different maximum temperatures. This is probably due to the variability in the thermal "constants" of each cable, particularly the thermal resistance of the sheaths. The sheath thermal resistance is known to decrease[3,5,7] as the dissipated losses increase. The losses being dissipated through the sheath are always less at the start of a heating transient than at the finish, even though the losses generated within the cable remain constant throughout. The reverse is true for a cooling transient. Consequently the sheath thermal resistance will vary during a test and the heating and cooling curves will usually not coincide. In theoretical

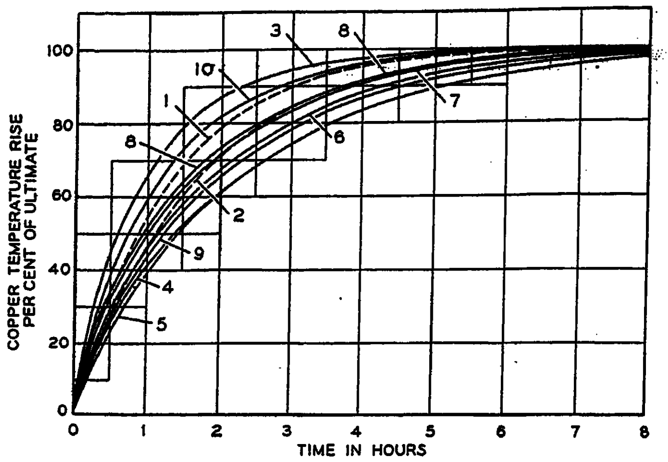


Figure 4. Calculated attainment factors for single-conductor cables

No.	Cond. Size, Cir. In.	Core Size, In.	Kv	Type	Nominal Thickness Mils Insul.	Nominal Thickness Mils Sheath	R_i
1	1.25	0.75	12	Ord; rope core	250	125	1.01
2	1.75	1.00	12	Ord; rope core	250	141	0.852
3	0.5	None	33	Ordinary	406	109	2.44
4	2.1	0.69	69	Oil-filled	315	141	0.855
5	2.1	None	69	Ord; segmental cond	688	169	1.86
6	1.0	None	69	Ordinary	750	141	2.60
7	0.75	None	69	Ordinary	750	141	2.88
7	0.6	0.69	132	Oil-filled	720	190*	2.48
7	0.6	0.50	132	Oil-filled	720	156	2.48
8	0.6	0.40	132	Oil-filled	720	250**	2.48
9	1.1	0.69	132	Oil-filled	720	156	2.0
10	0.25	0.69	132	Oil-filled	560	133	2.24

* Double. ** Fluted.

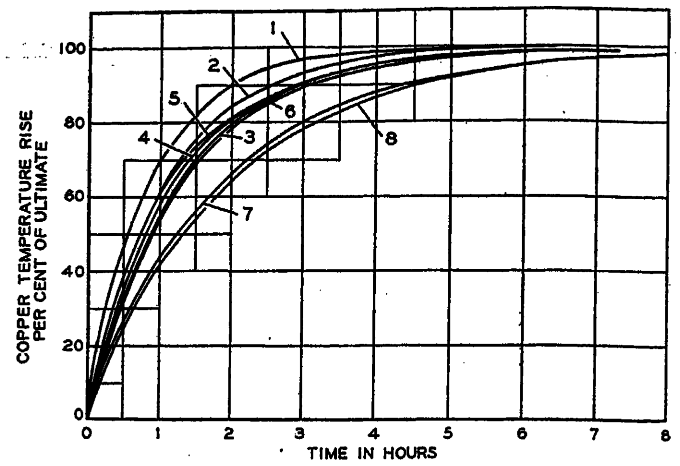


Figure 5. Attainment factors for 3-conductor cables

Curve No.	Cond. Size, Cir. In.	Stranding	Type of Cable	Nominal Thickness Mils Cond.	Nominal Thickness Mils Belt	Nominal Thickness Mils Sheath	R_i
1	#0 Awg	Compackround	"H" 12 kv	165		109	1.49
2	0.375	Standard sector	Belted, 5kv	78	47	109	0.56
3*	0.350	Compacksector	"H" experimental	110		109	0.535
4*	0.5	Compacksector	"H" experimental	90		109	0.569
5*	0.5	Compacksector	"H" experimental	125		117	0.587
6*	0.5	Compacksector	Belted 13 kv	141	78	125	1.11
7	0.4	Compackround	Oil-filled 69 kv	315		156	1.23
8	0.45	Compackround	Oil-filled 69 kv	315		156	1.195

*These attainment curves were determined by tests. Other attainment curves were calculated.

calculations the thermal constants of the cable are assumed to be truly constant and therefore the calculated heating and cooling curves are identical.

The desirability of having correct values of insulation thermal resistance deserves comment. As indicated by the tests reported in reference 5, part III, already referred to, the correctness of the values calculated by the usual method of reference 6 for shielded cables is open to question. The insulation thermal resistance is an essential factor in the determination of steady-state ratings as well as short-time ratings and oil-flow problems. It is suggested that the matter is important enough to justify obtaining demonstrably correct values by actual measurement. This might be done by adding a clause to the cable purchase specifications which would require the manufacturer to furnish the guaranteed value of the insulation thermal resistance. When sufficient background data had been established, it would be quite easy to estimate the values sufficiently closely for use in design studies where the cables have not been actually constructed.

It is not necessary that the periphery of the equivalent single conductor coincide with the periphery of the actual conductors. Two typical examples of equivalent single conductors are shown in Figure 3. In Figure 3(A) for a solid-type shielded cable the peripheries do coincide. In Figure 3(B) for a 66-kv oil-filled cable with round conductors they do not. This merely means that in the shielded cable example the actual heat flow is entirely from the backs of the conductors, whereas in the oil-filled cable example it is not. In both cases, however, the equivalent single-conductor method will give the correct result for the ultimate copper temperature rise above sheath, which is the most important consideration.

It also is necessary, of course, that the transient temperature rise be correct, or nearly so, throughout the transient. In the case of solid-type cable, either belted or shielded, it is obvious that if the periphery of the equivalent single conductor coincides with that of the actual conductors, then the actual heat flow must be the same as though the cable were a single-conductor cable. This usually is the case. The calculated transient will be correct provided the proper thermal constants for the cable are used. The thermal resistance of the equivalent single conductor is assumed to be negligible. The thermal capacity is calculated separately for the coppers and for the impregnated-paper insulation within the periphery of the equivalent single conductor. The sum of the two values is the total thermal capacity. The specific thermal capacity and the thermal resistivity p of the insulation also are required. The insulation is not uniform in composition due to the presence of the filler materials in the filler spaces. This nonuniformity is unimportant in solid-type cable since the thermal constants of the filler materials are the same as for the insulation. For modern solid-type cables p can usually be taken as 600 watts per degree centigrade per centimeter cube. The thermal resistance of the metal of the sheath is assumed to be negligible. The thermal capacity is calculated for the volume of lead in the sheath. If shielding tapes are present, the thermal capacity for the portions adjacent to the sheath can be added to that of the sheath if this refinement is thought desirable. The physical properties of cables are given in Table I.

APPLICATION OF METHOD TO OIL-FILLED CABLES

The determination of the physical constants of the equivalent single conductor for oil-filled cables requires a certain amount of judgment. This may be seen by considering the equivalent single-conductor in Figure 3(B). Some of the filler spaces are left open to provide extra channels for oil flow. The physical constants of the insulating oil must be used for the material in these spaces. The conductors project into the insulation space between the surface of the equivalent

Table I. Properties of Cable Materials

Material	Specific Gravity	Cubical Expansion Coefficient	Specific Thermal Capacity		Thermal Resistivity °C/watt per cm. cube
			Joules/cm. cube/C°	Joules/in. cube/C°	
Copper	8.95	5.01×10^{-5}	3.44	56.4	0.285
Lead	11.37	8.4×10^{-5}	1.43	23.4	2.990
Transformer oil	0.90	75×10^{-5}	1.90	31.1	611
Dry paper	1.41	10×10^{-5}	2.26	37.1	770
Oil-filled insulation*	1.15	45.75×10^{-5}	2.06	33.8	550

* Assumed 55 per cent oil, 45 per cent paper.

single conductor and the sheath. This is not permissible mathematically, as the treatment requires the insulation space to be occupied wholly by insulation. In addition, the mathematics requires the insulation to be uniform in its thermal properties. This latter requirement is not met in the actual cable, since parts of the insulation space are occupied by free oil and the remainder by impregnated paper. It is necessary to use compromise values. In addition to the thermal constants, coefficients of expansion also are required if the oil-flow transient is to be calculated. These too must be appropriate compromise values.

The recommended procedure is to assume that the copper conductors are located wholly within the equivalent single conductor. The remainder of the equivalent single conductor is considered to be occupied by insulation and oil. The thermal and physical constants are calculated for the appropriate volume of each material separately and then combined to get the total for the composite conductor. This is illustrated in detail in the sample calculation given later in the paper. The volume of the free oil in the tubes in the insulation space of the equivalent single conductor cable is next calculated and the remainder of the insulation space is assumed to be occupied by impregnated paper. The thermal and physical properties of the free oil and the impregnated paper are calculated separately and then combined in the same manner as for the conductor. It is very helpful to have a sample of the cable before one, to aid in deciding the relative volumes to use for oil and impregnated paper. The mathematics assumes that the unit thermal resistivity, the specific thermal capacity, and the coefficient of expansion of the insulation are uniform throughout the insulation. The unit values are required, not the total values. The unit thermal resistivity p can be taken as 550 degrees centigrade per watt per centimeter cube. The specific thermal capacity is obtained by dividing the total thermal capacity by the total volume of the equivalent insulation space. This procedure gives an average value which is assumed to be uniform throughout the insulation. The average coefficient of expansion is obtained in the manner shown in the numerical example.

The thermal capacity of the metal in the sheath is calculated in the same way as for solid-type cable. The spiral tubes of the oil channels are close to the sheath and relatively far from the conductors. The thermal capacity of the metal spirals and of the segments of metallic shielding close to the sheath can therefore be added to that of the sheath, if desired. This is considered an unnecessary refinement.

The temperature of the oil in these tubes is probably close to the temperature of the sheath. The viscosity of the oil will be controlled by sheath temperature. Consequently the calculation of the pressure drop of the oil during transients requires the transient sheath temperature to be calculated. This is done very readily along with the calculation of the copper temperature transient, as detailed in the section on Numerical Example. It is worth noting that the transient temperature of any location inside the insulation can be calculated just as readily as the transient sheath or copper temperature by the methods of this paper. If it is felt that it is too conservative to assume that the oil in the channels is at the same temperature as the sheath, the channels can be assumed to be further inside the insulation and the temperature transient calculated for that location.

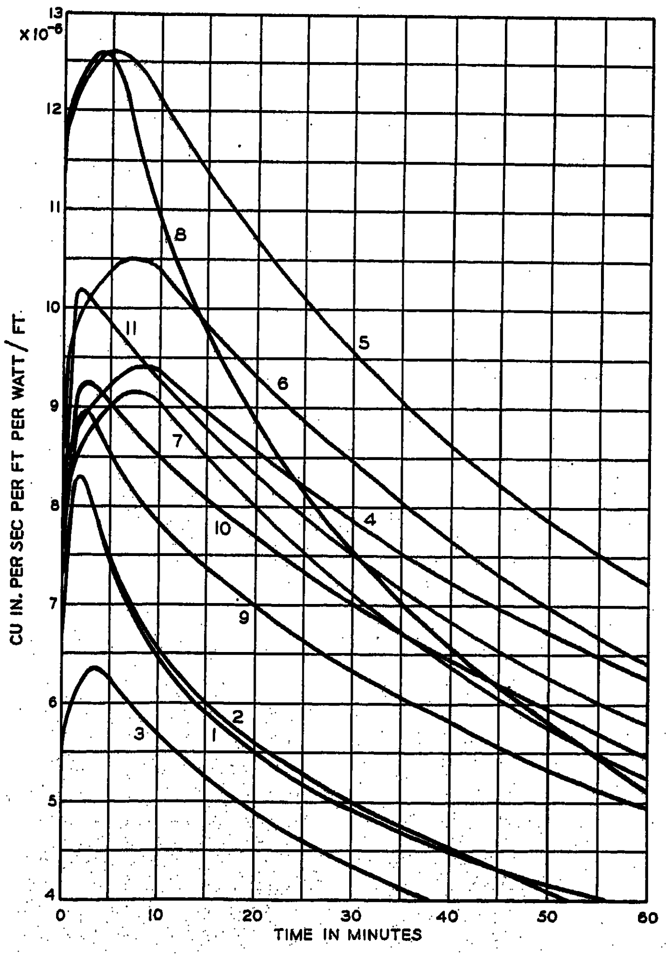


Figure 6 (left). Transient oil demand caused by unit change in copper loss

Curve No.	Conductor		Oil Tube Size (In.)	Kv	Nominal Thickness Mils	
	Size, Cir. In.	No.			Insul.	Sheath
1	2.8	1	0.69	69	330	156
2	2.1	1	0.69	69	315	141
3	2.0	1	None	69	315	235**
4	1.1	1	0.69	132	720	156
5	0.6	1	0.69	132	720	190*
6	0.6	1	0.50	132	720	156
7	0.6	1	0.40	132	720	250**
8	0.25	1	0.69	132	560	133
9	0.55	3	0.39	69	315	160
10	0.45	3	0.55	69	315	156
11	0.40	3	0.55	69	315	156

* Double. ** Fluted.

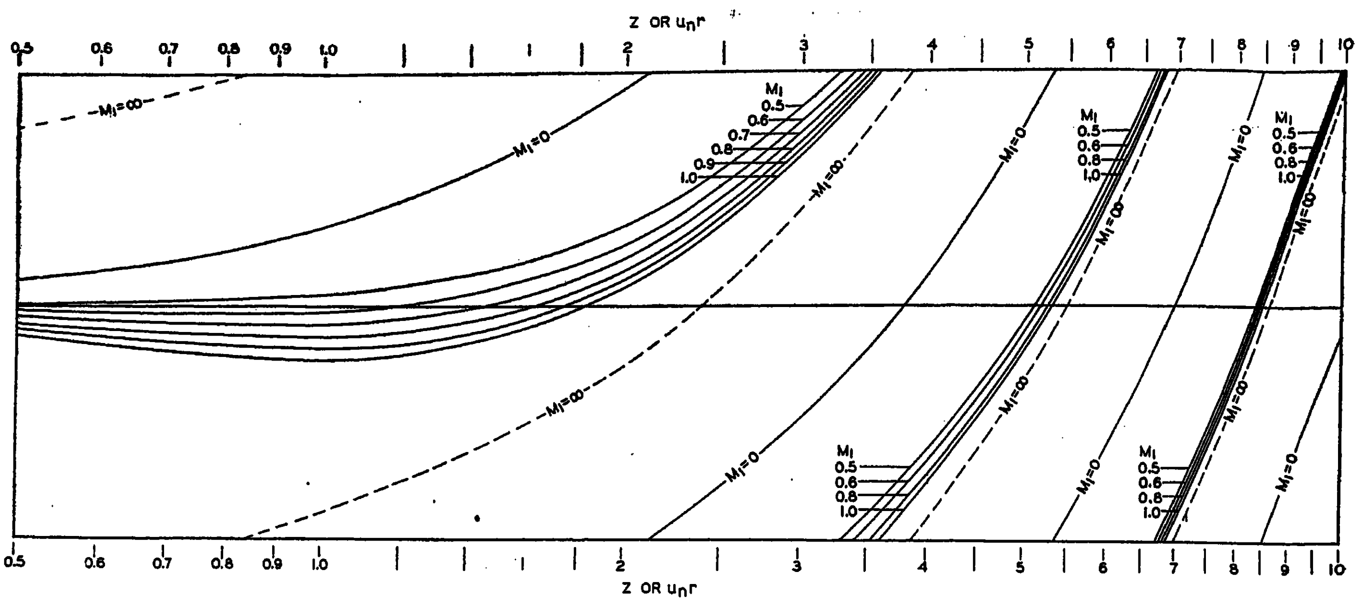


Figure 7

Treatment of Dielectric Losses in Transient Problems

The equivalent single-conductor conception is not capable of handling the problem of dielectric losses in an acceptable manner. This is because there is no equivalence between the actual dielectric field in a 3-conductor cable and the dielectric field in a single-conductor cable. In this respect the equivalent single-conductor method falls short of the capabilities of the rigorous solution for single-conductor cables, which includes the feature of dielectric losses properly distributed throughout the insulation. Fortunately, this is not of much importance.

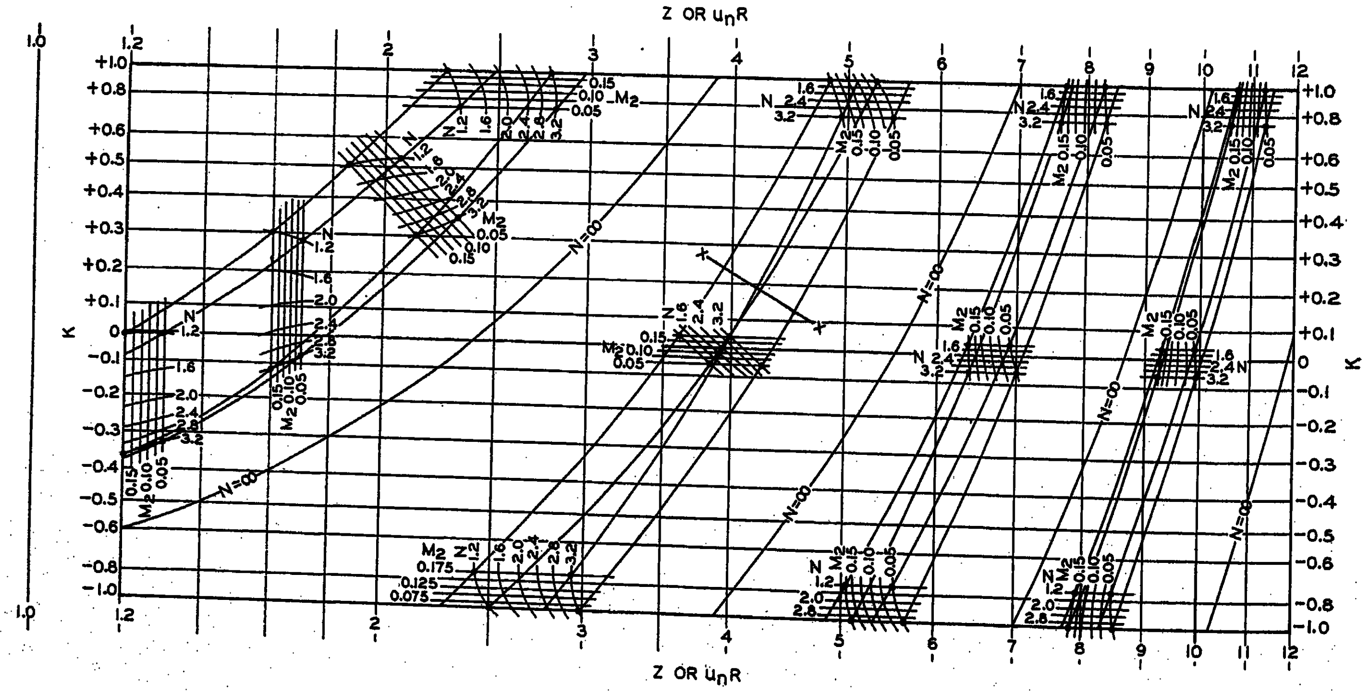
Dielectric losses are usually negligible in modern cables at the voltages at which 3-conductor cables are operated. In addition, most practical problems do not involve the switching on or off of the voltage, which is the only condition under which the dielectric losses can change. A rough approximation of the copper temperature transient caused by a change in dielectric loss can be made, if desired, by the well-known method of assuming that half the calculated value of dielectric loss is copper loss and the other half is sheath loss. This approximation can not be used to calculate the oil demand transient caused by a change in dielectric losses, since the nature of the oil demand transient is entirely different for copper loss, dielectric loss, and sheath loss respectively.

This is shown clearly in Figures 3 and 4 of reference 1.

Temperature of the Ducts

As a general rule, a cable responds much more quickly to loss changes than a duct structure. The cable temperature transient completes itself in less than eight hours, while the duct temperature transient will usually go on for two days or more. The difference is so pronounced that for short-time loading problems, the temperature of the inside surface of the occupied duct can usually be taken as remaining constant. This assumption was made in the development of the rigorous solution of reference 1 and also consequently applies to the present work. It is fortunate that this assumption is reasonably accurate, since a rigorous solution

Figure 8



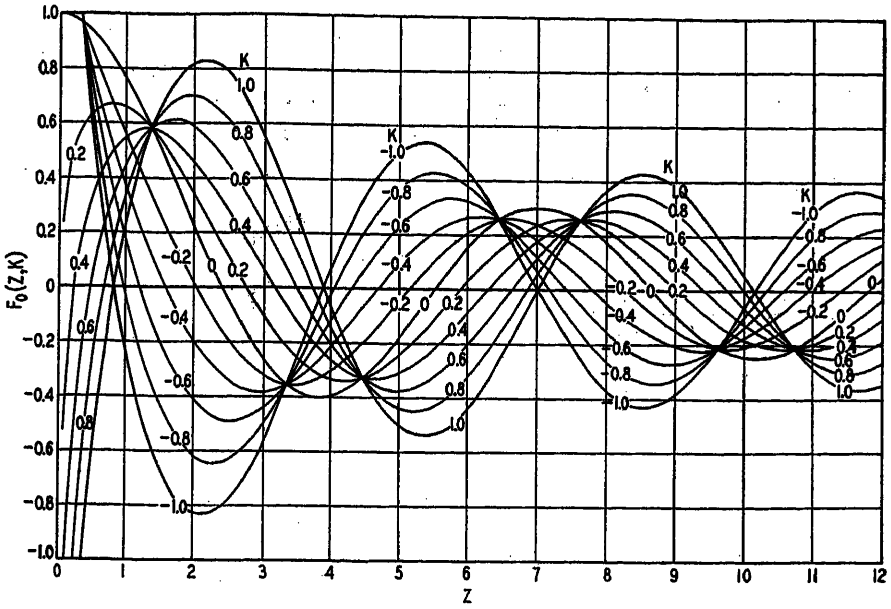


Figure 9. Function F_0

for the transient temperatures of duct structures in a form suitable for routine practical use has not yet become available.

The temperature of the inside surface of the occupied duct is the reference or ambient temperature to which the transient temperature rise must be added to get the total temperature of the cable. The choice of the occupied duct temperature as the reference temperature was forced by the requirements of the mathematical analysis. It is not a convenient temperature to use as a reference, or starting point, in practical problems. The chief disadvantage is that it cannot be readily measured with accuracy. The idle duct temperature, that is, the temperature of the air in an empty duct adjacent to the occupied duct, is much more convenient and is invariably used in practical cable temperature work. It is therefore recommended that the idle duct temperature be used as the reference temperature in transient problems. The idle duct temperature will remain constant during a transient just as does the occupied duct temperature. This being the case the idle duct temperature can be calculated in transient problems by exactly the same methods as for steady-state problems. These methods have been covered elsewhere, for example in reference 6, and will not be discussed here. Where it is possible to measure the idle duct temperature under the conditions applying to the problem under investigation, it is preferable usually to do so rather than to rely on calculations. The occupied duct temperature is obtained by adding the thermal rise of the occupied duct over the idle duct, calculated for the steady-state condition which is assumed to precede the initiation of the transient. The order of magnitude of this rise is indicated by the very general statement that the thermal resistance from the inside surface of an occupied duct to the air in an adjacent idle duct may be anywhere from 0.5 to 1.5 degrees centigrade per watt per foot of duct.

SHEATH THERMAL RESISTANCE

The choice of the value of sheath thermal resistance to use in a given problem requires a little thought. In the calculation of transient temperatures and oil demands, the temperature rise of the sheath must be calculated with reference to the temperature of the inside surface of the occupied duct, which remains constant. Published data on this subject are felt to be rather inadequate. A valuable discussion of the subject is given in reference 8, along with some test values of sheath thermal resistance determined for cables in air and in conduits. Additional data are to be found in references 3, 5, 6, and 7 among others. Study of references 3, 5, and 7 will disclose the fact mentioned earlier, that the sheath thermal resistance is not constant but is quite appreciably dependent on the losses being dissipated through the sheath. An endeavor should be made to select a value appropriate to the losses in the problem being considered.

Some operators of underground cable systems use values of thermal resistance determined by field measurements and which represent the combined thermal drops from the sheath to the occupied duct and from the occupied duct to the idle duct. These may be used in calculating the steady-state temperatures preceding transients, but it is not correct to use them in the calculation of the transients themselves.

Numerical Example

A numerical example is worked out to illustrate the equivalent single-conductor method. The cable is a 3-conductor 450,000-circular mil 69-kv oil-filled type with compact round conductors, as illustrated in Figure 3(B). It is assumed that the best available value for the insulation thermal resistance, R_t, has been obtained, either by actual test or by calculation. The thermal resistivity p of the insulation, other necessary physical properties of the cable, and cable data are listed in Tables I and II. All symbols are listed and explained as they occur in the calculation.

Table II. Cable Data

Conductor size	450,000 cir. mil (0.45 cir. inches)
Diameter of conductor	0.700 inches
Diameter over insulation	1.347 inches
Diameter over shielding	1.363 inches
Thickness of lead sheath	0.156 inches
Overall diameter	3.290 inches
Diameter under lead sheath, (D)	2.978 inches
Inside diameter of oil tubes	0.550 inches
Outside diameter of oil tubes	0.650 inches
Insulation thermal resistance, (R_t)	1.195 degrees °C/watt/ft.
Thermal resistivity of insulation, (p)	550 degrees °C/watt/cm. cube

DETERMINATION OF CONDUCTOR AND SHEATH TEMPERATURE TRANSIENTS

Solve equation 1 for d, the diameter of the equivalent single conductor which will have the correct steady-state temperature rise

$$R_t = 0.00522p \ln D/d \tag{1}$$

$$\ln D/d = 1.195/550(0.00522) = 0.416;$$

$$D/d = 1.515$$

$$d = 2.978/1.515 = 1.966 \text{ inches}$$

Insulation thickness on equivalent conductor $= (2.978 - 1.966)/2 = 0.506$ inches.

The next step is to consider that the copper conductors are located wholly within the circle bounded by diameter d, irrespective of their actual location and that the rest of the volume contained within d consists of impregnated paper. This means that the equivalent conductor in this hypothetical cable is composed of copper and impregnated paper, see Figure 3(B). The annular space between d and D is the insulation space of the equivalent single-conductor cable and therefore is considered to be wholly occupied by insulation, consisting partly of impregnated paper and partly of the free oil in the oil feed tubes.

Table III

Item	Symbol	How Obtained	Units	Value
1. Equivalent conductor radius	r	Calculated	Inches	0.983
2. Radius over insulation	R	Calculated	Inches	1.489
3. Thermal resistivity of insulation	p	Table I	C/watt/cm. cube C/watt/in. cube	550 216.2
4. Thermal resistivity of sheath	B	Selected by calculator	C/watt/sq.cm.	1,200
5. Surface thermal resistance, sheath to duct	E	0.00411 B/O.D. of cable	C/watt/ft.	1.5
6. Thermal capacity of sheath	Q_2	883 (Avg. Diam.)(thickness)/12	Joules/C/in.	36.0
7. Volume inside sheath	V	$\pi(1.489)^2\ 12$	Cu. in./ft.	83.5
8. Volume of equivalent conductor	C	$\pi(0.983)^2\ 12$	Cu. in./ft.	36.4
9. Volume of equivalent insulation (including oil tubes)		Item 7 − Item 8	Cu. in./ft.	47.1
10. Volume of oil in the three tubes		$3\pi(0.55)^2\ 12/4$	Cu. in./ft.	8.56
11. Specific thermal capacity of oil		Table I	Joules/C/cu. in.	31.1
12. Thermal capacity of oil in feed tubes		8.56 × 31.1	Joules/C/ft.	266.0
13. Volume of impregnated paper in equivalent insulation		Item 9 − Item 10	Cu. in./ft.	38.54
14. Specific thermal capacity of impregnated paper		Table I	Joules/C/cu. in.	33.8
15. Thermal capacity of impregnated paper portion of insulation		38.54 × 33.8	Joules/C/ft.	1,304
16. Total thermal capacity of insulation (including oil in feed tubes)		Item 12 + Item 15	Joules/C/ft.	1,570
17. Avg. specific thermal capacity of equivalent insulation	q	Item 16 + Item 9	Joules/C/in. cube	33.3
18. Actual volume of copper		3 × 0.45 × 9.43 (1 cir. mil = 9.43×10^{-6} in. ³/ft.)	Cu. in./ft.	12.72
19. Volume of impregnated paper in equivalent conductor		Item 8 − Item 18	Cu. in./ft.	23.68
20. Thermal capacity of copper conductors, see Table I		551(3)(0.45)	Joules/C/ft.	744.0
21. Thermal capacity of impregnated paper in equivalent conductor		Item 19 × Item 14	Joules/C/ft.	800.0
22. Total thermal capacity of equivalent conductor	Q_1	$\dfrac{\text{Item 20} + \text{Item 21}}{12}$	Joules/C/in.	128.7
23. $M_1 = Q_1/2\pi q r^2$		$128.7/2\pi(33.3)(0.983)^2$	Numeric	0.635
24. $M_2 = Q_2/2\pi q R^2$		$36.0/2\pi(33.3)(1.489)^2$	Numeric	0.078
25. $N = p/2\pi E$		$216.2/2\pi(1.5)(12)$	Numeric	1.91
26. R/r		1.489/0.983	Numeric	1.514
27. $p\,q$		Item 3 × Item 17	Sec./sq. in.	7,240

The calculation is continued in Table III using only the dimensions of the equivalent single-conductor cable.

Next obtain values of K and Z from Figures 7 and 8. These Figures together with Figure 9 solve the transient temperature equation graphically, as described in reference 1. Figure 8 must be traced on transparent paper for practical use. If Figures 7 and 8 are to be used frequently in routine work, they should be replotted to a scale about five times larger. The symbol Z represents the quantity ux, where x is any radius measured from the center of the cable. Thus at the surface of the conductor, $x = r$ and $Z = ur$; at the surface of the insulation, $x = R$ and $Z = uR$. At the general radius x, Z would equal ux. u is a number which the calculations will evaluate. In Figure 7, Z always represents ur. In Figure 8, Z always represents uR. In Figures 9 and 10, referred to later, Z represents any value of ux from ur to uR.

1. Lay Figure 8 over Figure 7 with the Z axes coinciding and with $Z = R/r = 1.514$ on Figure 8 coinciding with $Z = 1$ on Figure 7.

2. Interpolate on Figure 7 the sequence of curves $M_1 = 0.635$ and interpolate on Figure 8 the sequence of curves for $M_2 = 0.078$ and $N = 1.91$.

3. Note the points where the interpolated curves of Figure 7 intersect those of Figure 8. Only two intersections usually occur for cables with thin insulation. For cables with thick insulation three intersections usually occur. The third intersection can be ignored without much sacrifice in accuracy and this will save a great deal of computing labor later on.

4. Two intersections are obtained in the present problem. For the first point of intersection (starting at the left hand side of the figures) read off K_1 from the vertical scale on Figure 8; read off u_1r from the horizontal scale of Figure 7; and read off u_1R from the horizontal scale of Figure 8. For the second intersection read off in the same way the values of K_2, u_2r and u_2R.

These values now are tabulated. The making of a complete and orderly tabulation of every step of the calculation is of the utmost importance in obtaining a correct final result. The values of u_n also are required. They are calculated from $u_n = u_nR/R$ and entered in the tabulation. The values of u_n^2 and of $\lambda_n = u_n^2/pq$ are calculated and tabulated in Table IV.

The multiplier 60 is used in evaluating λ_n so that the time can be expressed in minutes instead of seconds in the final equation.

It now is necessary to obtain from Figure 9 the values of $F_o\ (Z,K)$. As already explained Z represents u_nr or u_nR. The values of $F_o\ (Z,K)$ corresponding to the tabulated values of K_n, u_nr and u_nR are set down in Table V.

It can be checked easily from Figure 9 that $F_o(u_1r,\ K_1) = +0.73$ is obtained by entering with $K_1 = -0.045$ and $Z = u_1r = 0.984$. $F_o(Z,K)$ is read from the left-hand scale. The remaining values of

Table IV

Quantity	How Obtained	Value n = 1 (First Intersection)	 n = 2 (Second Intersection)
K_n	Figures 7 and 8	$K_1 = -0.045$	$K_2 = -\ 0.78$
u_nr	Figures 7 and 8	$u_1r =\ 0.984$	$u_2r =\ 3.62$
u_nR	Figures 7 and 8	$u_1R =\ 1.491$	$u_2R =\ 5.47$
u_n	Calculated from u_nR/R	$u_1 =\ 1.002$	$u_2 =\ 3.68$
u_n^2	Calculated	$u_1^2 =\ 1.004$	$u_2^2 =\ 13.55$
$\lambda_n = 60u_n^2/pq$	Calculated	$\lambda_1 =\ 0.00832$	$\lambda_2^2 =\ 0.1124$

Table V

Quantity	How Obtained	Value n = 1 (First Intersection)	 n = 2 (Second Intersection)
$F_o(u_nr,\ K_n)$	Figure 9	$F_o(u_1r,\ K_1) = +0.73$	$F_o(u_2r,\ K_2) = -0.225$
$F_o(u_nR,\ K_n)$	Figure 9	$F_o(u_1R,\ K_1) = +0.47$	$F_o(u_2R,\ K_2) = +0.42$

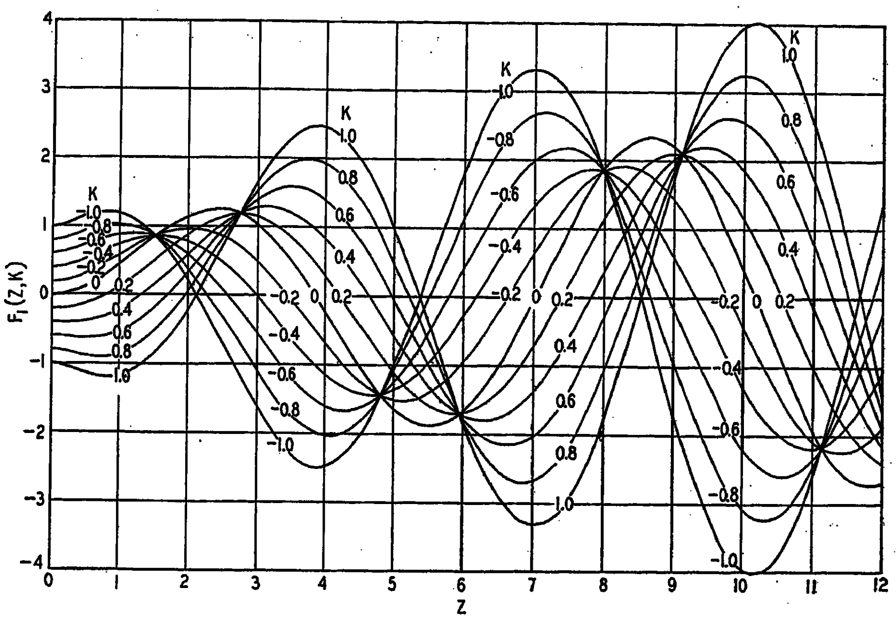


Figure 10. Function F_1

$F_o(Z,K)$ are obtained in similar fashion.

The last major step is to evaluate the quantities C_n in the following simultaneous equations, in which all other terms have already been evaluated

$$C_1F_o(u_1r,\ K_1)+C_2F_o(u_2r,\ K_2)=R_i+E \qquad (2)$$

$$C_1F_o(u_1R,\ K_1)+C_2F_o(u_2R,\ K_2)=E \qquad (3)$$

Substituting in these equations, there is

$$0.73C_1-0.225C_2=1.195+1.5=2.695$$
$$0.47C_1+0.42C_2=1.5$$

Solving

$$C_1=3.56$$
$$C_2=-0.414$$

These values now may be substituted in the equations for the temperature transients, which are given below.

Conductor temperature transient due to abruptly dropping one watt of copper loss

$$T_c=C_1F_o(u_1r,K_1)\epsilon^{-\lambda_1t}+C_2F_o(u_2r,K_2)\epsilon^{-\lambda_2t} \qquad (4)$$

Sheath temperature transient due to abruptly dropping one watt of copper loss

$$T_s=C_1F_o(u_1R,\ K_1)\epsilon^{-\lambda_1t}+C_2F_o(u_2R,K_2)\epsilon^{-\lambda_2t} \qquad (5)$$

Substituting numerical values, the final equations are

$$T_c=2.60\epsilon^{-0.00832t}+0.093\epsilon^{-0.1124t} \qquad (4)$$

$$T_s=1.67\epsilon^{-0.00832t}-0.174\epsilon^{-0.1124t} \qquad (5)$$

The evaluation of equation 4 is tabulated in the second column of Table VI. It is important to note that the whole calculation has been carried through for a change in loss of one watt per foot. The results can be used for any other change in loss by simply multiplying the tabulated results by the actual change in loss. This is illustrated by examples in the section on Short-Time Ratings of Cables. The right-hand column of Table VI contains an evaluation of the attainment factor. This is the per cent of ultimate temperature rise attained at any time during a transient caused by increasing the copper loss by one watt per foot. It is obtained by subtracting column three from 100. Column three is column two expressed in per cent of the initial temperature. The use of the attainment factor is explained in the section on Short-Time Ratings of Cables.

Table VI. Evaluation of Attainment Factor

Time (t), Minutes	T_c for Dropping One Watt of Copper Loss Per Foot of Cable	Decrement Factor Per Cent	Attainment Factor, Per Cent
0	2.693	100	0
2.5	2.615	97.1	2.9
5	2.548	94.7	5.3
10	2.422	90.0	10.0
15	2.312	85.8	14.2
30	2.028	75.7	24.3
60	1.580	58.7	41.3
120	0.957	35.5	64.5
240	0.354	13.1	86.9
480	0.047	1.7	98.3

Attainment factors for a wide variety of single-conductor cables are given in Figure 4. These can be useful for estimating the temperature transients of similar cables. In Figure 5 the attainment factors are given for a variety of three-conductor cables. The numerical value of the insulation thermal resistance R_i also is given in these figures for each of the cables concerned. The copper temperature rise above the occupied duct temperature in degrees centigrade at any point in the transient can be obtained by multiplying the ordinate of the attainment factor curve by (R_i+E) and by the watts loss.

For the sake of clarity only the conductor and sheath temperature transients due to a change in copper loss have been worked out in the example. In the Appendix the procedure is given for calculating the conductor and sheath temperature transients due to a change in sheath loss or a change in dielectric loss. The latter is only applicable to single-conductor cables, for the reason given in the section on Treatment of Dielectric Losses in Transient Problems. The Appendix also contains the procedure for obtaining the temperature transient at any point in the insulation due to a change in copper loss, or sheath loss, or dielectric loss.

DETERMINATION OF THE OIL DEMAND TRANSIENTS

The oil demands of the component parts of the cable, namely, the conductor, the insulation, and the sheath are obtained separately by calculating the volume of the respective components times their effective expansion coefficients times their individual rates of temperature change. The total oil demand is the algebraic sum of the above component demands.

When load is dropped, the cable cools and the conductor shrinks in volume. Oil must be supplied to the cable to fill the resulting space. The insulation also is shrinking in volume and consequently additional oil must be supplied to the cable.

As the sheath contracts, however, the volume within the sheath is reduced and oil is expelled from the cable. Thus the effect of the sheath component is negative and must be subtracted when the over-all demand of the cable is being determined.

Volumetric expansion coefficients are used for all the component parts of the cable.

The method of calculating the sheath expansion is of some interest. The volume enclosed within the sheath is multiplied by the volumetric expansion coefficient of metallic lead. To see that this is valid, imagine that the sheath fits tightly around a solid cylinder of lead, instead of around the normal cable insulation and copper. No matter what temperature change may occur, the lead cylinder will always exactly fit within the enclosing sheath. Consequently the volumetric expansion of the sheath is correctly calculated by multiplying the volume enclosed within the sheath by the volumetric coefficient of expansion of metallic lead. Suitable compromise values for the expansion coefficients of the equivalent conductor and insulation respectively are required. The expansions of the component parts of the equivalent conductor are individually determined and totalled for unit temperature change. The total is then divided by the total conductor volume to arrive at the effective expansion coefficient. A similar procedure is followed when dealing with the insulation. The actual values are given in Table VII.

Let

a_1 = conductor component of the oil demand
a_2 = sheath component of the oil demand
a_3 = insulation component of the oil demand
a = total oil demand $= a_1 + a_3 - a_2$

The equations for the component transient oil demands are as follows where only two terms are to be retained in the solution

$$a_1 = \frac{Cc}{pq}\left[u_1^2 C_1 F_o(u_1 r, K_1)\epsilon^{-\lambda_1 t} + u_2^2 C_2 F_o(u_2 r, K_2)\epsilon^{-\lambda_2 t}\right] \quad (6)$$

$$a_2 = \frac{Vv}{pq}\left[u_1^2 C_1 F_o(u_1 R, K_1)\epsilon^{-\lambda_1 t} + u_2^2 C_2 F_o(u_2 R, K_2)\epsilon^{-\lambda_2 t}\right] \quad (7)$$

$$a_3 = \frac{2\pi e}{pq}\left\{ C_1[F_1(u_1 R, K_1) - F_1(u_1 r, K_1)]\epsilon^{-\lambda_1 t} + C_2[F_1(u_2 R, K_2) - F_1(u_2 r, K_2)]\epsilon^{-\lambda_2 t}\right\} \quad (8)$$

The simplicity of the solution of the oil demand transient equations once the temperature transient equations have been solved now will be apparent. The coefficients C_1 and C_2 and the F_o (Z,K) functions are the same as for the transient temperature equations. The values of $F_1(Z,K)$ are obtained from Figure 10 in exactly the same manner that the values of $F_o(Z,K)$ were obtained from Figure 9.

These values must now be tabulated in order to obtain the numerical values for oil flow coefficients, as shown in Table IX.

Table X. Evaluation of Transient Oil Demand Due to Abruptly Dropping 1 Watt of Copper Loss

t Minutes	Oil Demand (in ^{3}/sec./ft./watt/ft.) n=1	n=2	Total
0			7.42×10^{-6}*
2.5	8.76×10^{-6}	0.49×10^{-6}	9.25
5	8.58	0.37	8.95
10	8.23	0.21	8.44
15	7.90	0.12	8.02
30	6.97	0.02	6.99
60	5.43		5.43
120	3.29		3.29
240	1.22		1.22
480	0.16		0.16

* Value obtained from equation 10.

Substituting these values in the transient equation for oil demand, the oil flow due to abruptly dropping one watt of copper loss is

$$a = 8.94\times10^{-6}\epsilon^{-\lambda_1 t} + 0.65\times10^{-6}\epsilon^{-\lambda_2 t}$$
cubic inch per second per foot (9)

The evaluation of equation 9 is given in Table X.

If the oil flow due to a change in sheath loss or a change in dielectric loss is required, it can be obtained simply by substituting the proper values of C_1 and C_2 in the component transient equations. The equations for evaluating these constants when sheath loss or dielectric loss is being considered are given in the Appendix. As in the transient temperature calculation, the oil demand has been worked out for a change in loss of one watt per foot.

At $t=0$, equations 6, 7, and 8 are not accurate since only the first two terms of an infinite series[1] are retained in the solution. After the first few moments the neglected terms rapidly become insignificant, but at $t=0$ they are important. For the initial instant ($t=0$) the following equations should be used

Oil flow due to abruptly dropping one watt of copper loss

$a_1 = Cc/Q_1$; $\quad a_2 = 0$; $\quad a_3 = 0$ (10)

Oil flow due to abruptly dropping one watt of dielectric loss

$a_1 = 0$; $\quad a_2 = 0$; $\quad a_3 = e/q$ (11)

Oil flow due to abruptly dropping one watt of sheath loss

Table VII. Cubical Expansion Coefficients of Equivalent Conductor and Equivalent Insulation

	Volume (in ^{3}/ft.)	Expansion Coefficient	Product
Equivalent Conductor			
Copper	12.72	5.01×10^{-5}	63.7×10^{-5}
Paper—45 per cent of 23.68	10.66	10×10^{-5}	106.6×10^{-5}
Oil—55 per cent of 23.68	13.02	75×10^{-5}	976.5×10^{-5}
	36.40		1146.8×10^{-5}
Equivalent Insulation			
Paper—45 per cent of 38.54	17.34	10×10^{-5}	173.4×10^{-5}
Oil—55 per cent of 38.54	21.20	75×10^{-5}	1592×10^{-5}
Free oil in feed tubes	8.56	75×10^{-5}	642×10^{-5}
	47.10		$2{,}407.4\times10^{-5}$

Additional quantities now are shown in Table VIII.

Table VIII

Item	Symbol	How Obtained	Units	Value
28. Average cubical expansion coefficient of conductor	c	$1{,}146.8\times10^{-5}/36.40$	Vol/Vol/C	31.5×10^{-5}
29. Average cubical expansion coefficient of insulation	e	$2{,}407.4\times10^{-5}/47.10$	Vol/Vol/C	51.1×10^{-5}
30. Cubical expansion coefficient of sheath	v	Table I	Vol/Vol/C	8.4×10^{-5}
31. Cc/pq		$36.4(31.5\times10^{-5})/7{,}240$	in ^{3}/ft./Csec.	15.85×10^{-7}
32. Vv/pq		$83.5(8.4\times10^{-5})/7{,}240$	in ^{3}/ft./Csec.	9.68×10^{-7}
33. $2\pi e\times12/pq$		$24\pi(51.1\times10^{-5})/7{,}240$	in ^{3}/ft./Csec.	53.2×10^{-7}

Table IX

Quantity	Value n=1 (First Intersection)	 n=2 (Second Intersection)
34. C_n	$C_1=3.56$	$C_2=-0.414$
35.	$C_1F_o(u_1r, K_1)=2.60$	$C_2F_o(u_2r, K_2)=0.093$
36.	$a_1=\frac{Cc}{pq}u_1^2C_1F_o(u_1r, K_1)=4.13\times10^{-6}$	$\frac{Cc}{pq}u_2^2C_2F_o(u_2r, K_2=2.0\times10^{-6}$
37.	$C_1F_o(u_1R, K_1)=1.672$	$C_2F_o(u_2R, K_2)=-0.174$
38.	$a_2=\frac{Vv}{pq}u_1^2C_1F_o(u_1R, K_1)=1.63\times10^{-6}$	$\frac{Vv}{pq}u_2^2C_2F_o(u_2R, K_2)=-2.28\times10^{-6}$
39.	$F_1(u_1r, K_1)=+0.48$	$F_1(u_2r, K_2)=-1.79$
40.	$F_1(u_1R, K_1)=+0.82$	$F_1(u_2R, K_2)=-0.14$
41.	$a_3=\frac{24\pi e}{pq}C_1[F_1(u_1R, K_1)-F_1(u_1r, K_1)]=6.44\times10^{-6}$	$\frac{24\pi e}{pq}C_2[F_1(u_2R, K_2)-F_1(u_2r, K_2)]=-3.63\times10^{-6}$
42. Total oil demand $(a_1+a_3-a_2)$ =	8.94×10^{-6}	$=0.65\times10^{-6}$

$$a_1=0; \qquad a_2=Vv/Q_2; \qquad a_3=0 \qquad (12)$$

There is no error in the transient temperature equations at $t=0$ because the mathematical form of solution automatically avoids it.

The oil demand transients are given in Figure 6 for the cables referred to in Figures 4 and 5 which are of the oil-filled type. These curves can be useful for quickly estimating the maximum oil demands of cables similar to those shown in the figure.

The calculating procedures given in the foregoing example can be used just as well for single-conductor cable problems as for 3-conductor cable problems.

Short-Time Ratings of Cables

The practical use of the calculated thermal transients is chiefly in the field of short-time ratings. Another but more limited field of practical use is in the designing of oil-filled cable lines. The pressure drop of the oil along the hollow core or oil-feed tubes in such lines is the controlling design factor. The pressure drop is directly proportional to the rate of flow of the oil and to its viscosity. The rate of flow is obtained from the calculated transient oil demands. The viscosity is obtained from the calculated transient temperature of the conductor if the cable is of the hollow-core type, or from the transient temperature of the sheath if the cable is of the 3-conductor type or the fluted-sheath type. It is, of course, necessary to have the characteristic viscosity-temperature curve of the oil used in the cable. The designing of oil-filled lines is a rather extensive subject which this paper does not attempt to cover, other than the calculation of the fundamental oil demand and temperature transients.

Short-time ratings consist essentially of answering the following question: "If the cable under consideration has been operating at approximately such-and-such a stable temperature, how much more load can be suddenly thrown on it for X hours without exceeding the maximum permissible copper temperature?"

This formulation of the question makes the calculating procedure almost obvious, once the transient temperature characteristics of the cable have been determined. Suppose that the shielded 500,000-circular mil 3-conductor cable number 5 of Figure 5 is installed in a duct bank with other cables, the idle duct temperature of the bank being 40 degrees centigrade, at, say 5 p.m. The load on the 500,000-circular mil cable is 250 amperes at that time and the cable has attained an approximately stable copper temperature. The thermal resistance between the idle duct and the occupied duct is taken as 1.0 degrees centigrade per watt per foot. The total conductor losses in the cable are 4.67 watts. The temperature of the occupied duct is therefore $40+4.67\times1=44.67$ degrees centigrade. The copper temperature will be about 58 degrees centigrade, that is, $3\times I^2R\,(R_t+E)+44.67$ degrees centigrade $=(4.67)(2.855)+44.67=58$ degrees centigrade.

The value of R_t is given in Figure 5. The sheath thermal resistance E is calculated from the formula given in item 5, Table III, using $B=1{,}200$ degrees centigrade per watt per square centimeter, which is about right when the total losses are about five watts per foot.

Now if an emergency such as the failure of a parallel cable occurs, how much additional load can be carried through the peak period? The peak will probably be over at 7 p.m. Accordingly the extra load will be applied for two hours, that is, from 5 p.m. to 7 p.m. The normal maximum allowable copper temperature for the cable, which will be assumed to be rated in the 12-kv class, is 83 degrees centigrade. Thus the additional copper temperature rise is to be $83-58=25$ degrees centigrade. The ultimate steady-state rise above the occupied duct temperature for one watt loss is $1\times(R_t+E)=2.855$. From Figure 1, the rise in two hours will be 81.5 per cent of the ultimate or 2.32 degrees centigrade. Hence the additional losses that can be allowed are $25/2.32=10.8$ watts. The allowable current will be

$$\sqrt{\frac{4.67+10.8\text{ watts}}{3R}}=442\text{ amperes}$$

or an increase in load of 192 amperes or 4,000 kva at 12 kv.

To be conservative in the calculation of the allowable current, the copper resistance is taken at the final temperature of 83 degrees centigrade instead of the initial temperature of 58 degrees centigrade.

It is interesting to note that to hold the temperature constant at the new level, the load must be sharply reduced after the 2-hour overload. Holding the temperature constant at this point requires the application of a transient which will cancel that part of the transient in Figure 1 which extends beyond the 2-hour point. Thus a negative transient must be applied, or in other words a load reduction.

To illustrate further the value of short-time overloads, the case will be considered for the same cable where it is decided that a higher temperature than 83 degrees centigrade will be permitted in an emergency. Suppose, for example, that the permissible temperature is to be 98 degrees centigrade as permitted for emergency operation by the Association of Edison Illuminating Companies Cable Specifications.[9] Following the same procedure as before, the permissible loading for two hours will be found to be 514 amperes or an increase of 264 amperes over the initial loading of 250 amperes. For one hour the maximum loading would be 578 amperes and for one-half hour the permissible loading would be 710 amperes. These are substantial increases which can be of much value, as for instance in making possible the elimination or deferment of a proposed additional cable line.

In many practical cases a utility has no means of controlling an overload caused by the trip-out or failure of one of a group of parallel lines. In such cases the practical question becomes "Did the overload damage the overloaded cables?" This question is readily answered by calculating the increase in watts loss caused by the overload. From the attainment factor for the cable the transient temperature rise is determined. This is added to the initial temperature to get the total temperature at the end of the overload. It can be said that if such an occurrence happens only occasionally, an experienced cable engineer will usually not consider that any appreciable damage has been done if the copper temperature has not exceeded 110 or 120 degrees centigrade for an hour or two.

The operating procedure to be followed in emergencies deserves some comment. As a rule the direct control of the cable circuits will be in the hands of the load dispatcher or the operating division, rather than the cable engineer. It is considered good practice to furnish the operating division with a list of normal permissible ratings for all cables on the system, accompanied by a standing instruction to notify the cable engineer immediately when the load on any cable exceeds its rating. The cable engineer will then learn from the operating division how long the overload is expected to continue. The engineer should have at hand the transient temperature characteristic, or attainment curve, of each type of cable on the system. From this, along with his intimate knowledge of duct temperatures throughout the system, he can estimate in a few minutes whether or not the cable will be damaged seriously. Usually there will be no means of reducing the overload by a controlled amount. It will be a question of either permitting it to continue or of taking the line out of service. The latter is nearly always a drastic, last-resort procedure. However, if the overload is going to result in "cooking" the cable, then taking the line out of service for a few hours will be preferable to damaging it so badly that complete replacement is necessitated. The latter may require months of time and the expenditure of many thousands of dollars. The cable engineer should have the authority to order the line taken out of service when in his opinion such action is justified, no matter how important the customer load may be that is interrupted.

It is not considered good practice to attempt to foresee all emergencies that might arise and to calculate in advance for the use of the operating division the overload capabilities of all system cable lines under such hypothetical conditions. With the aid of the transient temperature characteristics of the cables, any emergency can be promptly and more intelligently met as it arises by the cable engineer. The needs of many systems will be adequately met by the attainment curves given in Figures 4 and 5, without the necessity of calculating curves for the specific cables that may happen to be on the system. This is so because the attainment curves given are for a very wide variety of cable designs and the attainment curves are, in general, not very different even for considerable variations in cable make-up.

A type of operating problem which differs markedly from the problem of emergency short-time overloads is that of variable loading. It may be desired to take advantage of the fact that the carrying capacity of a cable operating under a load cycle of the type common to light and power feeders is somewhat greater than for a continuous steady load. The attainment curves can be used to calculate such problems, by a step-by-step procedure. The procedure has been fully covered elsewhere[3] and will not be repeated here. However, it seems desirable for the sake of completeness to draw attention to this important application of transient temperature calculations.

Appendix I

The transient temperature equations for changes in sheath loss or dielectric loss are evaluated in a similar manner to that illustrated for copper loss. The transient due to sheath loss will be explained first. The procedure is identical with the numerical example up to equations 2 and 3. The right-hand side of equation 2 is the copper temperature rise above ambient due to one watt of copper loss. This is replaced in the sheath loss transient by the copper temperature rise due to one watt of sheath loss, which is E. The right-hand side of equation 3 is the sheath temperature rise due to one watt of copper loss. This is replaced by the sheath temperature rise due to one watt of sheath loss, which again is E. The equations now become

$$C_1F_0(u_1r, K_1)+C_2F_0(u_2r, K_2)=E \qquad (2A)$$

$$C_1F_0(u_1R, K_1)+C_2F_0(u_2R, K_2)=E \qquad (3A)$$

These equations are to be solved for C_1 and C_2. The values of C_1 and C_2 so obtained when used in equation 4 transform it into the equation for the conductor temperature transient due to dropping one watt of sheath loss. When used in equation 5, they transform it into the equation for the sheath temperature transient due to dropping one watt of sheath loss.

The dielectric loss transients cannot be handled for 3-conductor cables for reasons explained in the section on Treatment of Dielectric Losses in Transient Problems. The following procedure, therefore, applies only to single-conductor cables for obtaining the dielectric loss transients. The right-hand side of equation 2 is replaced by the copper temperature rise due to one watt of dielectric loss, which is $(R_i/2+E)$. The right-hand side of equation 3 is replaced by the sheath temperature rise due to one watt of dielectric loss, which is E. The equations now become

$$C_1F_0(u_1r, K_1)+C_2F_0(u_2r, K_2)=Ri/2+E \qquad (2B)$$

$$C_1F_0(u_1R, K_1)+C_2F_0(u_2R, K_2)=E \qquad (3B)$$

These equations are to be solved for C_1 and C_2. The values so obtained when used in equation 4 transform it into the equation for the conductor temperature transient due to dropping one watt of dielectric loss. When used in equation 5 they transform it into the equation for the sheath temperature transient due to dropping one watt of dielectric loss.

If it is desired to calculate the temperature transient for some point in the insulation at radius x from the center of the cable, equation 4 is used but with the F_0 functions evaluated from Figure 9 for (u_nx, K_n). The final equation for the transient thus becomes

$$T_x=C_1F_0(u_1x, K_1)\epsilon^{-\lambda_1t}+C_2F_0(u_2x, K_2)\epsilon^{-\lambda_2t} \qquad (4A)$$

For the transient due to copper loss, the quantities C_1 and C_2 would be the same as determined from equations 2 and 3, that is, 3.56 and -0.414 respectively. For the transient due to sheath loss, they would have the values determined from equations 2A and 3A. For the transient due to dielectric loss they would have the values determined from equations 2B and 3B.

The procedure for handling problems in which three intersections occur on Figures 7 and 8 and the third intersection is not ignored will now be explained. Equations 2 and 3 are used as before for the transients due to change in copper loss, but they will have a third term, as follows

$$C_1F_0(u_1r, K_1)+C_2F_0(u_2r, K_2)+C_3F_0(u_3r, K_3)=R_i+E \qquad (2C)$$

$$C_1F_0(u_1R, K_1)+C_2F_0(u_2R, K_2)+C_3F_0(u_3R, K_3)=E \qquad (3C)$$

A third simultaneous equation is needed in order to be able to solve for C_1, C_2, and C_3. The third equation is written for any convenient point in the insulation, for example, at $x=(r+R)/2$. The right-hand side of the equation is the temperature rise at $x=(r+R)/2$ due to one watt of copper loss and is $p/2\pi \ln R/(r+R)/(2)$. The complete equation thus becomes

$$C_1F_0\left(u_1\frac{r+R}{2}, K_1\right)+C_2F_0\left(u_2\frac{r+R}{2}, K_2\right)+C_3F_0\left(u_3\frac{r+R}{2}, K_3\right)=p/2\pi \ln R/\frac{(r+R)}{2} \qquad (13)$$

The F_0 functions in equation 13 are evaluated from Figure 9, after which the three simultaneous equations 2C, 3C, and 13 are solved for C_1, C_2, and C_3. These three coefficients are then carried into the final equations. The final equation for copper temperature transient due to dropping one watt of copper loss becomes, for example

$$T_c=C_1F_0(u_1r, K_1)\epsilon^{-\lambda_1t}+C_2F_0(u_2r, K_2)\epsilon^{-\lambda_2t}+C_3F_0(u_3r, K_3)\epsilon^{-\lambda_3t} \qquad (4B)$$

Similarly the equation for sheath temperature transient due to dropping one watt of copper loss becomes

$$T_s=C_1F_0(u_1R, K_1)\epsilon^{-\lambda_1t}+C_2F_0(u_2R, K_2)\epsilon^{-\lambda_2t}+C_3F_0(u_3R, K_3)\epsilon^{-\lambda_3t} \qquad (5A)$$

For the transients due to sheath loss or dielectric loss, equations 4B and 5A are again used but with new values of C_1, C_2, and C_3 determined in the same manner as described for the case of two coefficients. In this case it will be necessary to evaluate the temperature rise at the point x in the insulation due to one watt of dielectric loss. This is given by the following expression

$$T_x=\frac{p}{4\pi(\ln R/r)}(\ln R/x)(\ln Rx/r^2) \qquad (14)$$

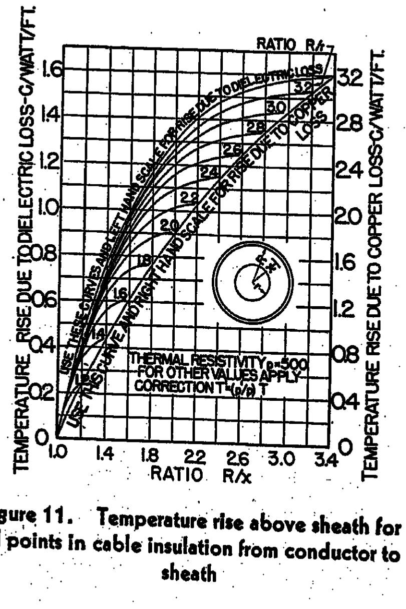


Figure 11. Temperature rise above sheath for all points in cable insulation from conductor to sheath

Figure 11 evaluates this quantity graphically, and also evaluates the temperature rise at any point X in the insulation due to one watt of copper loss, which is expressed by $T_x = p/2\pi \ln R/x$. It is to be noted that "any point x in the insulation" includes the conductor surface, for which $x=r$. Thus Figure 11 also will give the copper temperature for both copper loss and dielectric loss. The use of Figure 11 will save considerable calculation and avoid errors which might occur if numerical computation were used.

From all of the foregoing, it will be obvious that carrying a problem through in two coefficients is comparatively simple, whereas it is much more laborious for three coefficients. Hence the recommendation that the third term be dropped unless great accuracy is sought.

References

1. Thermal Transients and Oil Demands in Cables, K. W. Miller, F. O. Wollaston. *AIEE Transactions*, volume 52, March 1933, pages 98–110.

2. Temperatures in Electric Power Cables under Variable Loading, Elwood A. Church. *AIEE Transactions*, volume 50, September 1931, pages 982–92.

3. The Calculation of Cable Temperatures in Subway Ducts, W. B. Kirke. *AIEE Journal*, volume 49, October 1930, pages 855–59.

4. Theory of Oil-Filled Cables, G. B. Shanklin, F. H. Buller. *General Electric Review* (Schenectady, N. Y.), July and September 1931.

5. Load Ratings of Cable—II, Herman Halperin. *AIEE Transactions*, volume 61, part III, December 1942, pages 940–42.

6. Calculation of Electrical Problems of Underground Cables, D. M. Simmons. *Electric Journal* (East Pittsburgh, Pa.), May–November, 1932.

7. Thermal Properties of Underground Cables, P. Barenscher, A. R. McCann. *NELA Publication Number 267–91*, Edison Electric Institute (New York, N. Y.), August 1927, pages 267–91.

8. Current Carrying Capacity of Rubber Insulated Conductors, S. J. Rosch. *Electrical Engineering* (*AIEE Transactions*), volume 57, March 1938, pages 155–67.

9. Specifications for Impregnated Paper-Insulated Lead-Covered Cable, Solid Type. Association of Edison Illuminating Companies (New York, N. Y.) seventh edition.

Discussion

E. H. Grosser (Commonwealth Edison Company, Chicago, Ill.): This paper furnishes very useful tools to engineers faced with the problem of determining transient temperatures and oil demands of power cables. While the earlier paper by Miller and Wollaston[1] gave the essential theoretical basis for calculations of this type, the present paper puts this theoretical information into a directly usable practical form. In addition, an ingenious solution of the 3-conductor cable is given, using the equivalent single-conductor concept.

This method of calculation has been applied in the design of 3-conductor low-pressure oil-filled 66-kv cable lines in Chicago, totalling approximately 78 miles, of which 51 miles have been completed or are now under construction.

It is unfortunate that no means is available for including the effect of dielectric loss on the oil demand transient of the 3-conductor cable. The usual limiting case that determines line design from the standpoint of transient pressures is the very practical one in which a load-carrying line trips or fails. Dielectric losses, which may amount to as much as 10 to 12 per cent of the total losses of a 69-kv 3-conductor cable, thus are of importance. Perhaps the best way of including their effect is to design so that minimum pressures, using oil demands based on copper loss only, include some small allowances for additional pressure drop due to the dielectric loss transient.

The author states that the temperature of the oil in the oil channels of 3-conductor cable is probably close to the sheath temperature. We believe that the oil tube temperature can be represented more accurately as the temperature at a point with 20 to 30 per cent of the temperature rise between conductor and sheath. If the copper and sheath temperature transients are calculated, we may thus determine the approximate intermediate temperatures of the oil in the tubes as

$$
\begin{aligned}
T_o &= T_s + 0.25(T_c - T_s)\\
&= 0.75T_s + 0.25T_c
\end{aligned}
$$

where the subscripts o, s, and c denote temperatures of the oil, sheath, and conductor, respectively.

It is sometimes desirable to have an idea of the approximate peak value of the oil demand transient without taking the time to make a complete determination. This situation may arise in making preliminary estimates, or even in final design calculations where the amount of cable involved is very small, as, for example, in the termination of a long 3-conductor cable line with short lengths of single-conductor cable. The estimating curve of Figure 1 of this discussion has proved useful for this purpose. The abscissa is the total copper cross-section in the cable. For 3-conductor cables, it is the total area of the three conductors. The ordinate gives the maximum value of oil demand attained, a few minutes after the initiation of the transient, on assuming or dropping unit copper loss. This curve is based on the oil demands of actual cables as determined by calculation. The peak value of oil demand is thus shown to be roughly a function of the cable size, approximately independent of the voltage rating and number of conductors.

Reference

1. See reference 1 of the paper.

E. R. Thomas (Consolidated Edison Company of New York, Inc., New York, N. Y.): Mr. Wollaston is to be congratulated on preparing an excellent treatise on temperature transients in cable due to short time loading. Short time overloading on high-voltage cable has been of general concern to the industry at large in the last decade or so. However, I might point out that transient temperature phenomena was one of the fundamental factors required to be evaluated some 25 years ago in the early design of the a-c network. It obviously became necessary to design a secondary network system of cables where a short circuit would be capable of heating up a cable sufficiently rapid to melt a conductor of the cable in one branch of the circuit permitting that branch to burn clear without having developed too great heating in the parallel branch feeds of the network system to have damaged the rest of the network cable system. While these were temperature time transients generally much shorter in period than are being considered in this paper, they represent another type of thermal relation in which the distribution engineer is vitally concerned.

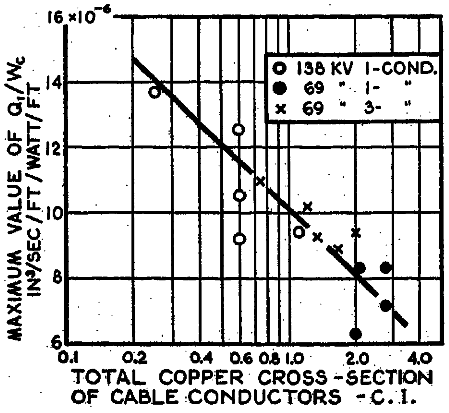


Figure 1. Transient oil demand of oil-filled cable due to dropping copper loss

M. H. McGrath (General Cable Company, Perth Amboy, N. J.): This paper provides a valuable and welcome addition to a 1933 AIEE paper by Miller and Wollaston. It not only contains important new material but, in addition, presents this material in a manner that will save much work in cases where temperature transients or oil demand calculations are involved, a matter of some consequence to those concerned with specific values rather than the general picture.

The new material, namely the new approach to the solution for the 3-conductor case, is of course quite important in itself, not only because of its practical value in duct lay installations but also because it suggests a possible basis of attack for the transient temperature and oil demand problem on pipe-type systems. The Wollaston method for three conductor is also quite timely in that it takes into account the fact that recent tests have failed to verify the generally accepted method of calculating the thermal resistance from copper to lead of a 3-conductor type-*H* cable.

Experience has shown one imperfection of the earlier Miller-Wollaston paper to be that the example given was, because of space considerations, condensed to the point where it is only with a very material expenditure of time and effort that an uninitiated computer can arrive at a suitable calculation format that will permit practical problems to be handled with speed and accuracy. The unabridged and unpublished report on which the earlier AIEE paper is based and which Miller and Wollaston made available to some of us, served at that time to offset to some extent this disadvantage. However, as time goes on, it has become increasingly evident that there was a very real need for something of a permanent nature in the AIEE record to amplify the brief example given previously. The example included by Wollaston in his present paper fills in this gap in admirable fashion. Although it applies specifically to the 3-conductor method which Wollaston has developed, the format used applies equally well to the single-conductor problem. Furthermore, Wollaston has materially reduced the computation work by showing that in many cases it is possible to obtain a completely satisfactory answer by using only two roots.

The earlier paper laid so much stress on the oil demand and other of the more complicated aspects of the oil-filled cable problem that it unintentionally concealed the fact, so clearly brought out in the new Wollaston paper, that the Miller-Wollaston methods provide a simple and at the same time the most accurate procedures yet developed for securing temperature attainment characteristics for use in determining emergency loadings for times up to the order of several hours on any type of duct installation.

In preliminary work on new projects one is frequently confronted with a number of alternate constructions. To carry through detail calculations for each alternate is often impractical. On the other hand, at least an approximate knowledge of the temperature or oil demand transient is often essential in making any significant comparative economic study of the alternates under consideration. The measured and calculated temperature attainment factor curves and the curves of unit oil demand transient given by Wollaston for the wide range of single and 3-conductor cable constructions employed by Commonwealth Edison Company will be invaluable in such cases because from this very extensive data one can obtain directly from one of the plots in the paper a reasonably accurate "guess" as to the general magnitude of either transient for most constructions of practical interest.

The engineer occasionally is confronted with a temperature transient or oil demand problem on a long submarine installation. It would appear that this problem can be readily approximated by the author's methods. Due to the excellent heat transfer by the water, there will be little if any thermal resistance between the sheath and the water. Thus the pertinent M_2, N curves of Figure 8 will be those shown for N=infinity. The intersection of these curves with the particular value of the M_1 group of Figure 7, will provide two roots. In setting up the simultaneous equations to determine the C coefficients, one would be similar to the author's equation 2 but with zero surface thermal resistance and the other equation would be written for, say, the midpoint of the insulation in much the same manner as suggested by the author for cases where three roots are employed on duct lay installations. The author's comments on this suggestion would be of interest.

Herman Halperin and C. A. Bauer (Commonwealth Edison Company, Chicago, Ill.): This paper is of considerable practical interest and value for various reasons, including the fact that it makes it feasible to determine accurately how much load can be carried within set temperature limits during a short-time emergency for 3-conductor cable. In addition to E. H. Grosser's discussion, the following remarks are made regarding the sections entitled Temperature of the Ducts and Sheath Thermal Resistance.

Many field measurements of duct temperatures cause agreement with the author that the duct temperature may be assumed as remaining constant during short-time heavy loading. In that period, the additional heat generated is so relatively small and the thermal time lag of ducts and associated earth is so extremely large that the change in duct temperature will be only a few per cent of the total ultimate possible change.

The author in his discussion in Item 4 of Table III may not seem explicitly clear as to the value to use for thermal resistivity of the sheath in determining the surface thermal resistance from sheath to the duct structure. He gives a value of 1,200 in the table which seems reasonable for determining the thermal resistance from the sheath to an unoccupied duct. In calculations using the thermal resistance of sheath to the surface of the duct in which the cable is located, the constant should be perhaps 550.

Regarding author's figures on thermal resistance from the inner surface of an occupied duct to the air in an adjacent idle duct, field measurements have indicated that at locations where the duct structure is fairly moist, the lower value applies, while at quite dry locations the higher value applies.

H. D. Short (Canada Wire and Cable Company Limited, Toronto, Ont., Canada): We have in the past found the Miller-Wollaston thermal transient formulas very useful tools in the practical design of high-voltage Pirelli-type single-conductor oil-filled cable lines in Canada. Mr. Wollaston's extension of these formulas to the case of 3-conductor cables is indeed most appropriate. In the 3-conductor case one is confronted more particularly with many practical applications where short-time current ratings are very valuable to the cable user in assessing fully the earning capacity of a given cable line. More especially is this true today when capital costs are high and the tendency prevails to maintain the present selling price of electric energy. From these points of view the author's method of computing the short-time current rating of 3-conductor cables is a most salutary contribution.

It seems in the nature of things to suggest here, as a worthwhile consideration, the inclusion of overload ratings in the various publications of cable current ratings. These could take the form of 1-hour, 2-hour, and possibly 3-hour overload ratings based upon several a priori loading conditions in respect of the maximum continuous current rating.

In regard to the determination of a hypothetical single-conductor equivalent to any given 3-conductor cable the thought occurs to me that one has to consider not only the thermal field but also the electric field, since they are closely inter-related and their corresponding potentials both obey the homogeneous Laplace equation for 2-dimensional flow. Thermal flux is analogous to electric flux and for any given configuration the same potential function is manifested, thereby establishing the well known equivalency of thermal resistance and electrical elastance. Hence I would like to propose the following method of determining the dimensions of the hypothetical single conductor which obviates the need of choosing a value for either D or d (the paper's nomenclature), and which brings to bear the effect of the electric field. It may be shown that

$$R_{TH1}C_s = \frac{pk}{4}$$

where

R_{TH1} = thermal resistance of the three conductors of the given 3-conductor cable in parallel to the lead sheath outer surface
= R_t of the paper
C_s = electric capacity between the three conductors of the given 3-conductor cable connected together as one electrode and the sheath as the other electrode, (electrostatic units per centimeter)
p = thermal resistivity of the insulation in question
k = dielectric constant of the insulation in question

Knowing R_{TH1}, p, and k, C_s may be readily found, and taking this value of capacity as that of the hypothetical single-conductor cable the following transpires

C = electric capacity of the hypothetical single-conductor cable

$$= \frac{k}{2 \log_e \frac{D_i}{d_h}}$$

$$= C_s$$

and

$$\log_e \frac{D_i}{d_h} = \frac{k}{2C_s}$$

where D_i and d_h are respectively the diameter over the insulant and over the conductor of the hypothetical single-conductor cable

Now in order to assimilate in the hypothetical single-conductor cable the electric field obtaining in the 3-conductor cable between the outer surfaces of the three conductors and the lead sheath we may proceed as follows

Let g_c' = stress on the surface of the conductor in the 3-conductor cable towards the sheath
and g_c = stress on the surface of the conductor in the hypothetical single-conductor cable

$$= \frac{E}{r_h \log_e \frac{D_i}{d_h}}$$

where

E = voltage to neutral

and

$$r_h = \frac{d_h}{2}$$

putting

$$g_c = g_c'$$

we obtain another expression for $\log_e \frac{D_i}{d_h}$ thusly

$$\log_e \frac{D_i}{d_h} = \frac{E}{r_h g_c'}$$

hence

$$r_h = \frac{2EC_s}{g_c' k}$$

from which the corresponding value of D_i may be readily computed.

Maybe this approach is a bit too pendantic, but in any case I should like to have Mr. Wollaston's comments in respect to its practical significance to the temperature phenomena in hand.

I should like to know whether the values of R_t given in Figures 4 and 5 are measured or calculated values; and if calculated what value of thermal resistivity was used therein. In addition, what value of thermal resistivity was used in calculating the time-temperature rise data of Figures 1 and 2; were the specimen cables used to determine Figures 1 and 2 new or old cables?

F. O. Wollaston: Mr. Grosser has discussed the effect of dielectric losses. The oil demand due to dielectric loss change in a 3-conductor shielded cable can be computed by the device mentioned in the section on Temperature Transient of 3-Conductor Cables, that is, by treating each core of the cable as a separate single-conductor cable. This component of oil demand can then be added to the component due to copper loss computed by the equivalent single-conductor method. This procedure would double the computing labor and would contain some error but may be worthwhile in the circumstances stated by Mr. Grosser. Mr. Grosser's finding that peak oil demand has a consistent relationship to total copper cross-section in cables of various designs is surprising to me. It will be valuable for estimating purposes.

I am indebted to Mr. McGrath for pointing out the application of the method to submarine cables. It would not be necessary to write a new equation for the midpoint of the insulation. Equations 2 and 3 are correct and sufficient. It would only be necessary to set the sheath thermal resistance E equal to zero in these two equations and proceed in the manner described in the paper. It may be helpful in this connection to note that equations 2 and 3 are the general transient temperature equations 4 and 5, written for $t=0$, that is, for the steady-state condition preceding the transient. The steady-state condition is a convenient known boundary condition which the general equations must fit correctly. In equations 2 and 3 we are determining the proper values of the constants C_1 and C_2 so that this fit will take place. In the case of submarine cable, the steady-state copper temperature rise above ambient for 1 watt of copper loss is correctly stated by equation 2 with E set equal to zero. The steady-state sheath temperature rise above ambient for 1 watt of copper loss is correctly stated by equation 3 with E set equal to zero, that is, the sheath temperature rise is zero. Accordingly these equations will correctly determine the constants C_1 and C_2. Of course submarine cable would usually be armored, with jute bedding under the armor and perhaps over it. The jute bedding has some thermal resistance and thus E in a practical case would be larger than zero.

Mr. Short's use of the electrostatic field to calculate the diameter of the equivalent single conductor is ingenious, but I do not see how it can be applied to shielded cable. The electrostatic field is not analogous to the thermal field in shielded cable, a fact which I endeavored to point out in the section headed Treatment of Dielectric Losses in Transient Problems. The values of R_t given in Figures 4 and 5 are calculated in some cases and experimental in others. For the calculated values, p in watt-centimeter units was taken as 550 for all the oil-filled cables, and as 650 for all the solid cables except the 66-kv single-conductor cables numbers 5, 6, and 7 in Figure 1, for which p was taken as 600. The value of p in the calculations for the cables in Figures 1 and 2 was 650. The cables were new but had been subjected to a number of load cycles on test. The terminations of the test lengths of cable were pressure tight and high internal pressures and some sheath expansion occurred during the tests before the experimental attainment factors given in Figures 1 and 2 were logged.

Mr. Halperin and Mr. Bauer state that the sheath thermal resistivity should be taken as about 550 instead of 1,200 when calculating the resistance from the sheath to the occupied duct. I am aware that Commonwealth Edison Company have a wealth of Chicago field observations which support this contention and which were discussed in one of Mr. Halperin's papers.[1] However, I have been unable to convince myself that these findings for Chicago would be generally applicable to other systems. The references on this subject listed in my paper state explicitly that the thermal resistivity of sheath (B) should be taken as 1,200 in calculating the thermal resistance from the sheath to the occupied duct and also from the sheath to still air. Reference 8 gives a good deal of laboratory test data supporting this value. In a recent paper[2] which was not available when my paper was written, J. H. Neher states that for fiber duct in concrete and cable losses in the neighborhood of 10 watts per foot, it is correct to calculate the sheath thermal resistance to the occupied duct wall by the formula given in my paper and taking B as 1,200. In working up my numerical example I assumed these conditions, that is, fiber ducts and moderate cable losses, although it was not so stated. It seems significant that Neher found that B should be much lower for ducts of higher thermal conductivity, for example 800 for terra cotta ducts. In Chicago the ducts are precast concrete and are quite generally damp or even very wet. It may be that the low value of B found in Chicago is peculiar to that type of duct, which is not generally used elsewhere and has not generally been used in laboratory tests. This suggestion is not entirely satisfactory, however, since Rosch, ref. 8 of the paper, found that B for cables in metallic conduits was in the order of 1,000 to 1,100. For such conduits, B should have been abnormally low, due to the low thermal resistance of the duct material. Another point which may be significant is that the Chicago data are for field conditions and we may expect that the air in the occupied ducts was heavily saturated with moisture. In the laboratory tests where B has almost invariably been found to be around 1,200, the surrounding air was presumably much drier. To sum up, I believe I have used the generally accepted and conservative value for B, but I feel that much more work will have to be done on the subject before we can all be sure of our ground and get into general agreement with each other.

REFERENCES

1. LOAD RATINGS OF CABLE, Herman Halperin. *Electrical Engineering* (*AIEE Transactions*) volume 58, October 1939, pages 535–52.

2. THE TEMPERATURE RISE OF CABLES IN A DUCT BANK, J. H. Neher. *AIEE Transactions*, volume 68, part I, 1949, pages 540–48.

Diagnosis of Rectifier Ailments

I. K. DORTORT
MEMBER AIEE

THE mercury-pool rectifier is a reliable machine fully accepted by industry. Incorrect operating conditions and internal deterioration lead to internal faults which are cleared by the protective gear. Unlike most other machines, the rectifier can be returned to service immediately after the fault is cleared. Now, even though no change has taken place in the circumstances which produced the fault, the rectifier can operate a long time before another failure takes place.

The explanation appears to be that a very improbable set of coincident phenomena have to occur to produce a failure. If this sounds far-fetched, consider the tremendous number of anode cycles which transpire without incident between faults, even in a very sick rectifier. A 12-anode rectifier with a bad record of one arc-back per day is averaging one bad anode cycle in 62 million.

Nevertheless, improper environment and internal disorders must be diagnosed and corrected early, and it is very important to the user that it be done with the least loss of service and minimum expense.

This paper is an attempt to assemble and correlate the known facts and experiences to provide a guide to a logical diagnostic procedure.

Scope

The most common type of failure is the arc-back, colloquially called a backfire. An occasional arc-back is no cause for concern about a rectifier properly applied and protected. However, almost anything that can go wrong with a rectifier or its associated equipment will eventually induce a progressively increasing arc-back rate. While the harm done by any one arc-back is very slight, the damage is cumulative. In the interest of continuity of service and minimum maintenance costs, the cause must be discovered and corrected as soon as possible.

In inverter service some of these same causes generally lead to arc-throughs or "spillovers." They also may induce arc-backs which are of little consequence but may themselves produce arc-throughs and interruptions. The detection of the faulty anode and the determination of the cause follows very closely the procedure to be outlined for arc-backs. Because of this and because inverters are still relatively few in number, diagnosis of arc-throughs will be treated chiefly by annotation.

Misfires, peculiar to Ignitrons, and excitation failures in general, are not particularly difficult to diagnose as a rule, and are therefore not included in this paper. However, some of the material contained herein will be found applicable.

Most other troubles that may cause service interruption (other than power short circuits) will be found lurking in control devices or control circuits, and do not differ from those experienced with comparable controls of other machines. They are outside the scope of this paper.

It appears then that the only subject remaining to be discussed here is the isolation and analysis of the causes of arc-backs. It will become apparent that the scope has not been unduly restricted.

Causes and Some Effects

Before we go into the discussion of procedure, it will be helpful to review the direct and indirect causes of arc-backs. Everyone interested in rectifiers already knows that an arc-back is a failure of the check-valve action, permitting a reversal of current flow between cathode and anode; in other words, the anode emits electrons and becomes in fact a cathode. This phenomenon may be caused by many things, such as poor vacuum, overtemperature, overloads, including those due to unbalance, high initial inverse voltage, voltage surges, deterioration of insulation, traces of foreign material on the anode, etc. An arc-back constitutes an unbalanced short circuit across the secondary winding of the rectifier transformer with reverse current in the faulted phase reaching values as high as 100 times the normal forward current.

In an otherwise sound rectifier the probability of arc-backs increases as a function of the residual ion concentration under the anode immediately after the completion of commutation, and of the value of the inverse voltage which appears suddenly between anode and cathode at that instant. The residual ion concentration depends, among other things, on the rate of decrease of the anode current near the end of commutation, and this rate of decrease in turn depends on the load, the commutating reactance, and phase control. The residual ionization also depends upon mercury vapor pressure and the presence of other gases. Again, the initial inverse voltage depends on load, commutating reactance, and phase control. An increase of any of these factors will increase the probability of arc-back.

Symptoms

In the initial phases of operation of a new rectifier, some arc-backs are to be expected. If the unit backfires excessively (or arcs-back, if you prefer), the seat of the trouble may be in the rectifier or in the rectifier transformer; it may be circuit trouble and may even be located in the connected a-c or d-c systems. The boundary between reasonable and excessive backfiring cannot be defined by formula, depending as it does on design, type of service, and past history. Trouble

---

Paper 49-270, recommended by the AIEE Electronic Power Converters Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted March 2, 1949; made available for printing September 7, 1949.

I. K. DORTORT is with the Allis-Chalmers Manufacturing Company, Milwaukee, Wis.

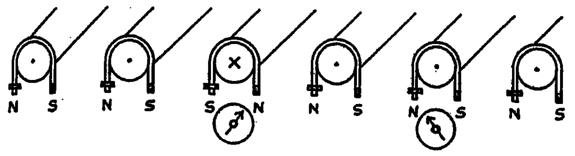
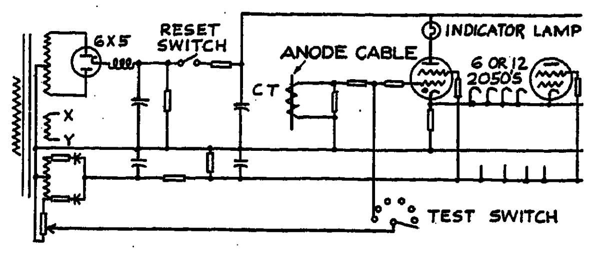


Figure 1 (left). U-bolt arc-back detector

Figure 4 (right). Electronic arc-back detector



developing on rectifiers with past records of satisfactory service should be much easier to diagnose, since it can be assumed that the record is proof of the soundness of application and engineering.

Whatever base is used as a criterion, the arc-back rate is our most important symptom, signaling the existence of one or more unfavorable conditions. The arc-back itself is not visible so we depend on external manifestations, such as the tripping of circuit breakers, to announce their occurrence. With very few exceptions industrial rectifier installations include a cathode or an anode circuit breaker or both, and an a-c circuit breaker. If the d-c machine circuit breaker trips only on reverse current and the d-c system is capable of feed-back, an arc-back will be announced by the opening of the circuit breaker with a report that one is inclined to accept as sufficient proof. Nevertheless, it is advisable in all cases to analyze the control and protective systems of all circuit breakers involved to establish the validity of assumptions based on circuit breakers operations.

Poor vacuum, another important symptom, may be caused by a leak, loss of pumping capacity, or the presence of foreign volatile matter in the vacuum system. This symptom requires immediate investigations, as continued poor vacuum will result in rapid deterioration of the rectifier.

Abnormally high or low temperatures also may lead to arc-backs. Wide variations of temperature among anode or anode cables indicate severe current unbalance conducive to arc-backs.

High transformer temperature may be caused by overload or loss of interphase transformer function. Saturation of the interphase transformer due to anode unbalance or a short circuit in the interphase winding will generally produce arc-backs. It also may double the copper losses of the main transformer winding.

A marked change in the noise produced by the transformer, other than that caused by changes of load conditions, is a danger signal. This is particularly true if the noise becomes rough. Noisy anode-balancing reactors generally indicate poor anode current balance and sharp clicks or thumps indicate excitation failures, misfires, or arc-throughs. An arc-back also will cause a sharp noise in an anode-balancing reactor, but usually is drowned out by the noise of the circuit breakers unless the arc-back is cleared by arc suppression without operation of the circuit breakers.

The symptoms described previously are directly observable without special equipment.

The following also have deviations from the normal: anode current balance; anode current wave-shape; anode-to-cathode voltage wave-shape; grid current wave-shape; grid-to-cathode voltage wave-shape; interphase transformer voltage wave-shape; and internal insulation resistances.

The most important thing to look for in these quantities and wave-shapes, and the most easily spotted, is dissymmetry of any kind among anodes and grids.

Instruments and Aids

Below are listed a number of standard and a few special devices which can be used to observe and record various sustained and transitory symptoms. These are in addition to those with which the rectifier unit is organically equipped.

1. Clip-on ammeter or other means of determining anode current balance.
2. Megger—preferably 1,000 volts.
3. Oscillograph, magnetic or cathode ray.
4. U-bolts and compass to catch current reversal.
5. Surge crest ammeter.
6. Special electronic arc-back detector for logging the distribution of arc-backs or arc-throughs among the anodes.
7. Memory attachments for oscillographs.

The usual assortment of test voltmeters, ammeters, thermometers, and a McLeod vacuum gauge are assumed to be on hand.

It will be noted that all but the first three items on the list provide a "memory" of transitory phenomena. Sitting up with a sick rectifier to catch randomly occurring momentary symptoms can be a very tedious job.

Procedure

In order to illustrate most effectively the various phases of an investigation, we will assume a typical case of trouble and follow a step-by-step procedure to determine the cause. Instruments and aids will be described and their use explained as they are called for in the procedure.

We will assume for our purpose a rectifier that has been operating satisfactorily until recently, averaging less than two or three arc-backs per year. Over the last few months this rate has increased to three or four per month. The circuit breakers have reclosed properly each time so there is not as yet any interference with operation. However, the outages all seem to occur during the peak load hours, and should the rate continue to increase, serious service interruptions are to be expected.

To start at the beginning, if we assume the station unattended, the only fact known is that unscheduled circuit breaker operations occur, and targets are found down on the instantaneous overload relays after each interruption.

ANALYSIS OF CIRCUIT BREAKER OPENINGS

The first step then is to determine whether or not the outages are caused by arc-backs. The d-c circuit breaker is equipped with a built-in reverse current trip and a shunt trip coil. There is no overload trip, overloads being cleared by the feeder circuit breakers. Opening of the a-c circuit breaker, water failure, and excitation failure will act on the shunt

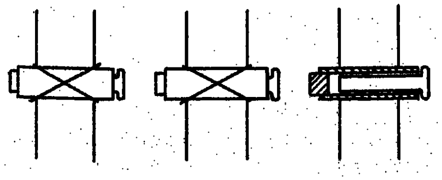


Figure 2. Magnetic links for surge-crest ammeter, installed on anode cables

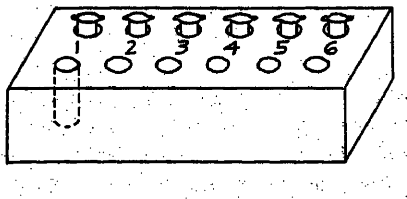


Figure 3. Magazine for magnetic links

trip of the d-c circuit breaker. The only light found on the annunciator is the one indicating an unscheduled opening of the circuit breakers. There is then sufficient evidence to justify the assumption that the outages are caused by arc-backs. If this is not so, the ensuing investigation can be expected to show this during the early stages.

Vacuum, temperature, and all other observable conditions have been checked and found correct. However, no seepage test has been made on the rectifier because this would require a shut-down.

ANODE CURRENT BALANCE

The next test that can be made without shutting down the rectifier is a check of the anode current balance.

Although clip-on ammeters are particularly inaccurate on unidirectional pulse currents such as flow in the anode circuits, they are convenient and generally used to check anode current balance. Care must be exercised in positioning the clip-on ammeter, as neighboring cables have an influence on the reading and even the location of the cable within the window of the ammeter iron and the angle of the ammeter with respect to the cable may cause large errors in the readings.

If greater accuracy is desired and if suitable sections of the anode connections are accessible, a d-c millivoltmeter is much more satisfactory. It should preferably be shielded and used in conjunction with two probes rigidly mounted on a bar of insulating material.

A fluctuating load makes accurate reading of instruments difficult and may introduce considerable error. This error is minimized by taking readings as the total d-c load holds momentarily or passes slowly through a selected value. If the load fluctuates too wildly to follow this procedure, an attempt is made to catch simultaneous readings on the d-c ammeter and clip-on ammeter for each anode in turn, and proportional corrections are applied to the anode current readings.

Between half and full load, unbalances up to 5 per cent above or below average are acceptable. If anodes are paralleled through reactors, deviations up to $7^{1}/_{2}$ per cent can be expected, but should improve with increasing phase-control.

At light load the unbalance will be worse than at the higher loads. Should any anodes drop out and not carry current for appreciable intervals, arc-backs are to be expected.

Even when the percentage of unbalance is within permissible limits, any change for the worse in comparison to previous records should be investigated.

We will assume here that anode current balance is satisfactory at high and low loads.

DETERMINATION OF ARC-BACK DISTRIBUTION

If it is permissible to shut down the rectifier over week ends, the next logical steps would be the seepage and megger tests. Assuming it is desirable to postpone a shutdown as long as possible, preparations are made to determine next the distribution of arc-backs among the anodes.

U-Bolts and Compass

The U-bolt can hardly be classified as a scientific instrument, but together with an ordinary compass it provides a simple and certainly an inexpensive means for determining which anode has backfired.

One U-bolt is tied to each anode cable in as uniform and symmetrical an arrangement as possible. One end of each U-bolt should be marked so no mistakes will be made when they are removed for checking. They should be mounted on the cables with markings always in the same direction as shown in Figure 1.

Anode currents during normal operation will magnetize the U-bolts in one direction, whereas the heavy reverse current in the faulted phase will reverse the polarity of the U-bolt on that phase during arc-back.

The reversal of polarity can be checked easily with an ordinary compass. If there is sufficient distance between U-bolts, the polarity can be determined without removing the bolts from the cables. In most cases it is necessary to remove them to obtain reliable indications. The check must of course be made before the rectifier is returned to service, as otherwise the record may be erased.

Surge Crest Ammeter

The U-bolt is at best a makeshift affair for this purpose. The surge crest ammeter is more reliable, easier to use, and in addition to polarity provides a relative measure of the currents involved. With proper calibration a fair measure of the actual crest current can be obtained.

The complete surge crest ammeter consists of three component parts: (1) any desired number of magnetic links; (2) an indicating instrument with a holder in which the magnetic links are placed for measurement; and (3) a demagnetizing coil through which each link is passed after the measurement has been made and before the link is used again.

The most convenient way of holding the magnetic link against the anode cable is to fasten a small piece of tubing of nonmagnetic material to the cable and at right angles to its axis. One end of the tube should be closed, so the link has to be inserted from the same end each time and the diameter of the tube should be such that the nibs provided at one end of the link will not go into the tube. This arrangement makes it very easy to remove the magnetic links for measurement and return them with proper direction assured, see Figure 2.

To permit restoration of service with a minimum loss of time and provide definite association of each meter reading with the proper anode, it is common practice to use twice as many links as there are anodes so that a demagnetized replacement link can be substituted for each one as it is removed from the anode cable. A convenient magazine for the links is made by drilling two rows of holes in a block of wood, one hole for each link on the cables and one directly beneath it for the demagnetized replacement, as in Figure 3.

In the event of an arc-back and before the unit is restored to service, each link is removed in turn from the anode cable and inserted in the magazine in the correspondingly marked position. Its replacement link from the other row is removed from the block and inserted in the holder on the cable. Twelve links can be changed easily in one minute.

Using either the rough-and-ready U-bolts or the more refined surge crest ammeter, the distribution of arc-backs among the anodes is tabulated. With the surge crest ammeter it is also possible, with somewhat less reliability, to determine the distribution of arc-throughs in inverter service. The magnetic link on the anode which last carried arc-through current will probably give the highest reading in the forward direction. It will not tell if the arc-through was due to loss of control on that anode or if the next succeeding anode failed to pick up at the proper time.

The surge crest ammeter can be tried with immediate reclosure of the breaker if not too much time elapses before the links are checked. Otherwise there is a chance that the record will be obscured or completely erased.

Electronic Arc-Back Detector

For operating conditions where the rectifier must be returned to service immediately, either automatically or manually, or where other conditions make it impracticable for an operator to get at the

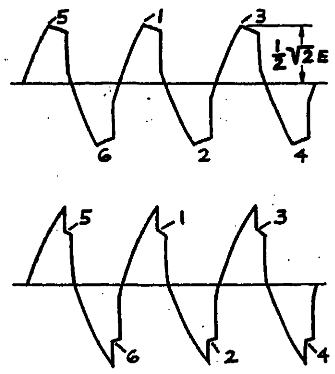


Figure 5(A) (top). Full interphase transformer voltage with no phase control

Figure 5(B) (bottom). Interphase transformer voltage with phase control

links installed on the anode cables, an electronic device has been built and used successfully, which lights a signal lamp corresponding to the phase on which the fault occurred. The diagram for the device is shown in Figure 4.

The circuit consists essentially of a number of thyratron tubes, six for a 6-phase rectifier, 12 for a 12-phase rectifier, and so forth, and a small d-c power supply. The grid of each tube is connected to a through-type current transformer in one of the anode leads. The tubes are normally blocked by negative bias on the grids. On the occurrence of an arc-back (or arc-through if an inverter is being investigated) the current transformer in the faulted phase produces a positive impulse on the grid of the corresponding tube and releases it. The resulting current flowing through the common cathode resistor increases the negative bias on all grids, so that all tubes except the one triggered are blocked. That tube will continue to fire until reset. The plate current is used to light a small signal lamp on the front of the cabinet.

In some cases two lamps may be lit simultaneously, but only the one corresponding to the leading phase should be recognized. Although arc-backs may occur on more than one anode for any given fault, it is improbable that they will be initiated at the same instant, and the one that leads off will most likely be the troublemaker.

A system using a large number of rectifiers would well justify building an electronic arc-back detector.

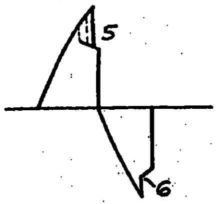


Figure 6. Interphase transformer voltage, showing erratic pick-up of anode 5

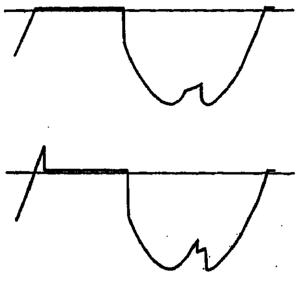


Figure 7(A) (top). Anode-to-cathode voltage with no phase control (double Y with interphase transformer)

Figure 7(B) (bottom). Anode-to-cathode voltage with phase control (same connections as 7A)

Failure to detect any arc-backs, even though circuit breaker openings continue to occur, calls for a review and thorough investigation of circuit breakers, protective relays and settings, interlocks, and control circuits. Power circuits all the way from the a-c circuit breaker to the d-c load should be checked carefully. The rectifier transformer should be scrutinized for intermittent faults. If oil-immersed, the oil should be checked for indications of under-oil arcing.

To avoid a premature ending, we will assume that arc-backs are taking place and most, but not all, are registered on anode 5. The cause is yet to be determined, with particular emphasis on its location in or external to the rectifier.

Oscillographic Analysis

As the cause of the arc-backs may lie outside the rectifier, it is still desired to avoid what might be an unnecessary shutdown. So we choose oscillographic analysis for our next step in the investigation.

The cathode-ray oscillograph, or oscilloscope, is one of the most useful and versatile implements in the investigation of rectifier disorders. The magnetic oscillograph is more cumbersome in every way and does not have the sensitivity of the oscilloscope. It does permit viewing as many as six separate quantities simultaneously and provides photographic film for careful study. However, the oscilloscope also can be equipped with a camera and is often used in that manner.

Either type of oscillograph is useful in investigating the important voltage and current wave-shapes listed in the chapter on symptoms.

The cathode-ray oscilloscope should be tried in several different positions to determine to what extent the image is being affected by stray fields. Twisted leads are important to avoid pick-up.

When using either instrument every precaution should be taken to insure personal safety. Whenever possible, the oscillograph is used in a grounded circuit and connected across the grounded end of a potentiometer if the voltage is high. These conditions, however, can seldom be fulfilled. It is wise to place the oscillograph on an insulated table and operate from an insulated stool or mat. When used on rectifiers of 600 volts, or even 250 volts, it is well to take the power supply of the cathode-ray oscilloscope through a small insulating transformer.

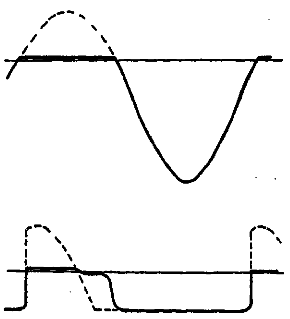


Figure 8(A) (top). Grid-to-cathode voltage, sinusoidal wave, with small amount of bias phase control

Figure 8(B) (bottom). Grid-to-cathode voltage, chopped sinusoidal voltage wave, negative loop suppressed

The first thing to look for is any dissymmetry or other irregularity in the firing of the anodes. The easiest way to do this is to check the wave-shape across the interphase transformer. Figures 5(A) and 5(B) show the normal wave-shape of the interphase transformer voltage, without and with phase control, for a 6-phase double-Y rectifier transformer. The sweep of the oscilloscope is adjusted for one cycle of the primary frequency so that each of the three positive and three negative loops of the curve will correspond to one of the six phases of the rectifier transformer secondary. The phases will, of course, appear in sequence, but the first loop will not necessarily coincide with phase one.

By various means it is possible to identify one loop of the resulting curve with its corresponding anode and from that all other loops can be properly identified. The sweep and synchronizing circuit of the oscilloscope must be stable, as any drift of the image across the screen will make identification almost impossible.

A quick way to identify a particular phase with a loop of the interphase transformer voltage wave is to disconnect momentarily the grid or grids of the anodes connected to that phase. The leading slope of one of the loops will continue forward, past the point where it normally breaks, to complete a sine wave. The point at which this particular curve normally breaks is the point at which the anode which was disabled would normally have picked up. This method applies only if the grids have a positive characteristic. This check should be made at light load.

Many other means of identification are possible. A sure way with less risk than the above is to determine the pick-up point of a given anode from either its voltage or current wave, without changing the sweep or synchronization.

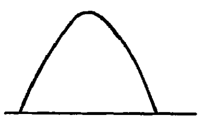
The numbered points on curves, Figures 5(A) and 5(B), indicate the time at which the anodes connected to correspondingly numbered phases pick up.

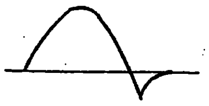
Any deviation of the trace from the general character indicated or any difference between loops is cause for suspicion and further investigation.

Any difference in the height of the pips above the loops in Figure 5(B) denotes differences of phase control among the phases. Such differences will generally cause unbalance of anode current and may be caused by trouble in the phase control circuit, in the grid insulation, or even a break in the internal connections between the grid inlet bushing and the grid. A trace such as shown in Figure 6 is caused by erratic pick-up and may be caused by external or internal disorders.

Marked differences between the positive and negative loops indicate d-c saturation of the interphase transformer, and are generally caused by an unbalance between the two 3-phase systems. A marked reduction in the amplitude of either the positive or negative loops, or both, might be caused by one or more shorted turns in the interphase winding. The peak voltage across the total interphase winding, without phase control, should be exactly one-half of the peak voltage of the transformer secondary phase-to-neutral voltage. If this value is not attained, it means the interphase transformer is not fully effective. Should the interphase transformer be inoperative for any reason, the transformer will, if it has a delta primary or delta tertiary, act like a 6-phase star, producing anode currents of only 60 degrees' instead of 120 degrees' duration, and of twice the normal amplitude. This condition will be productive of arc-backs.

The foregoing illustrates why the interphase transformer voltage offers excellent advantages for a quick over-all check of the operation of the rectifier. It has limitations, particularly when two anodes are paralleled through anode reactors, and of course in many instances the interphase transformer terminals are not readily accessible.





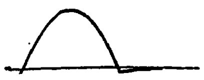


Figure 9A (top). Grid current for voltage as applied in Figure 8(A)

Figure 9B (center). Grid current, same as Figure 9(A), except for 15 per cent phase control

Figure 9C (right). Same as Figure 9(B), except grid has partially lost control

More specific investigations with the cathode-ray oscilloscope can be made directly on the anode and grid circuits of the rectifier. The anode-to-cathode voltage wave-shape, observed on all anodes, will disclose facts that cannot readily be deduced from the interphase transformer voltage. However, even using an electronic switch, it is not possible with standard equipment to observe all anodes simultaneously and comparison becomes more difficult unless the load is reasonably steady. The magnetic oscillograph is more useful for this work.

Figures 7(A) and 7(B), respectively, show the normal anode-to-cathode voltage without and with phase control. The particular points of interest are at the beginning and end of the conducting periods; the beginning showing any discrepancy in phase control, and the end showing the initial inverse voltage, which has a great effect on the arc-back rate.

The anode current wave-shape can be obtained by using current transformers or shunts, and the shunt may consist merely of a suitable length of the anode connection conductor. Differences of wave-shapes between anodes should be investigated.

Among the most revealing checks that can be made on the rectifier itself are the wave-shape of the grid-to-cathode voltage and grid current. A comparison of the grid-to-cathode voltage will reveal any discrepancy in the grid circuits, whether external or internal. Figure 8(A) shows a typical grid-to-cathode voltage where sinusoidal voltages are applied and Figure 8(B) shows a typical grid-to-cathode wave-shape for peaked voltages. Figures 9(A) and 9(B) show typical currents for sinusoidal voltages. In Figure 9(C) the grid has partially lost control.

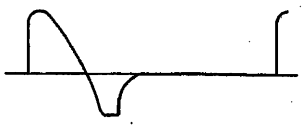
Unlike anode currents, the amplitudes of the positive and negative grid currents are of the same order of magnitude permitting ready observation of the deionizing current at the grid. Figure 10(A) is a typical curve showing the rapid decay of deionizing current at the grid immediately after completion of the anode-conducting period. In Figure 10(B) the deionizing period is dragged out almost to the beginning of the next positive cycle. This condition may be inherent in the particular design but may also be caused by poor vacuum, improper cooling of the internal parts of the rectifier, or deteriorated grid insulation. It also may indicate a broken grid. If all grids of the machine show exactly the same characteristic and have not changed from the time when the machine was performing satisfactorily, no special significance should be attached to this image. For average rectifier service, stress should be placed on the uniformity and constancy of the deionizing current form rather than on the form itself. As a rule, the higher the rated voltage of the machine, the shorter the deionizing time should be. For inverter service, in particular, deionization must be complete before the step-limit is reached, that is, the point at which the anode becomes positive with respect to the cathode, and the total time available for deionization is generally quite small. A relatively small increase of deionizing time will cause arc-throughs.

Figure 6, showing erratic pick-up on phase 5, calls for a careful check of the grid circuits of that phase. If all external components are found in good order, the next step will be the megger test. A short shut-down is required.

Megger Test

The 1,000-volt megger has been found to be most reliable, although the 500-volt megger is used more often because of its wider distribution. The 1,000-volt megger can be expected to discover weaknesses that do not show up on a 500-volt test.

The megger is used here primarily to



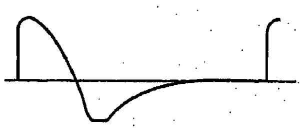


Figure 10(A) (top). Grid current for voltage as applied in Figure 8(B)

Figure 10(B) (bottom). Same as Figure 10(A) except for long deionizing time

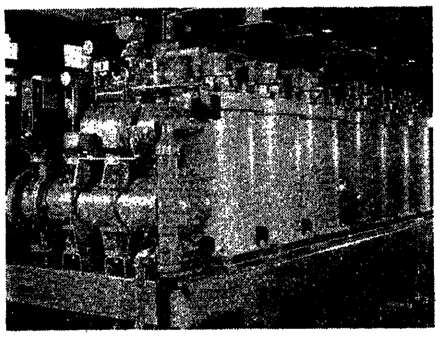


Figure 11. Rectifier with arc-back probability of less than 1 in 23 billion anode-cycles

test the internal insulating members of the rectifiers. Any external connections, electric and water, that might influence the readings should be removed.

Special care should be taken to see that the megger leads are supported so that they do not touch each other, ground, or anything that might influence the readings, as insulation resistances can be expected to run over 2,000 megohms.

As far as possible all insulating members should be tested and a record made of readings between anode and tank, grid to tank, tank to cathode if the cathode is insulated as on multiple-anode rectifiers, and tank to cooling coil where the rectifier has an internal cooling coil insulated from the tank. The value of insulation measured for the various component parts is a function of design and service. Significant differences between anodes of the same rectifier and between rectifiers of the same type should be investigated.

In certain designs of multianode tanks there are insulating members that have critical influence on the arc-back rate but cannot be checked from outside the rectifier by the usual means. One such insulating element is the support of the grid in the anode housing, and another, rarely used today, is a petticoat on the anode insulator serving as support for the anode housing.

With excitation established in the rectifier tank and the positive (usually grounded) terminal of the megger on the cathode and the negative (ungrounded) terminal on anode or grid, a good comparative measure of the respective insulating members can be obtained. The ionized mercury vapor provides an electric connection between the cathode and the anode housing.

Seepage Test

Again we assume that all results have been negative up to this point, and it is finally decided to shut down the rectifier to make a seepage test. For dependable results, the seepage rate should be averaged over at least six hours. The machine must be held at constant temperature, preferably room temperature, throughout the test. This means the rectifier will be out of service 10 to 12 hours or longer.

A McLeod vacuum gauge is generally provided for making seepage tests. With proper precautions a fair test can be made with the electric gauge but this is not recommended.

The general procedure for single-anode rectifiers is as follows:

1. Cool rectifier to room temperature, allowing sufficient time for internal parts to cool.
2. Pump rectifier down to best obtainable vacuum.
3. Close individual vacuum valves on rectifier tanks and the main vacuum valve.
4. After a period of not less than six hours, take a reading of the vacuum in the vacuum header. The rise of pressure in the header should not exceed 5 microns per hour.
5. Open main vacuum valve and restore good vacuum in the header.
6. Close main vacuum valve.
7. Open valve on one tank. After five minutes read vacuum. The increase in pressure divided by the number of hours the tank valve has been closed is approximately the average seepage rate. Close tank valve.
8. Repeat 5, 6, and 7 for each tank in turn.

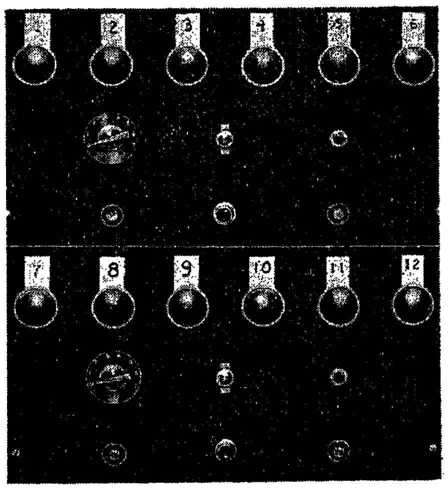


Figure 13. Electronic arc-back detector

The permissible seepage rate is based on previous experience with the particular design of machine. The correct value can be obtained from the manufacturer. As a general rule it can be assumed that a seepage of 0.5 micron per hour is the maximum permissible. However, any increase in seepage on a given tank should be investigated.

This procedure must be modified when there is no valve between the vacuum pump and the vacuum gauge. In this case both the high vacuum and roughing pumps must be shut down after the tank valves (or valve in the case of a multianode rectifier) are closed.

About one hour before the end of the test the pumps are started again, in proper sequence, to pump down the vacuum pipe line. Then they are shut down again, and when the high vacuum

Figure 12 (below). Group of instruments used in checking rectifiers

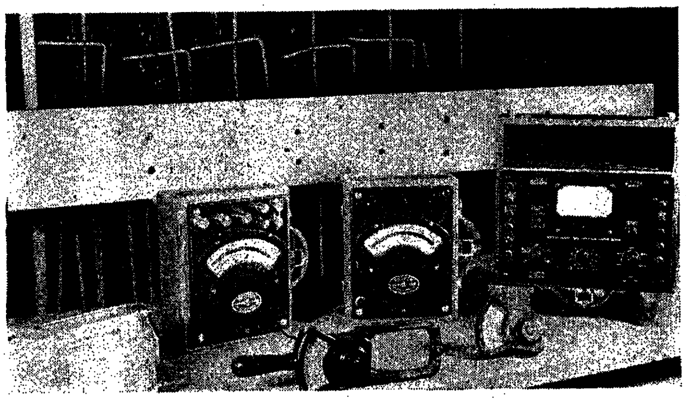
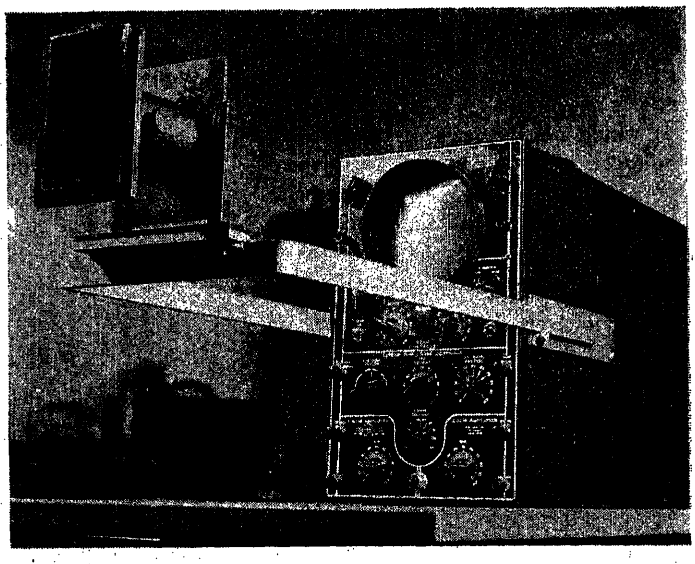


Figure 14 (right). Cathode-ray oscillograph with automatic camera attached



pump has cooled down sufficiently to be completely inoperative, the tank valves are opened, as above. Cooling water should flow continuously through the jacket of the high vacuum pump.

Naturally, no direct seepage test is possible in the case of sealed-off rectifiers. Where glass seals are employed and the light of the arc can be observed through them, a pinkish glow indicates a leaky tube. The excitation arc of a continuously excited rectifier will be unstable without the aid of the main arc when the vacuum is bad.

OSCILLOGRAMS UNDER FAULT CONDITIONS

Assuming that all checks and tests up to this point have produced negative results and a longer shutdown must still be avoided at this time, the rectifier will be returned to service for further observation. There is a possibility that progressive deterioration will produce more definite clues before serious damage is done.

During this period special attention should be given to the a-c and d-c systems for possible surges or other irregularities. Before going back in service, check the over-all circuit breaker speeds, including relay time, to make sure that slow tripping will not cause deterioration in parts of the equipment which are still sound.

Observation and analysis at this stage (or earlier) can be greatly enhanced by obtaining oscillograms under actual fault conditions. These oscillograms, to be of value, must show not only the fault itself, but what occurred immediately preceding it.

An excellent device for this purpose is a cathode-ray oscilloscope with a camera attachment for retaining the image that was on the screen at the time of the fault. Because of the retentivity of the oscilloscope screen, it is not difficult to have the fault trigger the shutter of the camera and photograph the image that was projected on the screen a cycle before the shutter actually opened.

It would not be feasible to have one oscilloscope-camera combination or memoscope for each anode. When the backfiring anode has been isolated and it is desired to obtain more specific information as to the phase relation of the fault, the magnitude, or other pertinent information, then this device can be applied directly. A single oscilloscope, however, can be connected across the interphase transformer to scan all anodes as previously described.

It might be difficult from the image so produced to decide with certainty which anode backfired, but since the timing of the shutter can be set to include in the image some of the phenomena preceding the fault, a clue to the cause might be found.

FINAL STEP

We have chosen for our example a difficult case which has stubbornly resisted diagnosis. Three more steps remain, the choice depending on circumstances:

1. Open for inspection and overhaul the tank or tanks with the poorest record.
2. If there is reasonable doubt that the trouble is internal, interchange the tank which has had the most arc-backs with one of the others.
3. If a spare tank and conditioning unit are available, interchange with each tank on the frame, one at a time, until the culprit is found by elimination.

The procedures, checks, and analyses outlined are directly applicable, or applicable with obvious modifications to rectifiers using the igniter principle.

Conclusion

Before embarking on any elaborate program of investigation, it is logical to carry out first the simpler tests requiring the least time and equipment. The actual order will be determined largely by service requirements and the severity of the trouble.

Load division, seepage, and megger tests are basic and should be carried out as soon as trouble is experienced, since any one of them might discover the source of trouble and indicate the remedy. The seepage and megger tests particularly are likely to determine at once whether trouble is in the rectifier itself or in associated equipment. A poor seepage test of course means that the leak has to be found and corrected. In many cases this requires opening the rectifier. Low megger tests invariably mean opening the rectifier to restore proper insulation levels.

The procedure can then be outlined in general as follows:

1. Ascertain that interruptions are being caused by arc-backs.
2. Check distribution of arc-backs among anodes.
3. Check anode current balance.
4. Megger test.
5. Seepage test.
6. Check operation of interphase transformer and wave-shape.
7. Check wave-shapes of: anode-to-cathode voltages; anode currents; grid-to-cathode voltages; and grid currents.
8. Detailed investigation of nature of arc-backs, including oscillograms obtained at the time of arc-back.
9. Investigation of a-c and d-c circuits.
10. Open tank or tanks for inspection and overhaul.
11. Check circuit breaker and relay speeds before restoring unit to service to make sure that slow tripping will not cause further deterioration.

This sequence is not fixed. Depending on local conditions, some steps may be eliminated and others added. Step 11, checking of relay and circuit breaker speeds, should always be included as a matter of routine.

The length of this paper should not be taken as a reflection on the reliability of rectifiers. If a rectifier installation is made with full understanding and due allowance for the fact that arc-backs do occur in normal operation with a frequency depending on the application and conditions of service, an occasional arc-back will have no serious consequences. If the arc-back rate increases beyond what could reasonably be expected, even though it causes no operational difficulty, the cause should be investigated. The seat of the trouble may be in the rectifier itself or in associated equipment, or it may even be in the supply or load circuits. An early diagnosis will reduce subsequent outage time and the cost of maintenance.

No Discussion

Rectifier Transformer Characteristics

AN AIEE COMMITTEE REPORT

AT EVERY installation of an electronic power converter, the voltage and phase characteristics of the power supply system are usually quite different from those required at the anode terminals of the rectifier. Because of this circumstance, a transformer having appropriate d-c windings is necessary to adapt the system to the rectifier requirements.

Many different converter circuits have been developed. In all of these the transformer connection influences the duty of the individual rectifying devices comprising the rectifier; therefore, the transformer connections used must be co-ordinated with the rectifying devices available.

Circuit Connections

Rectifier power circuits are of two general types. In one type, the transformer d-c windings are connected to the rectifying device so as to permit conduction in only one direction; while in the other type, current is carried in both directions during alternate half cycles. This classification is of considerable importance in the design of power transformers for rectifiers since the transformer utility, and winding arrangement to prevent core saturation, are different in the two types.

In power rectifier terminology, the terms single-way and double-way have been introduced to define the direction of current flow in the alternating voltage circuit elements which are conductively connected to the rectifying devices. A single-way rectifier is one in which the current between each terminal of the alternating voltage circuit and the rectifying device flows only in one direction. A double-way rectifier is one in which the current between each terminal of the alternating voltage circuit and the rectifying devices conductively connected to it flows in both directions.

In the single-way rectifier, which is the type most commonly used for power application, each rectifying device and its associated transformer d-c winding carry current in only one direction. There is, therefore, a direct component in each d-c winding current. In order to avoid saturation of the transformer core by this direct current, an even number of d-c windings must be associated with each a-c winding, and these d-c windings must be so connected that each conducts currents once per cycle, but at half cycle intervals, and in an opposite direction with respect to the a-c winding. This is shown clearly in Figure 1.

In this type of circuit, the a-c winding conducts current reflected from the pair of d-c windings and the current alternates in sign every half cycle. Since the a-c winding conducts current each half cycle while the d-c windings each conduct current only every other half cycle, the a-c winding kilovolt-amperes will be less than the sum of the kilovolt-amperes of the pair of d-c windings it supplies. Thus, for all single-way rectifier transformers, the a-c winding kilovolt-amperes will be less than the total d-c winding kva. The ratio between the a-c and d-c winding kilovolt-amperes depends upon the transformer connection.

In double-way rectifier circuits, the opposite directions of current conduction in the d-c windings are in alternate half cycles. The d-c winding current, therefore, contains no direct component and the a-c winding will conduct the reflected d-c winding current. Since the a-c and d-c windings are active simultaneously, their kilovolt-amperes will be equal and the winding arrangement is similar to the conventional transformer. It is, therefore, evident that the copper economy of the rectifier transformer for double-way circuits is greater than for single-way circuits.

Arrangement of Windings

The arrangement of rectifier transformer windings is similar to that of ordinary power transformers. The a-c winding may be either coaxial and concentric or coaxial and interleaved with respect to the d-c windings. Only the concentric arrangement will be discussed in this report. The d-c windings, usually the low voltage high-current windings, are on the inside next to the core, and the a-c winding, usually the high voltage, is on the outside. In some cases for low voltage

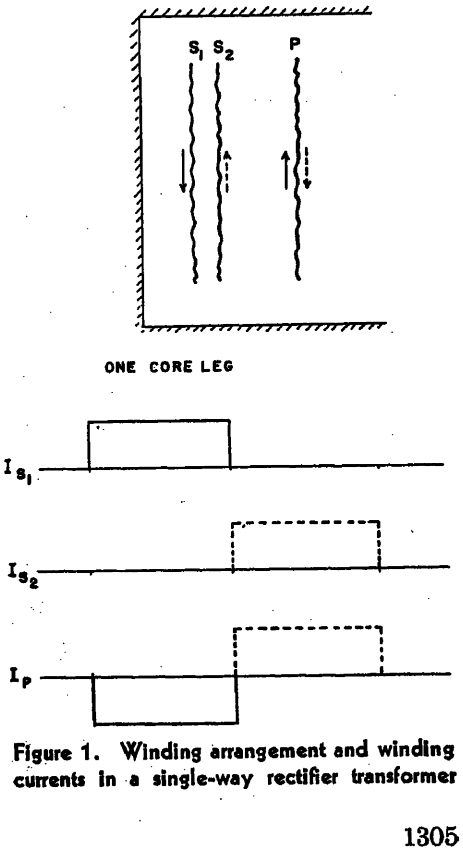


Figure 1. Winding arrangement and winding currents in a single-way rectifier transformer

---

Paper 49-271, recommended by the AIEE Electronic Power Converters Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted March 28, 1949; made available for printing September 7, 1949.

Personnel of the Rectifier Transformer Subcommittee of the AIEE Electronic Power Converters Committee: A. J. Maslin, *Chairman;* R. A. Nelson, F. D. Kaiser, and W. C. Sealey.

and very high direct current output, with a moderate voltage a-c winding, the reverse arrangement is found convenient.

In order to obtain balanced regulation, it is a necessary requirement that all d-c windings from anode to neutral have the same reactance to their associated a-c winding. This complicates the simple 2-winding arrangement when there is more than one d-c winding per phase, as in the single-way circuits.

It also is a necessary requirement that a-c and d-c windings which normally have balancing ampere turns be physically balanced with respect to the heights of the winding coil stacks. This balancing of winding lengths helps reduce the mechanical forces and minimizes stray losses.

With these general principles in mind, it may be helpful to discuss a few specific connections of the types most commonly used and see how these conditions are satisfied.

In all simple double-way rectifier transformers having one a-c and one d-c winding, such as the delta-Y 6-phase double-way connection, the current conductions are similar in manner to standard power transformers. The windings in these transformers can, therefore, be arranged in accordance with the established practices for power transformers, which satisfy the conditions mentioned.

The most common of all single-way circuits is the 6-phase double-Y having either a delta or Y a-c winding. For this connection, the two d-c windings, which are on the same core leg, can be made an interwound helical coil and located next to the core. An interwound helical coil consists of two or more windings, each having the same inside and outside diameters and the same number of turns, disposed to form a multiple pitched helix. Such a coil may be likened to nested helical springs having the same diameters and number of turns, or a multiple thread screw. The a-c winding is then concentric with the d-c windings and located on the outside. As stated before, the d-c windings may be the outside windings. In this case, these windings may consist of many multiple coils, with the coils of the two windings interleaved, so that each is spread over the full winding length.

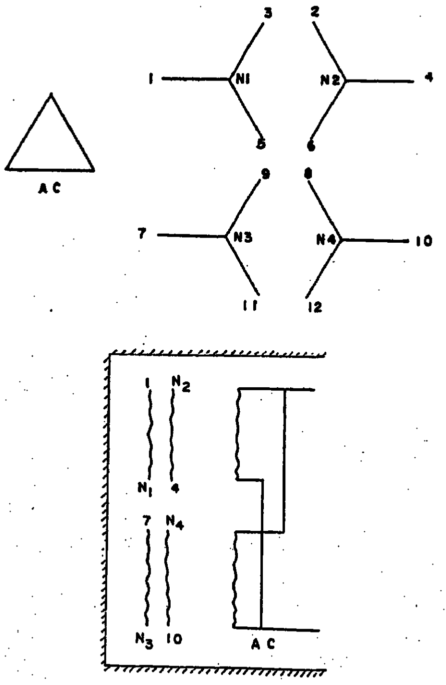


Figure 2. Schematic winding arrangement in a delta 6-phase parallel double-Y connected rectifier transformer

A modification of the double-Y circuit is the 6-phase parallel double-Y connection. This connection is, in effect, two double-Y sets of windings on one core. The two d-c windings, which are on the same core leg, and part of the same 6-phase set of Y windings, may be an interwound helical coil located next to the core in the top half of the core window. The other two d-c windings, which are on this same core leg, and part of the other 6-phase set of windings, are then a similar interwound helical coil and located next to the core in the bottom half of the core window. The a-c winding on each core leg is divided into two multiple circuits, one associated with each set of d-c windings. The a-c windings are concentric with and located outside the d-c windings, one in the top half and one in the bottom half of the core window. This arrangement is shown schematically in Figure 2.

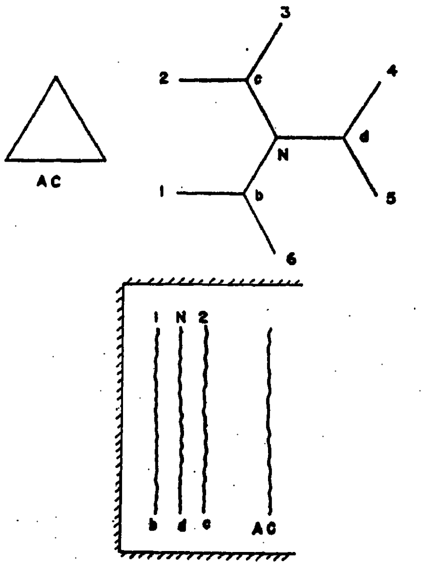


Figure 3. Schematic winding arrangement in a delta 6-phase fork connected rectifier transformer

Another connection which has been used is the single-way 6-phase fork with either a delta- or Y-connected a-c winding. The three d-c windings, which are all on the same core leg, can be interwound as a helical coil. With the a-c winding on the outside, this arrangement gives approximately the same leakage reactance between any one of the three d-c windings and the a-c winding, and very low reactance between the d-c windings themselves, as shown schematically in Figure 3.

There are two general arrangements for the 12-phase quadruple zigzag single-way circuit. One has a single-circuit a-c winding with four long d-c windings which may be interwound as a helical coil and four short d-c windings which also may be interwound as a helical coil, on each core leg.

The long and short coils being of unequal turns cannot be interwound, but rather each is spread out over the entire stack height. The other arrangement for the 12-phase zigzag has two a-c circuits in multiple, one associated with one of the two 6-phase sets of d-c windings, and the other associated with the other 6-phase set.

This arrangement is then similar to the parallel double-Y connection described previously, with the paralleled a-c windings in the top and bottom halves of the core window opposite their respective sets of d-c windings. These two types of the 12-phase zigzag connection are shown schematically in Figures 4 (A) and 4(B) respectively.

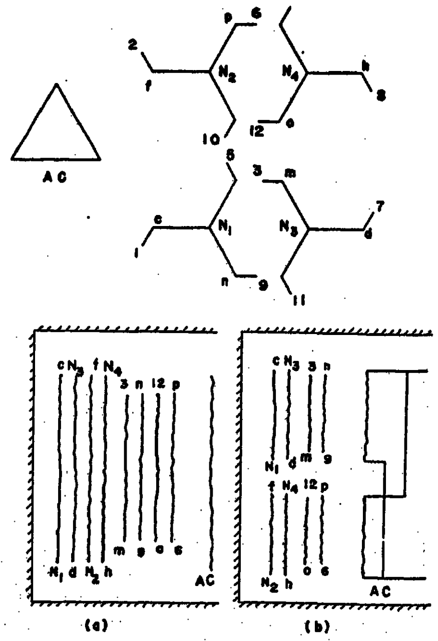


Figure 4. Schematic winding arrangements in delta 12-phase quadruple zigzag connected rectifier transformers

Structural Features

Rectifier transformers are essentially power transformers that have been modified in some important details to make them suitable for rectifier service. The modifications in winding arrangements have been described. A second and very important modification is an increase in the fault current limit of rectifier transformers over power transformers.

As will be shown later on, rectifier transformers are subjected to more severe stresses and at more frequent intervals than are power transformers. The mechanical strength must, therefore, be of the highest order, and all mechanical bracing must be ample for the forces involved and must be of such a nature as not to yield or fail under repeated stressing. The difficulty of successfully meeting these requirements increases with the size of the unit.

Adequate mechanical bracing, like cooling surface, is quite easily obtained in the smaller units; but with increasing rating the mechanical stresses, like losses, increase faster than in proportion to the capabilities of the structure. They therefore demand special consideration.

Rectifier transformer windings are usually wound with rectangular conductor of larger cross section than power transformer windings, so proportioned as to give high mechanical strength in the direction of greatest force. The outer coils in a concentric coil arrangement are designed so that the tensile stress under the most severe fault conditions is well below the elastic limit of the copper used. The inner coil is adequately supported on its inner surface to preclude buckling due to the compressive forces acting on its outer surface. Because of the axial component of forces, the coil supports between turns or coil sections must be adequate so that the beam strength of the conductors between supports will not allow deflections which could cause failure turn-to-turn or section-to-section.

Inasmuch as the axial net force between a-c and d-c windings is proportional to their net displacement of magnetic centers, it is desirable to have both windings as free as possible of taps so that the net magnetic displacement can be kept as small as possible. Also, the ampere turn distribution should be as nearly uniform as possible throughout the principal length of the leakage path so that internal coil forces or forces on individual sections are uniform and minimum. It is highly desirable to maintain a perfectly balanced winding both magnetically and mechanically and to avoid idle tap sections and extended windings for voltage control unless they can be achieved in a symmetrical manner in so far as winding layout is concerned. On the larger ratings it has become common to take care of direct voltage control, when required, by auxiliary means such as an autotransformer or voltage regulator, in order not to impair the rectifier transformer design as regards unbalanced magnetic forces.

The use of spirally-wound coils with a large pitch should be avoided because of the unbalanced magnetic distribution and resulting torque on the coil, which tends to unwind an outer coil or otherwise seriously deflect the copper conductors.

In addition to the forces on the windings themselves, the forces on leads and bus bars must be considered and adequate bracing provided for these structures. The optimum lead arrangement should minimize the stress without employing a configuration which produces stray losses or heating of tank wall and core clamps. The objectives of minimum stress and minimum stray losses and tank heating are mutually incompatible.

Some of the forces mentioned are ultimately transmitted to the top and bottom core clamps, which are made adequately strong and are tied together by means of steel members. Winding clamping surfaces are supported by the core clamps, thus making tight clamping of the winding structure possible.

Electrical Characteristics—Voltage

In a power rectifier, the transformer is of primary importance because many characteristics of the rectifier such as direct voltage, regulation, and power factor depend upon the transformer connection and characteristics.

For single-way rectifiers, the direct voltage is equal to the average voltage during the conducting period, between the transformer neutral and the conducting anode.

At no load, the reactance drop is zero and the direct voltage wave follows the envelope of d-c winding voltage waves inherent to the circuit or, according to the general equation

$$E_{do}=(p/\pi)\sqrt{2}E_s \sin (\pi/p) \qquad (1)$$

where

E_{do} = average direct voltage at no load assuming no overlap, no phase control, and zero arc drop
E_s = anode-to-neutral voltage (rms)
p = number of phases in a simple rectifier

Electrical Characteristics—Reactance

The commutating reactance is the reactance which opposes the current transfer between rectifying elements which conduct consecutively. In an actual installation, the commutating reactance includes the reactance of the anode leads, the transformer reactance, and the a-c supply system reactance, which must be considered irrespective of whether or not the rms value of the supply voltage is maintained constant at the transformer a-c winding terminals. The commutating reactance is usually expressed as a lumped value, anode-to-neutral ohms, referred to d-c winding of the transformer.

In accordance with the principles of rectifier operation in which the load current is successively conducted by the various anodes and their associated transformer d-c windings, it can be shown that the effect of commutating reactance is to increase the length of time of current transfer between the successive anodes, without altering the average or maximum value of anode current. It also can be shown that the length of time to effect a current transfer varies with load, and that the voltage-time integral, during the period of current transfer, represents a wattless voltage drop in so far as the average direct voltage is concerned. This voltage or commutation drop may be represented by the equation

$$E_x=(I_cX_cp)/(2\pi) \qquad (2)$$

where

E_x = direct voltage drop caused by commutating reactance
I_c = direct current commutated
X_c = line-to-neutral commutating reactance in ohms
p = number of phases in a simple rectifier

Knowing the ohms reactance per supply line, the effective commutating ohms of the supply line can be determined by the use of the commutating reactance transformation constant, D_x, in the equation

$$X_c=X_{cT}+X_{cL}=X_{cT}+(X_L/D_x)(E_s/E_n)^2 \qquad (3)$$

where

X_c = total commutating ohms
X_{cL} = commutating ohms of supply line
X_{cT} = commutating ohms of transformer
X_L = ohms reactance of supply line
E_s = anode-to-neutral voltage (rms)
E_n = supply voltage line-to-neutral (rms)
D_x = transformation constant

For a table of values of D_x for various circuits, see American Standards for Transformers, Regulators, and Reactors.[1]

For the case of double-way rectifier

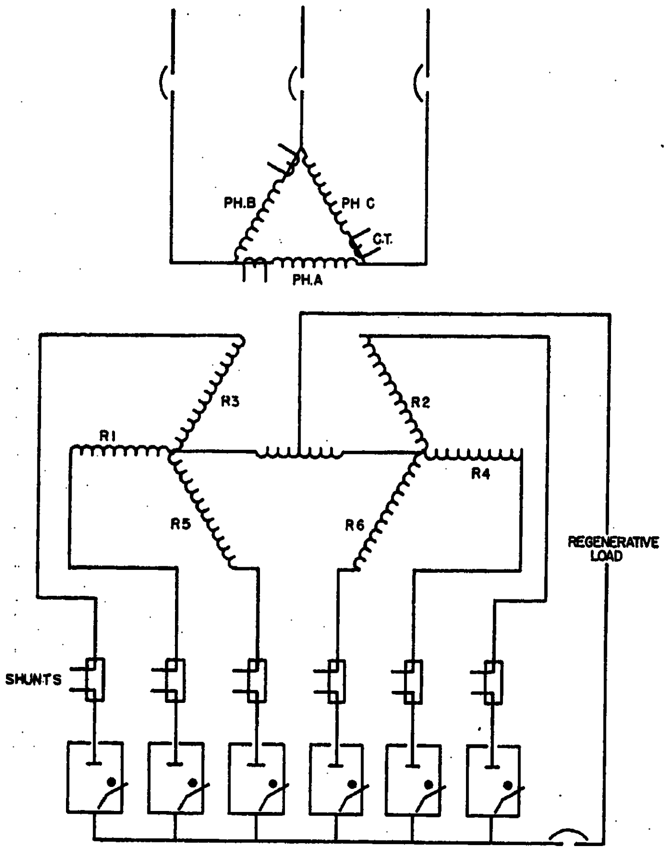


Figure 5. Schematic diagram for artificial arc-back tests

transformers having a single d-c winding, or single-way rectifier transformers assumed to have perfect interlacing between the pairs of d-c windings, the commutating ohms due to the transformer can be readily calculated from the transformer reactance.

The total transformer commutating reactance line-to-line is the leakage reactance between the d-c windings undergoing commutations and their respective a-c windings. The anode-to-neutral reactance is one-half the line-to-line reactance. We may, therefore, write

$$X_c = \frac{3E^2}{VA} X_t \tag{4}$$

where

X_c = commutating reactance line-to-neutral (ohms)
E = rms volts, anode-to-junction point of successive conducting phases
X_t = per unit transformer reactance on a-c winding—volt ampere base
VA = volt amperes of the a-c winding associated with commutating d-c windings

For all of the connections discussed here, the value E is actually E_s, the line-to-neutral d-c winding volts with the single exception of the fork-connected transformer. For the fork connection $E = 0.577E_s$. This fact is obvious when commutation is between two directly connected outer fork windings as $R_2 - R_3$, Figure 3. It also is true for commutation between anodes such as $R_1 - R_2$. This is because the commutating current is equal and opposite in the two outer fork windings in the circuit and it has been assumed that there is perfect interlacing and hence no leakage reactance between these two windings.

The VA is the total a-c winding volt amperes with the exception of the quadruple zigzag connection having two a-c winding circuits, each associated with separate 6-phase d-c windings.

In calculating the commutating ohms for the single-way circuits, we have assumed perfect interlacing between pairs of d-c windings on the same core leg. Actually, of course, the condition of zero reactance betweens pairs is not realized. However, with the usual arrangement of interwound helical coils or interleaved paralleled coils, the reactance between pairs is small. Considering the various reactances in a 3-winding equivalent diagram, where the a-c winding to each d-c winding reactance is equal, and the interlacing reactance is small, it can be shown readily that the major portion of the transformer reactance is in the a-c winding. In general, the reactance between d-c windings on the same core leg will be

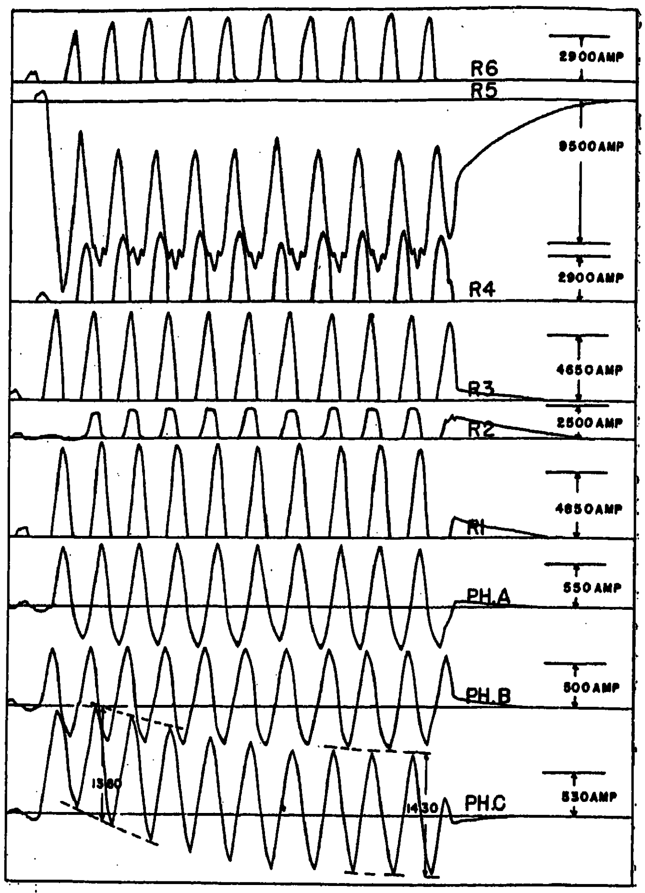


Figure 6. Oscillogram of an artificial arc-back test in the laboratory

in the range of 0.5 to 2 per cent on the a-c winding kilovolt-ampere base.

This imperfect interlacing of d-c windings will have the effect of increasing the commutating reactance 2 to 10 per cent

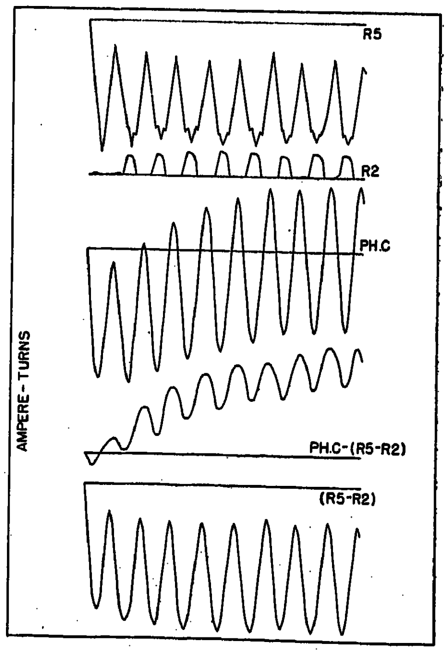


Figure 7. Magnetizing forces on one transformer core leg during arc back

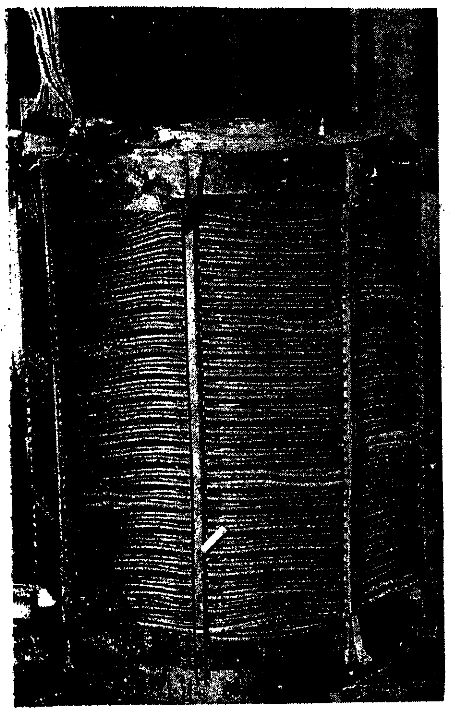


Figure 8. Interwound helical coil comprising two d-c windings damaged by repeated arc-backs

higher than the value calculated from the transformer reactance as given before.

Having calculated the X_c as above, the actual voltage drop caused by commutation can be determined from equation 2, or knowing the per cent reactance based on the a-c winding rating of the transformer considered as a normal power transformer (information which is usually given on the nameplate), the approximate per cent regulation due to commutating reactance may be determined from Table I.

Table I

Circuit	Per cent E_x*
Delta-6-phase Y double-way	0.5 per cent IX**
Delta-6-phase fork	0.5 per cent IX
Delta-6-phase double-Y	0.5 per cent IX
Delta-6-phase parallel double-Y	0.5 per cent IX
Delta-12-phase quadruple zigzag with single-circuit a-c winding	0.258 per cent IX
Delta-12-phase quadruple zigzag with multiple-circuit a-c winding	0.516 per cent IX

* Per cent E_x = regulation due to commutating reactance expressed as a per cent of the no load d-c voltage.

$$= 100 \times \frac{E_x}{E_{do}}$$

** Per cent IX = a-c to total d-c winding reactance in per cent.

The equations shown in Table I are approximate as they assume that the reactances of the a-c winding to each of the d-c winding groups, or commutating pairs, are equal, and that the commutating pairs are perfectly interlaced. They may yield results as much as 10 per cent low.

While transformer reactance is one of the largest factors in rectifier regulation, it also is one of the largest factors in limiting rectifier fault currents. It would, therefore, seem desirable to design rectifier transformers with higher values of reactance than are standard for power transformers of the same voltage and kilovolt-ampere rating. This is actually the case; however, since high reactance means high regulation, the application of the rectifier also must be considered.

Rectifiers are used in three general classifications of service. These are railway or mining service, industrial service, and electrochemical service.

Rectifiers in railway service usually have a regulation of approximately 5 to 6 per cent. This fixes the value of transformer reactance at 6 to 7 per cent for the usual 6-phase transformer connections. If the 12-phase quadruple zigzag connection having a single-circuit a-c winding is used, the transformer reactance may be increased to approximately 13 per cent and still not exceed a rectifier regulation of 6 per cent. This is an advantage of this connection in that it allows high reactance for limitation of fault currents and gives normal regulation. The 12-phase quadruple zigzag connection having a double-circuit a-c winding is similar to the 6-phase connections in regard to reactance and regulation.

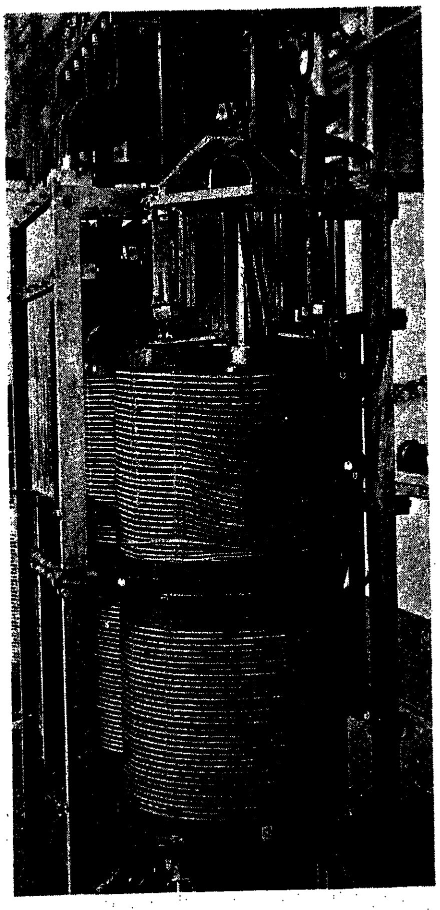


Figure 9. Delta 6-phase parallel double-Y connected rectifier transformer damaged by repeated arc backs

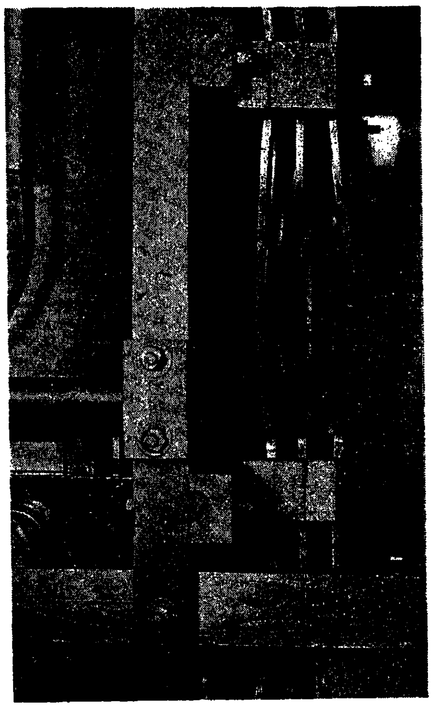


Figure 10. Bus bar leads deflected during arc-back tests

Rectifiers for industrial service are usually provided with phase control voltage regulation. The voltage is maintained at rated value to 100 per cent load. Phase control has the disadvantage of reducing the power factor and increasing the harmonic content of the supply line current. Because high reactance would increase the amount of phase control required, the inherent regulation of rectifiers for this type of service also is limited to approximately 6 per cent, and the transformer reactance is limited to approximately 7 per cent for the usual 6-phase connections.

Rectifier installations in electrochemical plants are usually considerably larger in kilowatt capacity than in railway or industrial service. These installations often comprise several rectifier transformers operating with phase shifting auto-transformers to give multiphase systems. Under the fault condition of arc back, d-c feed from the paralleling rectifier equipment is present until cleared by the d-c switchgear. This being a very severe condition, it is desirable to increase the transformer reactance above that provided for the smaller railway or industrial systems. Accordingly, the transformer reactance may be in the range 7 to 9 per cent.

Regulation in these installations is not so important because a current regulator, operating on phase control, is usually provided; also taps are generally included in the rectifier transformer or separate regulating transformers for direct voltage control. The transformer taps are used for coarse voltage adjustment and phase control for fine adjustment.

Electrical Characteristics—Resistance

In addition to the regulation due to reactance, there also is a voltage drop due to the losses of the circuit. The circuit losses include the copper or load losses of the main transformer and interphase transformer, and generally can be lumped into a single resistance or voltage drop, which is equal to P_r/I_d where P_r is total resistance loss in watts, and I_d is the d-c load current of the rectifier. The resistance loss should not include the core loss of the main transformer or interphase transformer, as these losses are supplied by the a-c source with negligible regulation through the transformer. In general, the resistance losses will range from 0.9 to 1.5 per cent of the rectifier rating, or for the larger ratings (above 3,000 kw) generally about 1 per cent.

In the consideration of short-circuit and arc-back calculations, the ratio X/R is used quite often. This ratio theoretically concerns the actual value of reactance and resistance in the equivalent or simplified circuit. If the arc drop is neglected, this ratio may be approximated by the ratio X_c/R_c, where X_c is the line-to-neutral commutating reactance, and R_c the line-to-neutral commutating resistance. The value of this ratio may range from 2.5 to 8.5, with the higher values in the larger 3,000-volt rectifiers, and the smaller values in 250-volt rectifiers.

Fault Currents

In many applications, rectifiers are arranged to operate in parallel with other converting equipment such as d-c generators, synchronous converters, or other rectifiers. Arc backs in rectifiers connected to such a system are equivalent to a short circuit on both the a-c and d-c systems. In cases such as this, the winding fault current may be much greater than the 25 times normal current, which according to American Standards Association, is the maximum current a power transformer must be capable of withstanding for two seconds.

In the case of a power transformer, the number of short circuits is few, if any, during any one year and relatively few during the life of the transformer. The occurrence of arc backs in rectifiers is basically random in nature. It is apparently not practical to absolutely eliminate the possibility of arc backs. Realizing this, the transformer designer must make allowances for the cumulative effects of repeated short circuits or arc backs which may occur at a rate of several per year.

On 600-volt class rectifier transformers under conditions of fault with d-c feed, it has been found advisable to consider the stresses in the coil sections and coil leads, based upon peak currents of the order of 50 times normal rms current in the respective windings and leads.

The magnitude of fault currents, especially during arc back, is importantly influenced by saturation of the rectifier transformer core for some sequences of switching. Some data on this phenomenon were obtained using a 6-tube Ignitron rectifier fed from a 252-kva transformer in conventional delta double-Y connection, as indicated in Figure 5.

Specially designed current transformers connected within the delta of the a-c windings were used to measure the three a-c winding currents, while six shunts, one in each of the anode leads, were used to record the d-c winding currents $R1$ to $R6$. Alternating current was supplied through an oil circuit breaker, and the regenerative load was connected to the set through a high-speed cathode circuit breaker. Anode circuit breakers were not used. Artificial arc backs were applied by connecting transformer terminal $R5$ to the tube cathode, using a short circuiting switch.

Figure 6, traced from the oscillogram, shows the currents recorded during one test. With the rectifier carrying approximately rated load at zero phase control, the artificial arc back was initiated substantially at the end of the normal conducting period, which time of initiation may be expected to yield the maximum first cycle of fault current. Reverse power from the regenerative load was interrupted in approximately one-half cycle by the high speed cathode circuit breaker, and the fault completely cleared by the opening of the a-c circuit breaker after about ten cycles. It will be noted that during the first half-cycle of fault, windings $R2$, $R4$, and $R6$ do not contribute to the fault current, while the regenerative load does. During the second half-cycle, $R6$ makes a substantial contribution, $R4$ begins to contribute, $R2$ does not contribute, and the reverse current from the load is out of the picture. As a result, the crest value of fault current ($R5$) changes but little during the entire fault period. A large amount of test data such as this supports the contention that, when high-speed anode switching is employed, the contribution from the unfaulted Y can be neglected without significant error.

Windings $R2$, $R5$, and phase C are on the same core leg of the transformer. Figure 7 was obtained from an enlargement of Figure 6 by multiplying measured deflections, calibration factors, and winding turns. All curves in Figure 7 are to the same scale. The net magnetomotive forces acting on this core leg are shown by the trace phase $C-(R5-R2)$, and contains both alternating and direct components. The alternating component reaches its approximate ultimate value within about three cycles, while the d-c component continues to increase for at least eight cycles. From a comparison of the traces phase C and $(R5-R2)$ in Figure 7, it is apparent that saturation of the transformer core is not an important factor during the first cycle or two, but is significant later. The effect of the increased alternating component of exciting magnetomotive forces on the alternating component of current in phase C winding is indicated in Figure 6. The crest-to-crest amplitude of this current changes from about 1,380 to 1,430 amperes—an increase of less than five per cent.

The mechanical force acting on the phase C winding during this fault is proportional to the ampere turn product

Phase C by $(R5-R2)$

as obtained from Figure 7. This is a maximum at the time of the second crest of current through winding $R5$ and subsequently diminishes to roughly 65 per cent of that value for the ninth crest. If the transformer core were capable of carrying unlimited flux and required zero magnetizing force (an ideal core), the phase C current and magnetomotive forces would not be reduced with time, but would be as shown by $(R5-R2)$ in Figure 7, and the maximum force on the winding would be sustained for the duration of the fault. Thus, in spite of an increase in alternating component of fault current in the a-c winding, it is significant that core saturation is effective in reducing the severity of the mechanical forces acting on the windings of the transformer during long faults.

In connection with the oscillogram, Figure 6, it is interesting to note that anodes *R1-R2-R3-R5* are all conducting at the instant of interruption of the a-c supply. The d-c transients, on the traces of *R5-R3-R2-R1* and all three primary phases, after interruption, are due to the dissipation of the energy stored in the magnetic field of the saturated core. This energy is dissipated as I^2R loss in the circuit and transformer and is in the same direction in all three primary phases (phase *C* is inverted in the oscillogram).

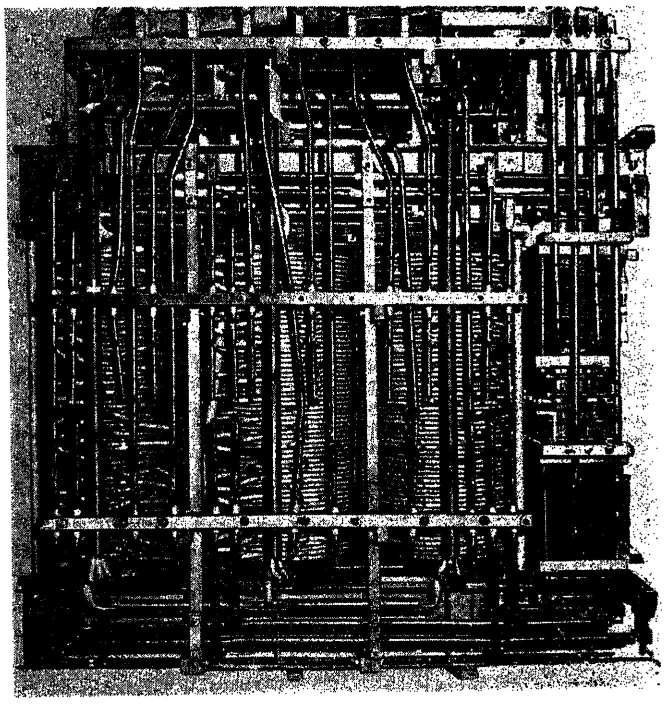


Figure 11. D-c winding leads deflected during service arc backs

Mechanical Forces

Rectifier transformers are subject to severe mechanical stresses due to abnormal currents that flow in the windings during short circuits or arc backs. Since the severest forces occur in the transformer during an arc back with d-c feed, the mechanical strength should be coordinated for this condition.

The accumulated data from laboratory tests and field experiences have contributed greatly to the art of rectifier transformer design and manufacture. These data have repeatedly substantiated the magnitude of fault currents to be expected and the destructive forces which result therefrom. In one early test a 3,000-kw 600-volt delta parallel double-Y rectifier transformer was tested to destruction by the repeated application of artificial arc backs in the laboratory. Figure 8 shows one inner coil, comprising a pair of interwound helical d-c windings, from this transformer. It will be noted that the deflections between radial spacers are greatest at the ends of the coil and are in opposite directions; that is, toward the center from each end. Electrical failure occurred at the bottom end, presumably because there the deflection between radial spacers was large enough to cause a winding-to-winding short circuit. Over-all mechanical failure of the coil also ensued as a result of the failure of the insulating member at the top end of the coil.

Figure 9 shows the transformer from which the coil, Figure 8, was taken. The repeated impacts of the large number of arc backs caused mechanical failure of the upper coil of the a-c winding. It will be noted that the coil support strips, which dovetail with the spacers between coil sections, have inched themselves upward between the upper core clamps, allowing the spacers to become detached and fall out. The resulting downward deflection of the portion of the coil thus released was unquestionably a factor in the failure of the insulating member at the top of the d-c coil in Figure 8. This a-c winding failure was due to lack of stability under repeated stressing, and not due to insufficient strength.

Figure 10 shows the deformation in bus bar leads on a rectifier transformer which was subjected to a large number of laboratory arc backs. While the deformation in the busses did not cause a failure, the need of adequate bracing is apparent.

In the design of braces for bus bar leads, it is essential that proper consideration be given to the allowable working stresses for the copper used; annealed copper has an allowable stress approximately one-third that of hard copper.

Figure 11 shows the deformation in bus rod leads on a rectifier transformer which had been subjected to a large number of arc backs with resulting d-c feed. This unit had not failed but was untanked for inspection after four years of continuous service.

Figures 8 to 11 are units of older designs and show the need of additional bracing. They are not representative of modern rectifier transformers.

Summary

This report briefly discusses the circuit, winding, structural, and electrical characteristics of rectifier transformers as they are required by the application, and as they in turn affect the over-all operation of the rectifier installation. Some data which permit approximate interpretation of rectifier transformer characteristics in terms of over-all rectifier performance are presented. The magnitude of fault currents, their frequency of occurrence, and their importance as factors in the design of rectifier transformers are emphasized. Examples of their destructive effect are cited.

Reference

1. Test Code for Rectifier-Transformer Equipment, American Standards Association (New York, N. Y.), *C57.28-1948*, page 11.

No Discussion

Space Code Selector Supervisory System

E. F. FORREST
ASSOCIATE AIEE

P. W. SCHIRMER
NONMEMBER AIEE

DURING the past 20 years there have been numerous supervisory systems using various designs and basic principles of transmitting signals such as tone, counting chain codes, or sequence chain codes.

The purpose of this paper is to summarize what requirements a system should meet to obtain the maximum benefits and economies from the viewpoint of the user, and to describe a commercially available equipment which meets these requirements.

Requirements

A supervisory system should fulfill the following essential operating requirements:

1. Transmit control circuit selection code and check this transmission before the control operation is performed.
2. Prevent control operation if the correct checking signal is not received.
3. Be suitable for operation over any commonly available type of channel.
4. Be suitable for operating a number of outlying stations from a single control station.
5. Components should harmonize with other switchboard devices and be suitable for building into standard switchboards.
6. Equipment should be easy to expand to take care of future growth.
7. Equipment should be easy to install, maintain, and test.
8. Be available for either 48-volt or 125-volt d-c operation.
9. Include provision for telemetering and telephone communication.
10. Should operate over the same standard range of battery voltage as the remainder of the switchgear devices.
11. Be simple and easy to operate.
12. Be rapid, uniform speed of operation for all points.

Space Code Selector Supervisory System

The system described in this paper known as the space code selector system meets these requirements. It uses a signal resembling the telegraph code which is transmitted and received by circuits composed of telephone-type relays. Where the telegraph uses short and long impulses, the space code system uses impulses of the same length. However, the time between impulses determines the code. Figure 1 shows a code consisting of two impulses. Impulse 1 is on for one unit of time and impulse 2 is on for one unit of time with one unit of time spacing between the two impulses. Thus a code with a definite number of impulses and a definite characteristic is established.

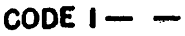


Figure 1

Again, if the same two impulses are used but the time spacing between impulses 1 and 2 is doubled, a new code results. The increased time element will be referred to as pause time. Figure 2 will show the relation between codes 1 and 2.

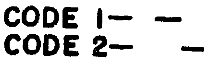


Figure 2

If three impulses are used, the codes can be increased, as shown in Figure 3, to four.

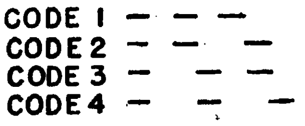


Figure 3

If four impulses are used, the codes are again increased, as shown in Figure 4, to eight.

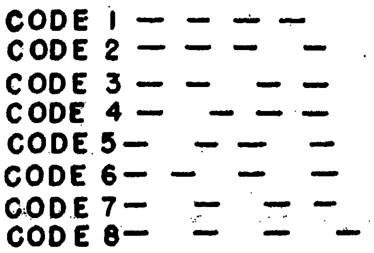


Figure 4

It is, therefore, shown that for each impulse added to the code the total number of codes is doubled. Five impulses will produce 16 codes, six impulses 32 codes, seven impulses 64 codes and eight impulses 128 codes.

A supervisory system requiring more than 128 codes would be rare. Therefore, the equipment described later is designed for a maximum of 128 code selections. The complete system of 128 codes is divided into 16 groups. Each group contains eight codes or selection points and the system may have from 1 to 16 groups depending on the number of points required. Figure 5 shows the codes used for the first eight points of 128-point system.

The codes in Figure 5 are divided into the first four impulses (point code) and the second four impulses (group code). The first four impulses with pauses denote the selection of the point within the group. The lack of pauses in the second four impulses place all the point codes in the first group.

Paper 49-272, recommended by the AIEE Substations Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 19, 1949; made available for printing September 13, 1949.

E. F. FORREST and P. W. SCHIRMER are both with the General Electric Company, Philadelphia, Pa.

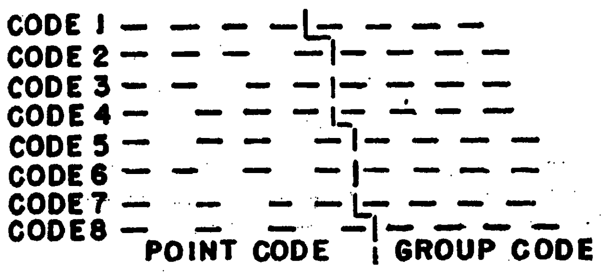


Figure 5 (left)

Figure 6 shows the relation between code 3 in the first group and code 11 in the second group. The first four impulses of code 11 are the same as the first four impulses for code three. The second four impulses have a pause inserted between impulses 4 and 5 denoting it as the third point in the second group or code 11.

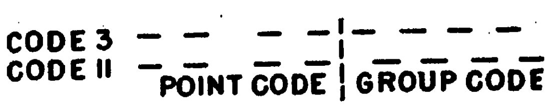


Figure 6 (left)

Figure 7 shows the relation between codes 3, 11, and 19 which are in the first, second, and third group respectively. Again the unit code is the same, but the pause between impulses 5 and 6 places code 19 in the third group. Other codes are established in the same manner.

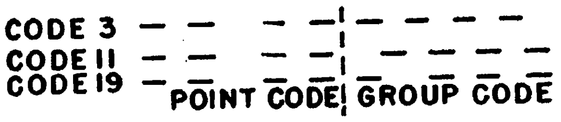


Figure 7 (left)

After the selection of a point is completed, a code of four impulses is used to perform a control operation or transmit an indication. Figure 8 shows the codes used for close and trip.

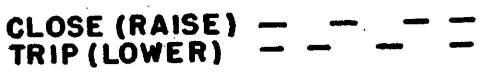


Figure 8 (left)

To complete a point selection on this system, the following steps take place:

1. The control station transmits the code for the point selection and registers its own transmission.
2. The outlying station will register this code and transmit the same code back to the control station. At the same time it registers its own transmission checking itself against the code originally received.
3. The control station registers the code received from the outlying station checking this code against the original transmission.
4. When the control station receives the same code it transmitted, a check feature is established and is so indicated by the lighting of the amber lamp, Figure 9.
5. When an automatic operation occurs, this exchange of codes is reversed.

After a point selection is completed, the check key, Figure 9, can be operated to check the indication of the point without operating the supervised unit.

This system also provides the following protection against misoperation:

1. It must always transmit and receive eight impulses for a selection.
2. It always transmits and receives four impulses for control and indication. If a selection code were to replace a control code, the receiving equipment would reset.
3. If more or less than the correct number of impulses are received due to channel interference, a selection or control will be incomplete and equipment must be reset.
4. If an impulse is received during pause time, the equipment will prevent a selection or operation.

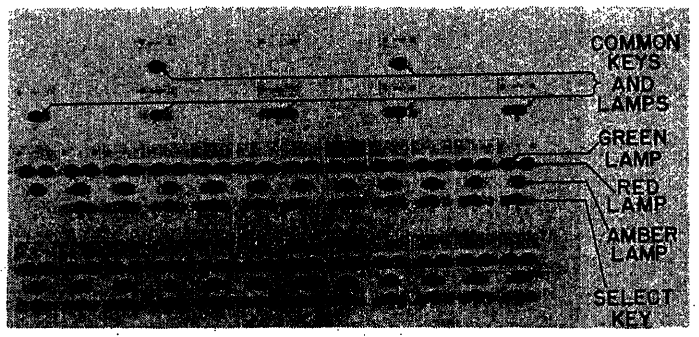


Figure 9 (right). Control and indication equipment

The system operates on a single polarity impulse and, therefore, can be used for any type of channel which will carry signal intelligence such as single polarity direct current, unmodulated carrier, single audio tone or microwave.

The equipment may be used for multistation operation where one control station will operate several remote stations. For example, a 24-point system can be divided into three remote stations with eight points in each station. Station 1 will only respond to the eight codes assigned to that station. Stations 2 and 3 will lock out until the selection established in station 1 is complete. Likewise any code assigned to station 2 will lock out stations 1 and 3. The division of points may be any multiple of eight.

Equipment

The space code selector supervisory system consists of control station equipment interconnected with one or more outlying station equipments. See Figures 9, 10, and 11. The interconnecting channel may be either a 2-conductor metallic line, a single-frequency carrier current, or microwave system.

The control station equipment, Figures 10 and 11, includes the cases containing telephone relays, a key and lamp group for control and indication of each apparatus unit, and a set of common operating keys and lamps.

Each key and lamp group, Figure 9, consists of:

1. A push button select key used to select the apparatus unit to be controlled.
2. An amber lamp to indicate that a selection has been made and checked and equipment is ready to transmit control codes.
3. Red and green lamps to provide continuous indication of the position or condition of the apparatus unit. An automatic change in position of apparatus unit will be indicated by flashing a red or green lamp.

A common key and lamp group consists of:

1. Two push button control keys "close" and "trip" controlling the selected unit.
2. A green start lamp which indicates that the equipment is in a normal at-rest position.
3. A red alarm lamp which lights whenever an operation originates at the outlying station. An alarm bell sounds simultaneously.
4. A push button bell key which extinguishes the alarm lamp and silences the bell, but does not stop the flashing of the indicating lamps.
5. A push button reset key which resets

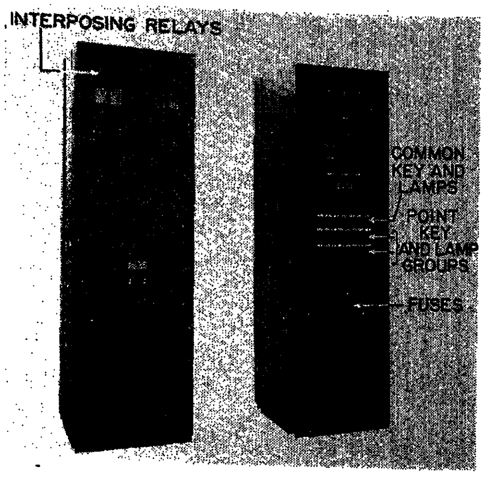


Figure 10. Front view supervisory equipment. Control station on right, outlying station on left

the equipment to its normal condition and stops the flashing of the indicating lamps.

6. A push button check key which causes the equipment to check the position of any selected apparatus unit or to perform station check operation of all points when the equipment is in a normal reset position.

The supervisory relay cases, Figure 11, consist of the following:

1. Two common cases (1 and 2) containing the supervisory relays required to transmit and receive codes for any system up to 128 points.
2. One point relay case for each eight apparatus units controlled. Point 1 is always used for station check.
3. One line case containing the equipment required to adapt the supervisory system to the particular channel involved.

The relay cases are semiflush mounted, standard switchboard type of drawout construction, see Figure 12. A flexible cable of sufficient length is used to make connections between the relays on the drawout cradle and the removable plugs at the rear of the case so that the unit can be withdrawn for inspection or adjustment of relays without disconnecting the leads, see Figure 13. The relay assembly may be entirely removed by disconnecting the plugs. The point relay cases at any terminal are interchangeable.

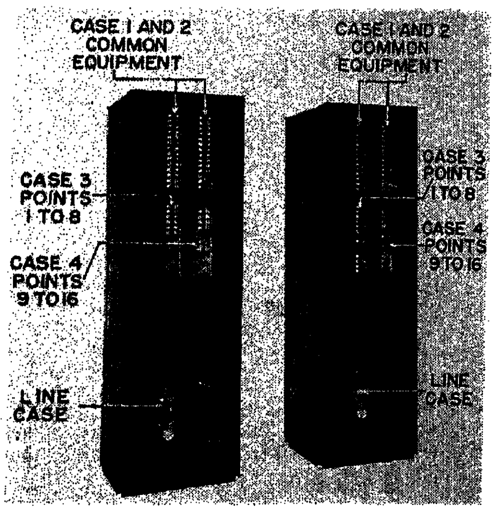


Figure 11. Rear view supervisory equipment. Control station on right, outlying station on left

The outlying station equipment, Figures 10 and 11, is essentially the same except for the substitution of interposing auxiliary relays instead of the key and lamp groups.

Future Growth

Since the common relay cases (1 and 2), Figure 11, always contain the necessary code transmitting and receiving relays for 128 points, additional points can be added in groups of eight by simply adding another point relay case. A standard switchboard unit 24 inches wide will accommodate 32 points (see Figures 10 and 11). A 28 inch unit will cover 56 points, a 32-inch unit 96 points. For systems larger than 96 points, two units are required. To facilitate easy expansion the switchboard units are originally drilled for the maximum number of point key and lamp groups and relay cases. For unequipped points the holes for the key and lamp groups are buttoned and cover plates are provided in place of the relay cases not required at that time.

Any system can be changed from operation over one type of channel to another by simply substituting the proper line relay case.

Telemetering Selection

Telemetering equipment, suitable to the type of channel used, can be connected by supervisory selection to give intermittent readings over the supervisory channel. Torque balance telemeters are used for direct metallic wire channels, and impulse or frequency-type telemeters are used for operation through insulating transformers or over a carrier current channel. A separate additional metering channel is recommended for readings directly associated with a control function, or if a continuous reading of a selected quantity is desired.

Telephone Communication

Telephone equipment can be provided to operate over the supervisory channel between the control and outlying stations. Carrier current equipments are usually provided with integral telephone equipments which do not require a supervisory selection point for ringing.

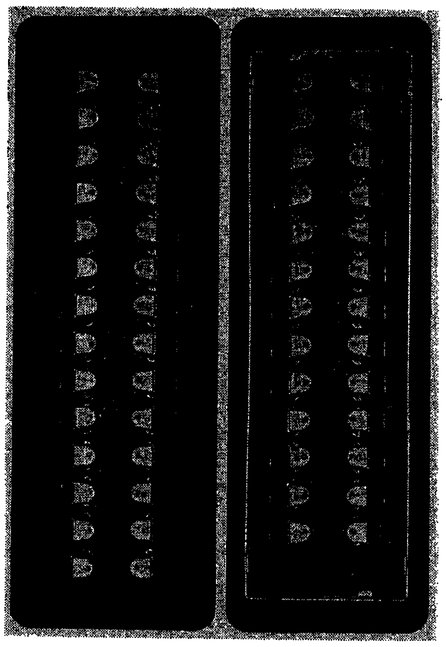


Figure 12. Drawout cases containing telephone relays. Right-hand view cover in place, left-hand cover removed

Control Power

A storage battery is required to furnish control power to the supervisory equipment at the control station and each outlying station. Recommended operating voltage is 48 volts of direct current but equipments can be furnished for 125-volt d-c operation where required. Separate batteries with suitable charging equipments are usually provided for the

Figure 13. Drawout carriage being removed from case for inspection

supervisory system, but station control batteries can be used as sources of control power provided they are ungrounded and will maintain a voltage within the operating limits of the equipment. These limits are 42 to 56 volts for 48-volt systems and 105 to 140 volts for 125-volt systems. The actual current requirements are quite low, and the continuous drain required for the average size system is approximately two amperes.

Conclusions

The space code selector supervisory system described in this paper meets all the essential operating requirements previously outlined. In the design of the equipment, special emphasis on providing maximum flexibility resulted in the following:

1. The system can be expanded to take care of future growth by the simple addition of point relay cases in the previously drilled switchboard units.
2. The system can be expanded to handle multiple outlying stations.
3. Any type of channel can be used by changing the line case.
4. The system can be furnished for either 48 or 125 volts of direct current.
5. The system can be built as an integral part of the switchboard with all devices harmonizing with standard switchboard equipment.

Discussion

W. A. Derr (Westinghouse Electric Corporation, East Pittsburgh, Pa.): Mr. Forrest and Mr. Schirmer have listed what they feel are the basic requirements of a supervisory control system without placing any particular emphasis on any of these requirements. There are two basic requirements of a supervisory control system which warrant special stress. These two basic requirements are accuracy and reliability. The degree of accuracy and reliability obtained with a supervisory control system is determined by the principle of operation and the caliber of the component parts. The list of requirements presented by the authors could be expanded to include other items which are just as important as some of those listed. Following are several such additional items:

1. The component parts should be chosen so that maintenance requirements are kept to a minimum.
2. The number of signal responsive elements at each station should be kept to a minimum.
3. Design should provide maximum flexibility as concerns the types of operations which can be performed.
4. For operation over telephone line wires the continuity of the circuit should be continuously supervised.
5. For circuit breaker operation the supervisory control itself should be inherently antipumping.
6. There should be no critical timing in the operation of the system.

An all-relay coded impulse supervisory control system known as Visicode[1,2] has been available for many years. This system meets all of the requirements listed in this discussion as well as all of those listed by the authors. In the Visicode system all impulses are of the same length and polarity. Also, the space time between impulses is not varied. Requirement number 12 in the paper is not clear. The uniform speed mentioned could refer to the operate time after selection or to the selection time itself. It is assumed that it refers to the operate time after selection. If it was meant to refer to the point selection time, the statement is not correct. Actually the system described in the paper does not have uniform selection time for all points.

The paper indicates the system described is designed for a maximum of 128 selections. While the necessity of more than 128 selections in one station is rare, the necessity of more than 128 selections for a single multistation system is not rare. There are many multistation systems in operation which already involve more than 128 selections or which are designed for expansion to more than 128 selections.

The paper states that the selection and check codes are based on differentiation between spaces of one and two units of time. However, no mention is made as to the actual length of these times. Due to the change in release time of relays over the operating voltage range, it appears that the units of space time would have to be relatively long for reliable operation if the 2-to-1 ratio is maintained.

The arrangement of the individual circuit control indicating lamps and keys as well as the arrangement of the common keys as described in the paper has been used with the all-relay coded impulse system mentioned previously[1,2] for a number of years.

The use of drawout relay cases appears to offer no advantage for the following reasons:

1. The expense of stocking completely assembled relay units probably could not be economically justified. Relatively few individual spare relays should be required for an all-relay supervisory control system.
2. Since properly applied telephone-type relays require such infrequent attention, connecting jack contacts could well result in more difficulty than the relays themselves.
3. Experience indicates that difficulty with a particular relay can best be located as it operates as part of a complete system rather than as it operates individually. Removing an assembly of relays from a switchboard panel after having located the relay in difficulty is of no more advantage than having the relays readily accessible on the switchboard.
4. The drawout case has not eliminated the necessity of unsoldering and soldering for individual relay replacement.

Figures 12 and 13 of the paper indicate that the relays employed are somewhat smaller than the type normally used. The reason for this is not obvious since there is a negligible saving in space at the possible sacrifice of durability and ease of relay maintenance.

The paper recommends the use of a separate channel for telemetered indications associated with a control function. This is an economic disadvantage since a large number of applications require telemetered indications in conjunction with control functions. It is essential to have simultaneous telemetering with the control of tap changers, speed and load control of generators, and the like. Providing a separate channel for such telemetered indications usually would be difficult to justify. The providing of telemetered indications directly associated with control functions without the use of a separate additional channel has been a standard arrangement with the Visicode system for the past 15 years. This is true for operation over telephone-type line wires and carrier or microwave channels.

The paper also indicates that a separate channel is required for any continuous telemetered reading. Providing a single continuous reading over the supervisory control channel which is only momentarily interrupted while some other operation is taking place has also been a standard arrangement with the Visicode system. The equipment can be arranged so that any one of several telemetered indications can be provided over the supervisory control channel, with the operator being provided with the necessary keys and lamps to select any one of the quantities for continuous indication. This is known as continuous selective telemetering.

A 3-wire supervisory control system with selection based on the location of long and short impulses was developed in 1923. An all-relay single channel system employing the same number of impulses for all selection codes with selection being determined by the location of long and short impulses was developed and applied by the Westinghouse Electric Corporation in 1936. The system described by Mr. Forrest and Mr. Schirmer is similar to these systems except for the use of long and short pauses between impulses instead of long and short impulses.

In conclusion, the system described in the paper does not employ operating principles which are new to the art or which have not been employed previously. However, the detailed circuit design of the system described by the authors has apparently imposed certain operating limitations which do not exist in other modern all-relay supervisory control systems.

REFERENCES

1. DEVELOPMENT OF A TWO-WIRE SUPERVISORY CONTROL SYSTEM WITH REMOTE METERING, R. J. Wensley, W. M. Donovan. *AIEE Transactions*, volume 49, October 1930, pages 1339–43.

2. VISICODE SUPERVISORY CONTROL, L. J. Cissna. *Electric Journal* (East Pittsburgh, Pa.), volume 28, July 1931, pages 424–30.

G. S. Lunge (General Electric Company, Schenectady, N. Y.): It is interesting to compare the characteristics and construction of the supervisory control system described by the authors with the type of code selector supervisory control equipment built for many years by the same manufacturer and of which there are numerous installations still in operation.

The older equipment utilized selection codes consisting of combinations of impulses of two different polarities. Where a direct metallic pilot wire channel without insulating transformers was not available, two audio-frequencies ordinarily were used to represent the two polarities required. Thus a simple telegraph-type channel, such as an unmodulated carrier pilot relay channel, to handle impulses of one polarity only would not have been enough. This was a particularly severe handicap for carrier supervisory control applications.

Instead of using only one length of impulse and two widely different lengths of spaces between impulses, the older equipment used three different lengths of impulses, thus requiring more careful relay timing adjustment to distinguish between them.

Only one (long) impulse was used for the final control (close or trip) operation, whereas now a 4-impulse code is used for this, for greater safety.

The older equipment was designed for 125-volt battery operation only and also required a mid-tap from the battery, whereas the equipment described in the paper can be designed for either 48 volts or 125 volts and needs no mid-tap from the battery. In common with most types of supervisory control equipment, it was not convenient to add to it beyond the number of points for which wiring and drilling had been provided in the initial equipment.

The older (polarized) code selector supervisory equipment contained, besides many telephone-type relays, numerous step-by-step selectors similar to line switches used in automatic telephone practice. These selectors required occasional lubrication and adjustment. In contrast to this, the supervisory equipment described in the paper is an all-relay system intended to operate without maintenance other than periodic inspection.

E. F. Forrest and P. W. Schirmer: The authors wish to thank the discussors of this paper for their remarks and for calling attention to some additional requirements which were not specifically mentioned. The supervisory system described in this paper meets all the requirements outlined by Mr. Derr in the opening paragraph of his discussion.

The time required to complete a point selection (select a point and check the selection) with the supervisory system described in this paper is approximately 2.5 seconds ± 0.3 seconds. This variation in selection time is so small that in ordinary operation it is very difficult to detect. The operate time after selection is always uniform.

In the design of a standard supervisory system such as this, it is very desirable to include initially in the common equipment all of the components required to transmit and receive the number of codes that ultimately might be required in the majority of cases. From the nature of the scheme it is evident that 128 is not an inherent limit on the number of points for which the base equipment could be designed. However, our experience shows that even multistation systems requiring more than 128 selections are very rare. An important feature of this standard design is that the first two relay cases always contain the necessary relays required to transmit and receive the codes for the maximum number of points, and expansion up to that point can be accomplished by the addition of "point" relay cases without modifying the basic common equipment.

The supervisory system described in this paper does not necessitate the use of a separate channel for telemetered indications. Selected or continuous readings, interrupted only momentarily while control or indication codes are being transmitted, can be transmitted readily over the supervisory channel. However, if uninterrupted readings are necessary, a separate telemetering channel is required. Even a separate channel does not necessarily mean a separate pair of wires, as several channels can be provided over one pair of wires by the use of audio-tone equipment or over carrier by the use of a number of different frequencies.

The drawout relay case construction used in this design was not adopted to make possible the stocking of completely assembled relay units nor to make it easier to locate troubles and replace relays. However, in some installations, these points do have some merit. The primary reasons for utilizing the drawout relay case construction are as follows:

1. The common or basic equipment required to transmit and receive codes for the maximum number of points can be included in two standard cases (numbers 1 and 2). Any size system can be built or expanded up to the maximum without modifying this basic equipment.
2. A system can be expanded to take care of future growth by the simple addition of point relay cases. It is much simpler to install and connect complete relay cases than individual telephone relays.
3. Special features or channel conditions can be accommodated by varying the components in the line case. This makes it possible to change the type of channel or operating features after an equipment has been installed by changing the line case without modifying the basic supervisory system.
4. The drawout relay case provides a dust-proof construction which harmonizes with standard switchboard devices.

The telephone-type relays used in this equipment were selected as a result of extensive tests on the many available types, some of which were the result of wartime developments for the aircraft industry where light weight and extreme reliability were major requirements. The selection was made primarily upon the basis of reliability and long life, size having been a secondary consideration. These comparative tests led to the conclusion that the type selected was the best available regardless of size. Some of the features described in the paper obviously were not new, but we believe it is fair to say that the salient ones, such as drawout relay cases containing standard assemblies that can be put together to build any size system, in general represent definite improvements over previous designs.

A Convenient Method for Determining Generator Reserve

HOWARD P. SEELYE
FELLOW AIEE

HE study of generator reserve requirements by methods using probability
athematics has received a considerable
nount of attention in recent years and
veral papers have been published de-
ribing such methods. For the most part
ie calculations required are quite long
id involved, and the underlying prin-
ples are somewhat complex. Although
ppreciable benefits are to be derived
om studies of system reserve require-
ents by these means, it is doubtful if
ney have had very general or widespread
se. There would seem to be a need for
me short, direct method or guide where-
y any system could arrive at the order
f its reserve requirements directly, pro-
ided certain simple basic factors are
nown.

In a paper presented in 1947[1] certain
ather simple algebraic formulas for the
udy of the reserves necessary to take
are of forced or emergency outages of
enerators were developed, and their
ethod of application was indicated.
hese formulas provided for the use of
idividual basic data as to average fre-
uency and duration of outage for the
ystem being studied. They also cover
he use of any variety of different sizes
f machines and different running times.
t was indicated in that paper, however,
hat for most systems, consisting of a
iverse assortment of generator sizes, the
ork could be greatly simplified and no
reat error introduced, if it were as-
umed as an approximation that all of the
achines were of the same size, that is,
he average for the system.

This simplified approximate method
ow has been extended to the develop-
nent of a few charts whereby the per-
entage of reserve in terms of total system
apability or total system load may be found directly. These charts are given in this paper with a brief explanation of how they were derived from the preceding paper and how similar charts for other variations could be determined if required.

Basic Formula

The formula given in the preceding paper,[1] which is used in developing the charts, is as follows:

The expectancy of outages of *j machines or more,* is

$$E_{j+} = \left(\frac{n}{T}\right)^j \frac{d^{j-1}}{(j-1)!} \epsilon^{-\frac{nd}{T}}$$

where

n = the number of generators running
d = the average duration of individual outages (in years)
T = the average interval between individual outages (in years)
j = the number of generators out at the same time
ϵ = the base of Naperian logarithms, 2.7183

An outage refers to the whole group of individual generator outages characterized by j, the largest number of generators out together at any time during the continuous duration for the whole group. E_{j+} is the expectancy, given in terms of a decimal. For example, if $E_{j+} = 0.05$ it means that the outage of the corresponding j machines (three perhaps) *or more* may be anticipated 5/100, or five times in 100 years, or once in 20 years on the average.

The quantities d, the average duration of individual outages, and T, the average interval between outages can best be obtained from records on the system being studied, if such records are available. If not, they may be assumed from the records of other systems, national averages, or other such sources, or they may be estimated from experience. For example, an average of one outage each two years ($T = 2$) of an average duration of one week ($d = 0.02$) might be assumed to apply.

Application of Average Size

As explained previously, the formula can be applied directly if all of the machines on the system are of the same size and have the same percentage of running time to total time, or a good approximation reached if all machines are assumed to be of the same size, that is the average size, and to have the same average running time. Figure 3 of the previous paper shows how a simple chart can be plotted with such average values. For this chart the basic outage data $d = 0.02$ and $T = 2$ were chosen. It also was assumed that an expectancy of once in 30 years would be satisfactory ($E_{j+} = 0.033$) that is an outage would not be expected to equal or exceed the reserve oftener than once in 30 years. This chart is repeated here as Figure 1 for reference. The curve shows the relation between the total number of generators and the indicated total reserve required—150 megawatts for 15 machines for example.

Development of New Charts

It is a rather obvious operation to convert the lower scale of this chart, which now shows megawatts of reserve, to *number* of generators (of average size) which should be in reserve by dividing by the average size. If the average size were 50 megawatts for example, the scale divisions would read 1, 2, 3, 4, and 5. It also is quite obvious that by a further conversion the curve could be replotted to show

---

Paper 49-278, recommended by the AIEE Joint Subcommittee on Application of Probability Methods to Power System Problems and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17-21, 1949. Manuscript submitted June 17, 1949; made available for printing September 1, 1949.

HOWARD P. SEELYE is Chief Engineer of the Engineering Department of The Detroit Edison Company, Detroit, Mich.

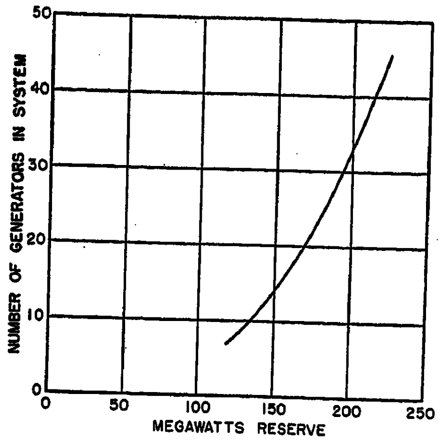


Figure 1. Variation of reserve requirement with number of generators

Average size of generators = 50 megawatts
Reserve will not be exceeded oftener than once in 30 years
Average interval between forced outages of each generator = 2.0 years running time
Duration of outages = 0.02 year

the percentage of the total number of generators which should be held in reserve.

The values for this replot would be obtained by dividing the number in reserve by the total number of generators for various points along the curve.

This is essentially the method used in obtaining the curve shown in Figure 2. The percentages are determined directly by calculation, without the intermediate steps just described.

The method can probably best be illustrated by a sample calculation:

$$E_{j+}=\left(\frac{n}{T}\right)^j \frac{d^{j-1}}{(j-1)!}\epsilon^{-\frac{nd}{T}}$$

If $E_{j+}=0.033$; $d=0.02$; $T=2$

Then

$$0.033=\left(\frac{n}{2}\right)^j \frac{0.02^{j-1}}{(j-1)!}\epsilon^{-0.01n}$$

For a system with 20 generators, with average running time 75 per cent

$$n=0.75\times 20=15$$

$$0.033=\left(\frac{15}{2}\right)^j \frac{.02^{j-1}}{(j-1)!}\epsilon^{-0.15}$$

This equation may be solved for j by calculating the value of the left-hand member for various simple values of j and then interpolating to obtain the value of j corresponding to $E_{j+}=0.033$.

$$\text{If } j=2\ldots\ldots\frac{15}{2}\Big)^2 \frac{0.02^{(2-1)}}{(2-1)!}\epsilon^{-0.15}=$$

$$7.5^2\times 0.02\times 0.86071=0.9683$$

$$j=3\ldots\ldots\left(\frac{15}{2}\right)^3 \frac{0.02^{(3-1)}}{(3-1)!}\epsilon^{-0.15}=$$

$$7.5^3\times\frac{0.0004}{2}\times 0.86071=0.07262$$

$$i=4\ldots\ldots\left(\frac{15}{2}\right)^4 \frac{0.02^{(4-1)}}{(4-1)!}\epsilon^{-0.15}=$$

$$7.5^4\times\frac{2^3\times 10^{-6}}{6}\times 0.86071=0.003631$$

It has been discovered that the plotted curve of j against n is approximately a straight line on semilog paper. The value of j corresponding to $E_{j+}=0.033$ may therefore be calculated by use of the formula

$$j=3+\frac{\log E_3-\log E_j}{\log E_3-\log E_4}=$$

$$3+\frac{\log 0.07262-\log 0.03333}{\log 0.07262-\log 0.003631}$$

$$=3+\frac{0.33822}{1.30103}=3.260$$

Converting to percentage of total generators, 3.260/20 = 16.3 per cent.

Similarly, for a system with 40 generators, 75 per cent running time, $n = 30$.

$$E_{j+}=0.033=\left(\frac{30}{2}\right)^j \frac{0.02^{j-1}}{(j-1)!}\epsilon^{-0.3}$$

For

$j=2\ldots\ldots 15^2\times 0.02\times 0.74082=3.3337$
$j=3\ldots\ldots 15^3\times 0.0002\times 0.74082=0.5000$
$j=4\ldots\ldots 15^4\times 1.33\times 10^{-6}\times 0.74082=0.05000$
$j=5\ldots\ldots 15^5\times 0.667\times 10^{-8}\times 0.74082=0.00375$

$$j=4+\frac{\log 0.04995-\log 0.0333}{\log 0.04995-\log 0.003746}=4.1566$$

In percentage of total number of generators 4.1566/40 = 10.39 per cent.

Criterion for Expectancy

It would seem that an outage which would be expected to occur somewhat less frequently than once in 30 years could be reasonably faced without providing full reserve for it. That is, such an outage might require unusual measures of dropping voltage or frequency or even of some load. This period has, therefore, been used as the criterion for expectancy throughout all of the calculations in this paper and the charts shown: $E_{j+}=0.033$. The reserve provided by this criterion would take care of all outages which might be expected more frequently than once in 30 years, and also would take care of most of the amount of the outage in the more infrequent cases where the reserve would be exceeded. Similar charts could be calculated by the methods indicated for other expectancies such as once in 20 years or once in 50 years. It should be noted, however, that, as was indicated in the preceding paper, the curve between reserve and expectancy is relatively steep in this range and not much advantage is gained for the more frequent expectancy nor is the penalty very large for the less frequent one. The effect would be approximated from the charts given here, if desired, by slightly modifying the reserve percentages indicated.

Reserve in Terms of Load

In Figure 2, the per cent reserve is expressed in terms of the total capability of the system. It is probably more frequently desired to have reserve in terms of system load. When loads are estimated in advance, the total capability which should be provided to serve them will be at least equal to the load plus the necessary amount of reserve. In most cases it will be more convenient to know the capability needed for a given load than the load for which a given capability will provide. In the calculations here described it has been assumed that the capability remaining after the greatest outage within the expectancy used would be sufficient to carry the load. It is a simple matter, therefore, to convert the reserve percentage in terms of capability to reserve percentage in terms of load. For the 40-generator example used previously, the reserve indicated was 10.39 per cent of capability

$$\frac{10.39}{100-10.39}=11.6 \text{ per cent of load}$$

Figure 3 has been plotted on this basis.

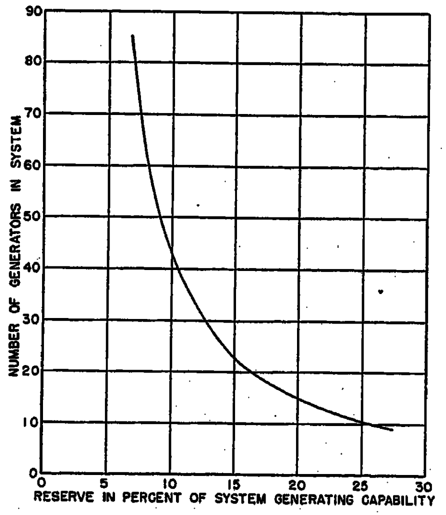


Figure 2. Generator emergency reserve in per cent of system capability

Reserve will not be exceeded oftener than once in 30 years
Average interval between forced outages of each generator = 2.0 years running time
Duration of outages = 0.02 year
Average running time = 75 per cent

Variations in Basic Data

In order that the charts may be useful for a wide variety of local conditions, the effect of several variations in the basic data is included in them, as follows:

Variation in Outage Frequency: The average frequency of individual outages may vary considerably with different systems, depending on type of equipment, maintenance policy, loading and operating practices, and other local conditions. Separate curves have therefore been plotted on Figure 3 for several values of average interval (T), namely 1.5, 2, and 2.5 years respectively. The curves are approximately parallel so values for other intervals such as $T = 1$ or $T = 3$ or for intermediate values can be approximated easily if needed.

Variation in Average Length of Outage: The average length of individual machine outages used in calculating the charts was $d = 0.02$, or approximately one week. On some systems the average outage period may be appreciably different. A variation in this factor makes a variation in the reserve values throughout the range of the chart, which may be approximated by applying a constant percentage of change to each of these values. Figure 4 gives these modifying percentages for any change in d from the assumed value of 0.02. For example, if the average duration of outage were 0.03 (about 11 days) the reserve would be about 13 per cent greater than the value obtained from Figure 3 for the system in question. For a system of 30 generators with $T = 2$, the figure shows 14.3 per cent reserve. For $d = 0.03$ this would become $14.3 \times 1.13 = 16.2$ per cent.

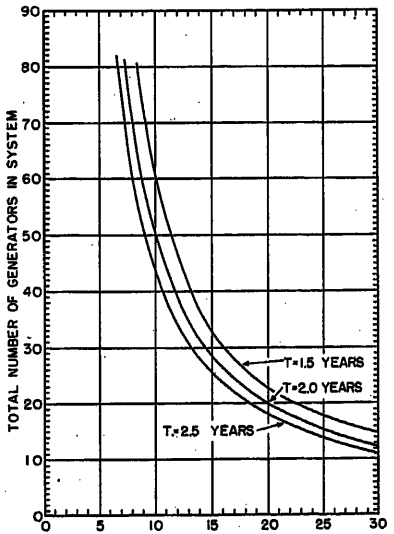


Figure 3. Generator emergency reserve in per cent of system load

Reserve will not be exceeded oftener than once in 30 years

T=average interval between forced outages of each generator in years of running time

Duration of outages=0.02 year

Average running time=75 per cent

Variation in Running Time: In the calculations for Figure 2 and 3, an average running time of 75 per cent of total time was used for all generators. If a different value is experienced, the reserves obtained from the charts will be varied accordingly. The effect may be approximated by a constant percentage applied to the whole range of values. These percentages for any variation for the assumed 75 per cent are shown in Figure 5. For example, if the average running time is only 60 per cent, the reserve values given by Figure 3 should be reduced by about 8 per cent. For the 30 generator system, therefore, the reserve required would be reduced from the 14.3 per cent given by the chart to $14.3 \times 0.92 = 13.2$ per cent.

New Charts: The effect of the several variations have been indicated by the supplementary curves and charts for convenience in using them for a specific problem without appreciable calculation. If frequent use of the charts is to be made with such variations, it will be more satisfactory to alter the chart scale accordingly or plot a new chart for the individual conditions.

Use of Charts

The practical use of the charts is fairly obvious and has been pretty well indicated in the preceding discussion. They are particularly applicable to systems having all steam-driven generators for which seasonal variations in availability are not a large factor, as might be the case with hydro. It also was assumed initially that the system would have seasonal variations in load which would provide margins of capability and these would be quite fully used for scheduled maintenance. On such a system, an outage at one time of the year is just as important as one at any other time, so the basic data have all been taken on a full yearly basis. For other conditions, the results could be modified, of course, as was indicated in the preceding paper.

For any system which reasonably well fits these conditions, if the values of the basic data (T, d, and running time) are known, or can be assumed, the charts will give a good idea of the order of the reserve required for emergency outages. Referring to Figure 3, a system with 15 generators should have about 25 per cent of its peak load as reserve ($T = 2$) to meet the criterion of not being exceeded by an outage oftener than once in 30 years. If, however, the system increases so as to have 25 generators, the reserve percentage need be only about 16.5 per cent. If the latter system is interconnected with another system having 20 generators, or 45 in all, the reserve for the pool need be only 10.7 per cent. The advantage of interconnection in reducing required reserve is evident. It is interesting to note, however, that this advantage becomes progressively smaller as the pool grows larger, the curves becoming relatively steep.

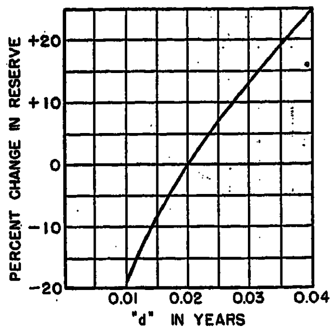


Figure 4. Change in reserve with variation of duration of outage

In per cent of reserve for d=0.02

It is significant that these charts show the reserve required as dependent upon the number of machines rather than upon their ratings. A system would require less reserve for 30 machines than for 20 even with the same system load. This is reasonable since a large machine is just as likely to go out as a small one but the effect of an outage on the total system capability will be much greater with the larger one. The magnitude of this factor in the decision on the size of a new generator for a system might be approximated from the chart.

Smaller Systems

For systems with a comparatively small number of generators, say less than 15, the charts are not as applicable in the form given as they are to larger systems. This is due to the fact that individual generator units are a larger percentage of the total capability, and the assumption of a smooth curve rather than a stepped curve, for the relation between reserve and the expectancy of its use, upon which this development was based, may lead to appreciable discrepancies. This was shown in the previous paper in its Figure 1. The greater the diversity in the sizes of the machines used, the nearer the smooth

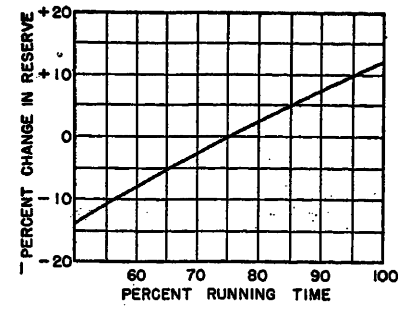


Figure 5. Change in reserve with variation of running time

In per cent of reserve for per cent of running time = 75

curve will fit, since there are more intermediate values of possible outage. For the extreme case of all machines of the same size, the problem may better be considered from the standpoint of the order of protection which is provided by one, two, or three units as reserve.

Figure 6 has been developed to show this, using the same basic data as for the previous charts, that is $T = 2$, $d = 0.02$, and 75 per cent running time. It is plotted on semilog paper for convenience. The left-hand curve shows the expectancy of a simultaneous outage of two units or more in terms of the average expected interval in years. The right-hand curve shows the expectancy of outage of three units or more.

For example, on a system having five units, a 2-unit outage (or more) might be expected about once in 15 years, hence one of the five generators (20 per cent of capacity or 25 per cent of load) in reserve would protect against the more frequent occurrence of less magnitude. On the other hand, a 3-generator reserve would probably never be called upon, the expectancy of such an outage being of the order of hundreds of years, and there are no intermediate possibilities, assuming all units to be the same size. Similarly, for a 10-generator system, a single generator reserve (10 per cent of capacity or 11.1 per cent of load) would be exceeded by an outage of two units or more once in four years.

If such operation is not to be tolerated, a 2-unit reserve (20 per cent of capacity or 25 per cent of load) would be required and this would be exceeded by an outage of three or more units once in 100 years, and should therefore be very good protection.

It should be kept in mind that comparisons of the figures taken from Figure 6 should be made with those from Figure 2 rather than Figure 3 since Figure 2 and Figure 6 are both in terms of generator capability, whereas Figure 3 is in terms of load. For 10 generators, Figure 2 would indicate a reserve of about 25 per cent for a 30-year criterion, or $2\frac{1}{2}$ generators. This is intermediate between the 4-year interval for two generators and the 100-year interval for three generators from Figure 6.

Other Reserves

It should be emphasized that the reserve covered by the calculations and charts in this paper are only the reserves for emergency outages of generators. On many systems, additional reserves will be found advisable or necessary to provide for scheduled maintenance or for unforeseen loads or other conditions. It is recognized that these matters are part of the total consideration of reserves, but they are subject to other controlling factors than those upon which this present study has been based. It is believed that for the purpose of getting a quick estimate of the reserves required for emergency outages, without prolonged calculations, these charts will be found very useful to many power systems. It must be recognized, however, that neither this method nor that of the longer more accurate calculations can be depended upon solely to give the final answer. They are at best merely guides and the results indicated must always be tempered and qualified by good judgment based upon experience and the peculiar conditions of the case being studied.

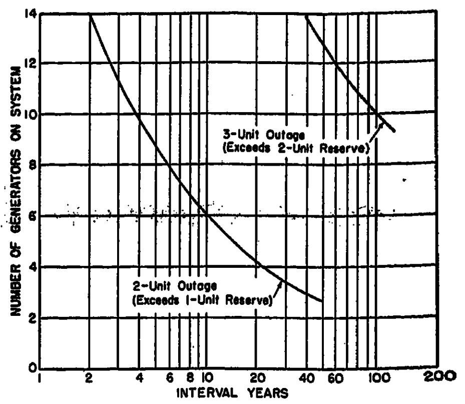


Figure 6. Protective value of 1-unit and 2-unit reserve of small systems

Average running time = 75 per cent

Reference

1. Outage Expectancy as a Basis for Generator Reserve, Howard P. Seelye. *AIEE Transactions*, volume 66, 1947, pages 1483–88.

No Discussion

Operation of a Ground-Fault Neutralizer on a Regulated Distribution System

H. R. TOMLINSON
MEMBER AIEE

F. B. HUNT
ASSOCIATE AIEE

ARCING grounds occurring on an underground delta-connected distribution system produce oscillatory overvoltage stresses which result in simultaneous failures of the cable insulation. These have been the predominant cause of primary circuit outages in the operation of a 2,400-volt ungrounded, distribution system serving a load of 12,000 kva in five square miles of urban territory. With a view toward improving service by the suppression of arcing grounds, a ground-fault neutralizer has been installed in accordance with the well-known neutralizer theory.

Improvements to operation have resulted by reason of the two basic benefits to be gained by the neutralizer. The neutralization of the capacitive fault currents has eliminated the severe arc damage to cables formerly experienced during phase-to-ground failures. The slow rate of recovery voltage tending to restrike the arc has converted the former oscillatory arcing disturbances into delayed recurrent grounds which ultimately resolve into solid grounds. This modification of the recovery voltage by the neutralizer is shown effectively in Figure 1, wherein are compared oscillograms taken during field tests without and with the neutralizer in operation.

However, the application of a neutralizer to a distribution system has given rise to some special problems, particularly by the effects of the zero sequence voltages produced by single-phase feeder voltage regulators. These effects have been analyzed for both ground-free and grounded conditions, after having been observed during field tests. Field tests also have been made to accumulate oscillograph records of the recovery voltages obtainable on an extensive cable system by variations of neutralizer tuning, in addition to wave form records of the fault currents.

Description of System

The distribution system on which the neutralizer has been applied is supplied by a substation having delta-connected step-down transformers of 17,000-kva total capacity, and consists of 15 primary circuits each of which is regulated by two single-phase induction regulators connected in open delta. Feeder primaries, which for the most part are underground, utilize 141 conductor miles of single-conductor lead-sheathed cable. The ground fault current of this primary system, previous to the neutralizer installation with 2,400 volts maintained on the bus and all feeder regulators in neutral positions, was 47 amperes.

The neutralizer equipment consists of three standard distribution transformers connected 2,400 volts Y-grounded to 240 volts open-cornered delta, with three air-core current-limiting reactors connected in series across the opened corner to control the quadrature lagging neutralizer current. This arrangement was selected for the reason that current-limiting reactors of the proper ohmic value were available for this use. The neutralizer performs the same as one with $(N/3)^2$ ohms reactance installed in the primary neutral, where N is the transformation ratio of the neutral deriving transformer.

The neutralizer equipment is connected to the station service feeder which is unregulated and at all times energized from the operating main bus.

When the system is in normal operation, the cable capacitance and the neutralizer inductance are in series resonance. This is changed to parallel resonance when a ground fault occurs. Hence, the application of a neutralizer to a regulated distribution system requires an analysis of the effects of feeder voltage regulators on the two resonant circuits.

This discussion will be confined to the effects of open delta-connected regulators. However, similar results will be obtained with other single-phase regulator connections.

Zero Sequence Voltage Due to Regulators

The open delta-connected regulators in any off-neutral position cause a zero sequence voltage to be impressed upon the series circuit containing the neutralizer inductance and cable capacitance. As shown in Figure 2, the vector representation of this voltage can range from the system neutral to any point within a rhombus, the center of which is at the neutral of the bus voltages, and the major and minor axes of which are $E_R/\sqrt{3}$ and $E_R/3$ respectively. The maximum zero sequence voltage, as resulting from all regulators being in the extreme positions, is in phase with or in phase opposition to the neutral voltage

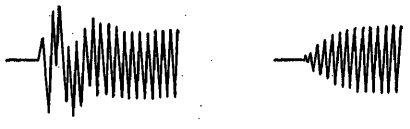


Figure 1. Oscillograms of recovery voltages

Paper 49-282, recommended by the AIEE Protective Devices Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 18, 1949; made available for printing September 8, 1949.

H. R. TOMLINSON and F. B. HUNT are both with the New England Power Service Company, Boston, Mass.

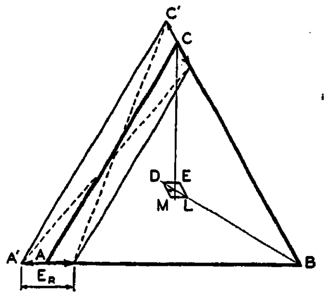


Figure 2. Range of zero sequence voltage due to voltage regulators

ACB—bus voltages
AA', CC'—regulator voltages
$DELM$—range of zero sequence voltage
E_R—voltage range of regulators
$DL=\frac{E_R}{\sqrt{3}}$, $EM=\frac{E_R}{3}$

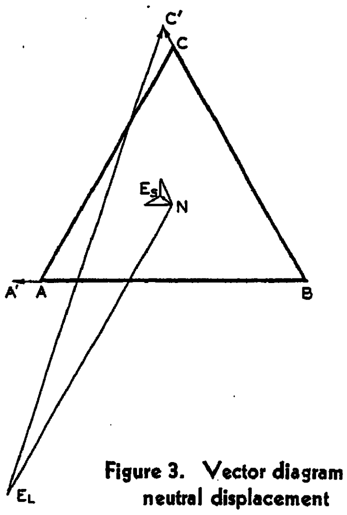


Figure 3. Vector diagram of neutral displacement

ACB—bus voltages
AA',CC'—regulator voltages
NE_s—zero sequence voltage
NE_L—quadrature voltage on neutralizer
E_LC'—maximum voltage to ground. Based on resonant tuning and coil constant = 40

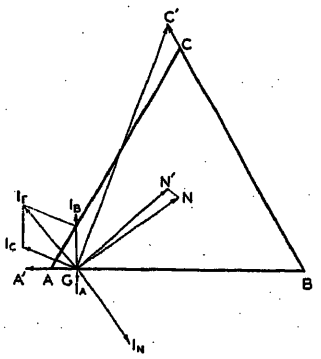


Figure 4. Vector diagram during fault

ACB—bus voltages
AA', CC'—regulator voltages
AG—regulator voltage on grounded cable
GA', GB, GC'—ground-to-phase voltages
I_A, I_B, I_C—A-B-C phase capacity currents
GI_F—total capacity current
GN'—effective voltage of capacity current
GN—voltage on neutralizer
GI_N—neutralizer current

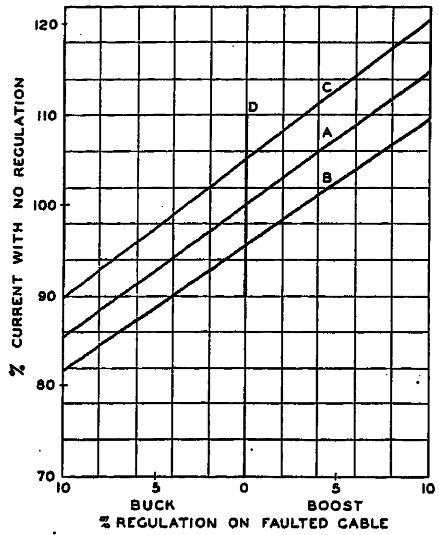


Figure 5. Range of capacity and neutralizer currents

A—neutralizer current
B—minimum capacity current (regulators full boost)
C—maximum capacity current (regulators full buck)
D—capacity current unregulated cable grounded

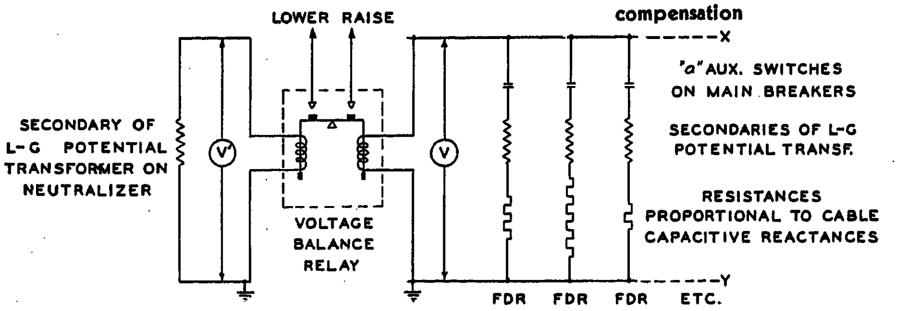

TO
NEUTRALIZER REGULATOR
LOWER RAISE
SECONDARY OF L-G POTENTIAL TRANSFORMER ON NEUTRALIZER
V
VOLTAGE BALANCE RELAY
V
X
'a' AUX. SWITCHES ON MAIN BREAKERS
SECONDARIES OF L-G POTENTIAL TRANSF.
RESISTANCES PROPORTIONAL TO CABLE CAPACITIVE REACTANCES
Y
FDR FDR FDR ETC.

Figure 6. Control scheme for zero sequence compensation

to the unregulated phase, depending on whether the regulators are bucking or boosting the bus voltages. This maximum voltage is considered in the following in order to determine the greatest effects upon the resonant circuits.

The Series Resonant Circuit

When the neutralizer is exactly tuned to the system capacitance, the zero sequence voltage causes a current to flow through the series resonant circuit limited only by the resistance component of the circuit. This current in the neutralizer causes a quadrature leading voltage drop across the inductance equal in magnitude to the resistance drop across it multiplied by its coil constant (coil constant equals ratio of reactance to effective resistance).[1] This quadrature voltage is impressed between ground and the system virtual neutral, and, as shown in Figure 3, causes a maximum voltage to ground several times the value of the normal phase-to-ground voltage. On a system with regulators having an over-all range of 20 per cent, this overvoltage to ground may be four to five times normal, which is counter to the purpose of the neutralizer and indicates that a method must be devised to imit the maximum ground voltage to a tolerable value.

The Parallel Resonant Circuit

In parallel resonance, as when a ground fault occurs on the system, any combination of regulators in the off-neutral position produces different effects in the two parallel branches, and hence complete neutralization of the capacity current under these conditions is not possible without compensating action. The effective voltage on the neutralizer is the ground-to-neutral voltage (zero sequence) of the unregulated system, whereas the voltage producing the flow of capacity current is the ground-to-neutral voltage (zero sequence) of the regulated system.

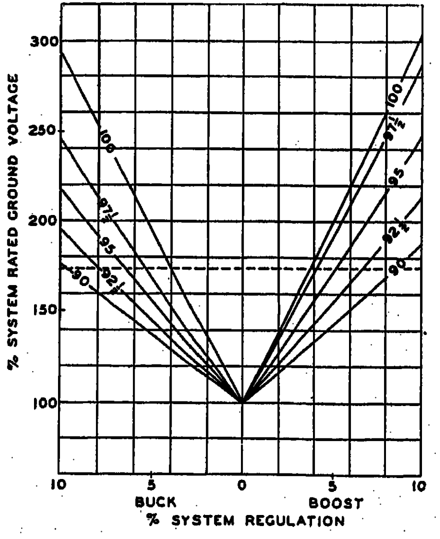
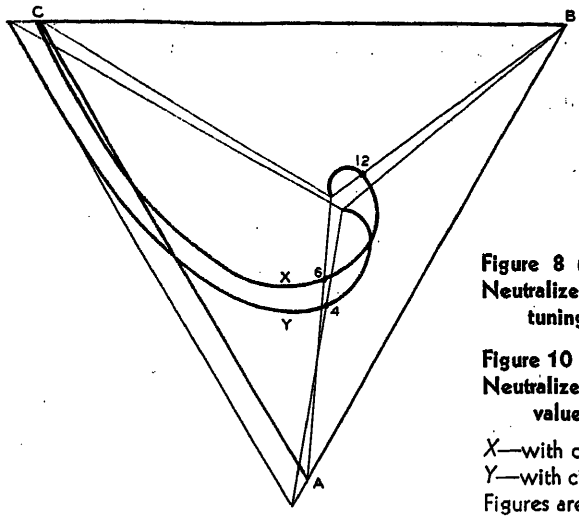


Figure 7. Maximum line-to-ground voltages. Based on neutralizer coil constant of 40

Dotted line is system rated phase-to-phase voltage. Figures on lines are neutralizer tuning in per cent of resonant value



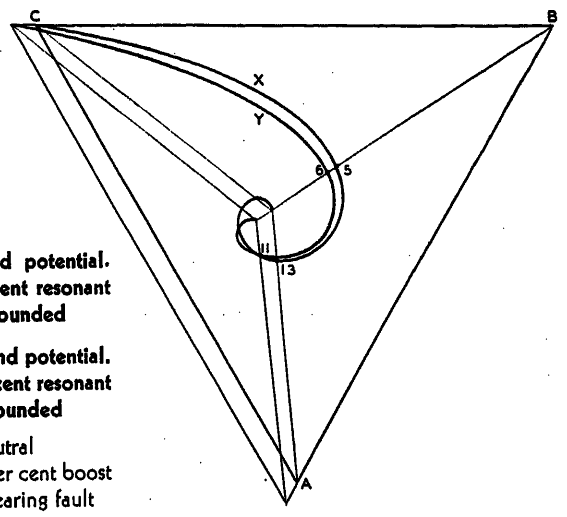


Figure 8 (left). Locus of ground potential. Neutralizer adjusted to 91 per cent resonant tuning. Regulated phase grounded

Figure 10 (right). Locus of ground potential. Neutralizer adjusted to 106 per cent resonant value. Regulated phase grounded

X—with circuit regulators on neutral
Y—with circuit regulators on 5 per cent boost
Figures are time in cycles after clearing fault

The first is affected only by the regulator on the faulted cable, the latter by all regulators on the system including that on the faulted cable. The vector difference of these two ground-to-neutral voltages is the zero sequence voltage of the feeder regulators. This voltage produces a residual current in the fault which has a maximum value, when referred to the capacitive fault current with no regulation, equal in percentage to the regulating range of the feeder regulators. The vector representation of a ground fault is shown in Figure 4, which is based on the case of all feeder regulators in the maximum boost position except the one on the faulted cable, which is in the maximum buck position. This is an improbable operating condition, but it clarifies the effects of regulators on the ground-to-neutral voltages. The residual fault current is the resultant of the neutralizer and capacity currents.

The position of the regulator on the faulted cable establishes the ground reference of the neutral voltages, and thereby affects both the magnitudes and angular positions of the effective voltages on the neutralizer and on the system capacitance. Hence an analysis of the currents resulting from these voltages must be referred to the position of the regulator on the faulted cable. This is done in Figure 5, wherein is shown the range in magnitude of the currents in the neutralizer and capacitance circuits.

As shown by line *A* in Figure 5, the maximum neutralizer current occurs when the regulator on the faulted phase is in the extreme boost position, and hence the current and kilovolt-ampere rating of the neutralizer equipment must be adequate for the conditions imposed on it by the extreme boost position of any regulator on the system.

The magnitude of the capacitive fault current is similarly affected by the operation of the regulator on the faulted phase, and, in addition, varies between lower and upper limits because of the operation of the system regulators. Lines *B* and *C* in Figure 5 are the lower and upper limits of capacitive currents obtainable within probable operating conditions. Both Figures 4 and 5 indicate that maximum capacitance currents result when the system regulators are in maximum buck positions.

Line *D* in Figure 5 indicates the range in magnitude of the resultant capacity current when a ground occurs on the unregulated phase.

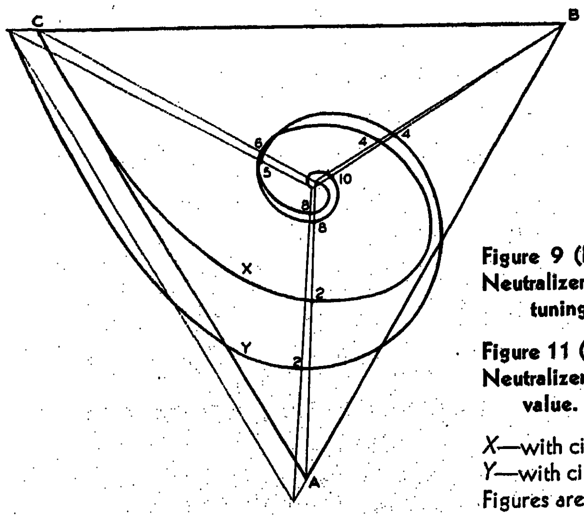
Control of Ground Voltages

By reason of the regulator action, it is evident that a means must be devised to limit the ground voltages during normal operation when a neutralizer is applied to a regulated system. The following three methods are suggested to accomplish this:

1. Install voltage regulators on the neutralizer circuit.
2. Insert reactance in the series circuit by avoiding resonant tuning.
3. Install a saturable core reactor in the neutralizer.

A discussion of each method and their effects during ground fault and normal conditions follows:

1. By equipping the neutralizer circuit with voltage regulators, a compensating zero sequence voltage can be impressed on the neutralizer equal to the same effective voltage on the cable capacitance.



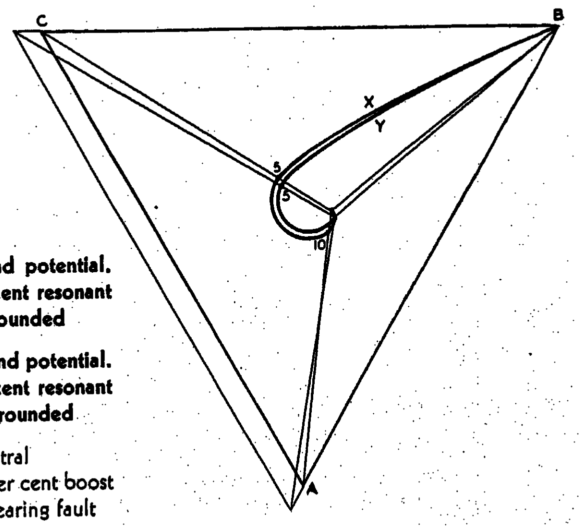


Figure 9 (left). Locus of ground potential. Neutralizer adjusted to 72 per cent resonant tuning. Regulated phase grounded

Figure 11 (right). Locus of ground potential. Neutralizer adjusted to 91 per cent resonant value. Unregulated phase grounded

X—with circuit regulators on neutral
Y—with circuit regulators on 5 per cent boost
Figures are time in cycles after clearing fault

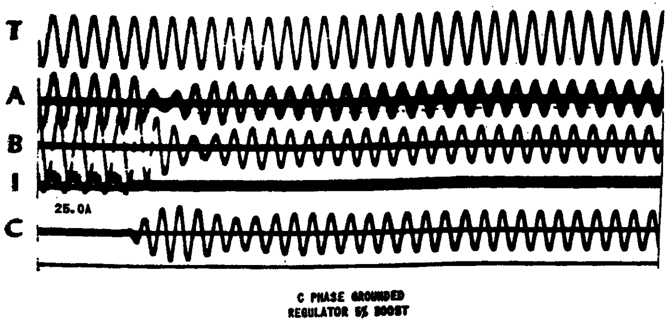


Figure 12. Current in fault and phase-to-ground voltages as a ground is removed from C phase

All regulators on neutral except those on faulted feeder on 5 per cent boost. Neutralizer set on 72 per cent of resonant value

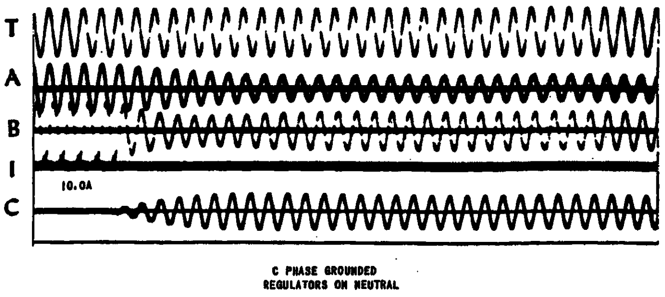


Figure 14. Current in fault and phase-to-ground voltages as a ground is removed from C phase

All regulators on neutral. Neutralizer set on 106 per cent of resonant value

During normal conditions the neutralizer and the system will assume the same neutral, no series resonant current will flow, and the performance will be the same as on an unregulated system. During a ground fault, the effective ground-to-neutral voltages on the neutralizer and the cable capacitances will be the same, hence neutralization of the fault current is obtainable. The performance during fault conditions will be the same as on an unregulated system except that the magnitudes of both quadrature currents will be influenced by the operation of the feeder regulators. The magnitude of the maximum current and voltage thus obtainable defines the rating of the neutralizer equipment.

The neutralizer regulators must have sufficient range to correct for other zero sequence contributions, such as unbalances in cable capacitances, in order to eliminate completely the undesirable effects of series resonance.

The main problem is to make the neutralizer regulators automatically responsive to the ever-changing zero sequence voltage of the system regulators. A suggested method is to use a voltage-balancing relay to match the neutralizer and the system zero sequence voltages, as shown in Figure 6. Line-to-ground potential transformers with loading resistors proportionate to cable capacitive reactances are employed on each feeder, and all are paralleled into the voltage matching relay which operates the neutralizer regulator until V' equals V. An identical control is required for each regulated phase.

By the elimination of neutral displacement, correct indications will be obtained on bus-connected ground-detecting equipment usually employed on ungrounded delta systems. This method also allows for the use of sensitive current or potential ground-indicating relays with the neutralizer equipment.

2. By avoiding resonant tuning with the neutralizer reactor, a net reactance will be inserted in the series circuit, which provides a ready means for controlling the extent of the neutral displacement. Inductive reactance of less than the resonant value will cause leading currents in the series circuit, and greater values will cause lagging currents to flow.

During normal conditions, any net reactance will result in a reduction of the maximum voltage to ground, the amount of which will be approximately the same for equal deviations of the neutralizer reactor above and below the resonant value. Figure 7 shows the resulting voltages to ground obtainable by a series capacitive circuit and with a neutralizer coil constant of 40.

As a precaution against the system resolving into tuned series resonance because of a switching operation, such as the clearing of sectionalizing fuses or the automatic operation of a feeder main circuit breaker, it is advisable to adjust the reactor to an ohmic value less than for resonant tuning, as shown in Figure 7. Planned switching operations that may result in additional capacitance to the system can be provided for by a preliminary adjustment of the reactor to the new conditions.

It is possible that the magnitude of the series current will exceed the required quadrature neutralizing current, in which case the series current will define the rating of the neutralizer equipment.

When a ground fault occurs on the system, the detuning of the neutralizer within the limits of 85 to 115 per cent of the resonant value has no serious detrimental effect on either the arc neutralization or the recovery voltage characteristics. It causes a fundamental frequency residual fault current which, although not desirable, is a small percentage of the un-

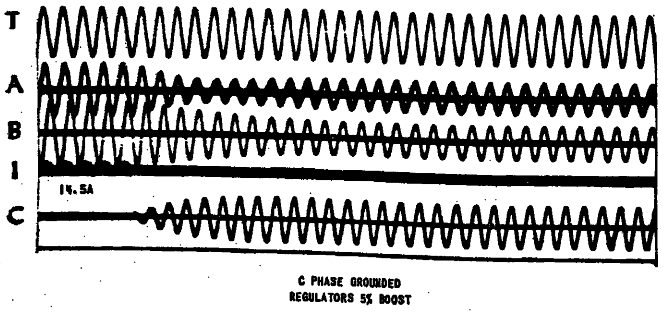


Figure 13. Current in fault and phase-to-ground voltages as a ground is removed from C phase

All regulators on neutral except those on faulted feeder on 5 per cent boost. Neutralizer set on 91 per cent of resonant value

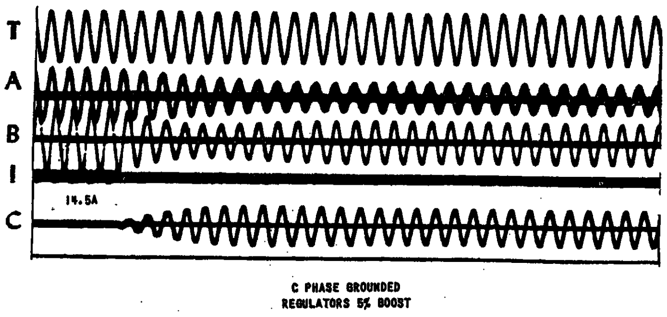


Figure 15. Current in fault and phase-to-ground voltages as a ground is removed from C phase

All regulators on neutral except those on faulted feeder on 5 per cent boost. Neutralizer set on 106 per cent of resonant value

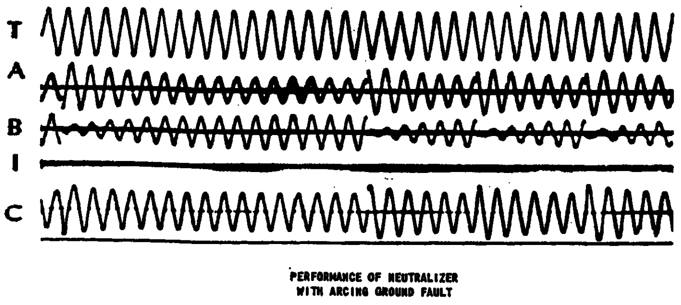


Figure 16. Phase-to-ground voltages with intermittent arcing on B phase of a feeder

All regulators on neutral. Neutralizer set on 97.5 per cent of resonant value

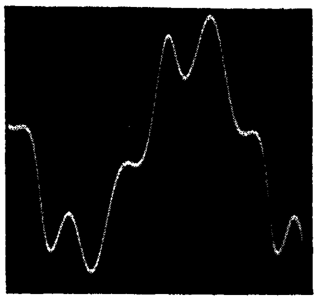


Figure 17. Ground-fault current wave form without a neutralizer

All regulators on neutral except those on faulted feeder on 5 per cent boost. Total current 53.7 amperes. Harmonic analysis: First, 50.7 amperes; third, 3.45 amperes; fifth, 13.5 amperes; seventh, 2.5 amperes

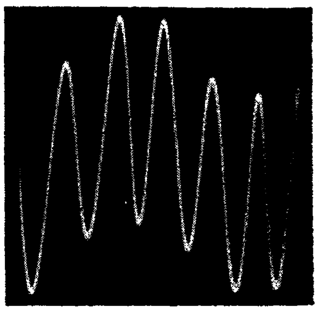


Figure 18. Ground-fault current wave form with a neutralizer set on 97.5 per cent of resonant value

All regulators on neutral except those on faulted feeder on 5 per cent boost. Total current 16.3 amperes. Harmonic analysis: First, 6.8 amperes; third, 0.5 amperes; fifth, 14.7 amperes; seventh, 1.6 amperes

neutralized fault current and hence is proportionately less damaging to the cable system. It alters the characteristics of the recovery voltage in the following manner.

When the system is tuned to resonance and a ground fault is extinguished, the line-to-ground voltage on the grounded phase returns to its normal value in accordance with the oscillogram in Figure 1. In an inverse manner the voltages to ground on the two unfaulted phases return gradually from line-to-line to their normal value. The time delay in restoring these voltages coincides with the decay of the neutralizer current to zero, the rate of which is determined by the time constant of the zero sequence circuit. Conditions are most favorable to arc extinction when this circuit is tuned to the system frequency.

When the neutralizer is not tuned for resonance, the natural frequency of the zero sequence circuit differs from the system, thus giving rise to oscillations in the phase-to-ground voltages as the two frequencies beat. This condition produces nodes in the recovery voltage exceeding the normal phase-to-neutral voltage.

Hence, any out-of-tune condition is less favorable to the return to normal operation.

Test oscillograms, Figures 12, 13, 14, and 15, taken on the system mentioned show the effects of neutralizer tuning and regulators upon the phase-to-ground recovery voltages. They can be illustrated best by polar plots of the locus of the neutral voltage amplitude on the 3-phase voltage vectors, as shown in Figures 8 to 11 inclusive. For any out-of-tune condition, the locus deviates from a straight line and follows a logarithmic spiral to a point near the neutral as determined by the zero sequence voltage of the regulators. When the elapsed time is included in the plots, a convenient comparison is available of the effects of the various tuning values on both the amplitude and rate-of-rise of the recovery voltage.

3. The use of a saturable iron core in the neutralizer reactor will allow for resonant tuning with a restricted displacement of the system neutral during ground free conditions. Saturation would occur at a marginal value over the maximum voltage to which the neutralizer may be subjected, thereby limiting the maximum voltage to ground to approximately twice the normal value. This method will provide the advantages of resonant tuning during ground faults. However, the resulting series resonant current will require equipment of higher current rating than needed for fault neutralization. Ground-indicating equipment will be accurate only at times when the net system regulation is at neutral.

Losses and Harmonics

Complete neutralization of the ground-fault current on a regulated distribution system is not possible because of the loss components of the neutralizer and cable system and because of currents of harmonic frequencies which are present where transformers and induction voltage regulators are overexcited and of various ages. The in-phase component of the unneutralized fault current on a system largely underground may exceed 5 per cent. Fault current wave forms on the system tested were analyzed, and the fifth and seventh harmonics were found to be the most pronounced; without a neutralizer the former being in the order of 25 per cent and the latter 5 per cent of the total fault current. After the neutralizer was installed, various ohmic values of the neutralizer coil had little effect upon harmonic currents, but altered their percentage of the total residual current. In one instance, the fifth harmonic was 90 per cent of the total when the fundamental was 42 per cent.

Typical wave forms of ground-fault current with and without a neutralizer are shown in Figures 17 and 18.

Wave forms of ground-fault currents were taken with various settings of the voltage regulators on the shortest feeder, all other regulators on the system remaining on neutral. The age of these two regulators exceeds 30 years. The effects on harmonics in the fault were not entirely consistent, but, in general, varied with regulator positions.

Other Test Results

At the start of a ground test, an interesting oscillogram, Figure 16, was obtained. An intentional ground was placed on C phase with an oil fuse cutout in series with the grounding lead. A simultaneous ground developed on B phase, causing the fuse to operate. The ensuing recurrent ground on B phase is pictured. The neutralizer, set on 97.5 per cent of resonant tuning, prevented any additional failures which otherwise would have occurred by reason of an uncontrolled arcing ground condition. This type of fault without a neutralizer can subject the system to severe overvoltages.[2]

Operating Experience

Operating experience has been obtained on a distribution system equipped with a neutralizer, as described in the section on Description of System. Voltages to ground have been limited by avoiding resonant tuning. The records of two years' operation well justify the installation of the neutralizer. In the previous $4\frac{1}{2}$-year period there are recorded 20 ground disturbances, one of which caused five simultaneous faults, one caused three simultaneous faults, 11 caused two simultaneous faults, and seven remained as single grounds. Although no data are available to indicate the number of second grounds caused by raising the ground voltages on two unfaulted phases, it is reasonable to assume that a large percentage of the above multiple grounds were as a result of arcing conditions for the reason that periodic grounding tests were made to break down weak spots not capable of withstanding phase-to-phase voltage.

After the neutralizer was installed, a large number of faults were experienced, many caused by activities of postwar construction within the territory served. Of 23 disturbances recorded, two were double grounds and 21 remained single grounds.

Conclusions

The ground-fault neutralizer is a means for improving the reliability of an ungrounded distribution system. When applied to an underground delta-connected system, it provides for a high degree of reliability to service.

Distribution plant equipment influence both the operation and design of the neutralizer. The principal features are as follows:

1. Single-phase feeder voltage regulators give rise to the flow of zero sequence current, which, during normal operation, causes the neutralizer to displace the system neutral.

2. Losses in the cable system and harmonic currents of distribution transformers and regulators prevent complete neutralization of the ground-fault current.

3. In normal operation, a zero sequence current may influence the continuous current rating of the neutralizer.

4. When a ground fault occurs, the maximum boost position of the feeder regulator influences the kilovolt-ampere rating of the neutralizer.

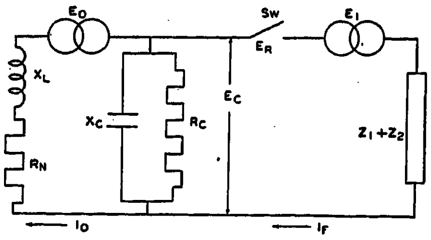


Figure 19. Zero sequence circuit

The displacement of the system neutral during normal operation may be controlled by:

1. Compensating the neutralizer with zero sequence voltage by means of automatically controlled voltage regulators.

2. Avoiding resonant tuning of the neutralizer.

Within limits, this has no serious effects on either the fault neutralization or the recovery voltage. The preferred setting is to an ohmic value less than for resonance to provide for loss of cable capacitance when a protective device operates.

Appendix I

The zero sequence circuit of an unfaulted delta system with one or more regulated feeders and a neutralizer is shown on the left-hand side of Figure 19. The zero sequence current in vector notation is given by the equation

$$I_o=\frac{E_o}{R_N+\frac{R_cX_c^2}{R_c^2+X_c^2}+j\left(X_L-\frac{R_c^2X_c}{R_c^2+X_c^2}\right)} \qquad (1)$$

where

I_o = zero phase sequence current per phase
E_o = zero phase sequence voltage due to regulators
R_N = three times zero sequence resistance
R_c = per phase resistance of phase-to-ground insulation
X_c = per phase capacitative reactance to ground
X_L = three times neutralizer inductive reactance

This series circuit is in resonance when

$$X_L=\frac{R_c^2X_c}{R_c^2+X_c^2} \qquad (2)$$

A ground fault on a feeder is represented by closing the switch in Figure 19, thereby forming a 2-loop network with two voltage sources. The steady-state fault current is

$$I_F=\frac{Z_o}{Z_oZ_A-Z_{A0}^2}(3E_1)-\frac{Z_{A0}}{Z_oZ_A-Z_{A0}^2}(3E_o) \qquad (3)$$

where

$$Z_o=R_N+\frac{R_cX_c^2}{R_c^2+X_c^2}+j\left(X_L-\frac{R_c^2X_c}{R_c^2+X_c^2}\right)$$

$$Z_{Ao}=\frac{R_cX_c^2}{R_c^2+X_c^2}-j\frac{R_c^2X_c}{R_c^2+X_c^2}$$

$Z_A=Z_1+Z_2+Z_{Ao}$
Z_1+Z_2 = sum of positive and negative sequence impedances
E_1 = positive sequence voltage to ground

At resonance Z_o is much less than Z_{Ao} so that the fault current is approximately proportional to E_o.

References

1. American Standard Definitions of Electrical Terms. AIEE, Section 65.11.140, 1941, page 206.

2. Effect of Restriking on Recovery Voltage, C. Concordia, W. F. Skeats. *Electrical Engineering* (*AIEE Transactions*) volume 58, August 1939, pages 371–76.

3. The Electrostatic Unbalance of Transmission Lines and Its Effect on the Application of Petersen Coils, John Alexander Melvin Lyon. *Electrical Engineering* (*AIEE Transactions*) volume 58, March 1939, pages 107–12.

4. Factors Affecting Arc Extinction on a Petersen-Coil System, J. R. Eaton. *Electrical Engineering* (*AIEE Transactions*) volume 58, November 1939, pages 576–77.

5. Electric Circuits (book), Electrical Engineering Staff, Massachusetts Institute of Technology. John Wiley and Sons, Inc., New York, N. Y., 1943.

Discussion

C. B. Hinton (General Electric Company, Schenectady, N. Y.): There are only a few ground-fault neutralizers in use on distribution systems in the United States and consequently there have not been very many papers written on the subject. The installation described in this paper is the first on a regulated distribution system and so it opens up an entirely new field for the application of these protective devices. The authors are to be congratulated for the initiative shown in making the application and describing the problems which they had to overcome.

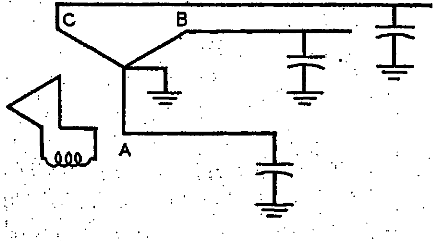


Figure 1. System without a fault

My discussion is concerned with the automatic voltage-limiting action which can be built into a ground-fault neutralizer application and which experience has shown to be a desirable feature. This particular subject was not mentioned in the paper.

The first neutralizer in this country was installed on the system of the Alabama Power Company. It was an air-core type coil in the neutral of the system and produced a good operating record. Some switching operations, however, caused a momentary single-phasing condition with the result that sizeable over-voltages occurred resulting in insulator flashover. Since that time General Electric neutralizers have been built with a steel core designed to

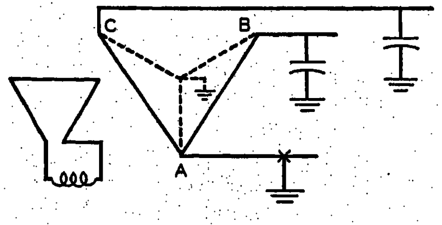


Figure 2. System with a single line-to-ground fault

saturate on the occurrence of voltages higher than normal, thereby detuning the system and effectively preventing excessive overvoltages.

The same automatic protection can be obtained in the Bauch-type neutralizer, which is the type described in the paper, by proper design of the grounding bank. The mechanism for throwing the system out of tune differs from that which acts in a conventional neutralizer but it is just as effective and is shown in Figure 1 of this discussion. Normally the voltage across each leg of the grounding bank is of line-to-neutral magnitude. When a single-line-to-ground fault occurs, the voltages on the two unfaulted legs approach line-to-line magnitude, Figure 2 of discussion. This increased voltage results in a slight change in the magnetizing current drawn by the grounding bank. The magnetizing component of current has the same phase relationship as the current supplied by the neutralizer, and therefore the two components add directly to produce the total reactive current in the fault, Figure 3 of discussion. If the voltage across the grounding bank tends to increase still more for any reason, the magnetizing current increases very rapidly, so that the total reactive component in the fault becomes relatively large, see Figure 4 of this discussion, and the system is detuned. Thus any increase in voltage at the terminals of the grounding bank causes an automatic voltage-limiting action to take place.

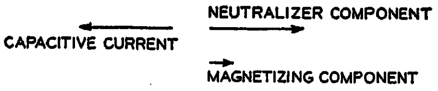


Figure 3. Components of current which flow in a single line-to-ground fault

If the grounding bank is designed judiciously, the increase in magnetizing current can be held to a low value for a normal voltage increase of 73 per cent during a single-line-to-ground fault and can be forced to a relatively high value when the voltage appreciably exceeds this amount. This can be accomplished by taking advantage of the familiar nonlinear saturation characteristic of core steel.

The grounding bank described in the paper consists of three standard distribution transformers rated 2,400-240 volts. The voltage on each leg of the bank is approximately 1,380 volts with no fault on the system. When a single line-to-ground fault occurs, the voltage on the two unfaulted legs increases to approximately 2,400 volts, resulting in a slightly higher magnetizing current. Any further increase in voltage would cause the valve action described previously to begin to operate. The maximum value the voltage would reach before it was effectively limited, however, is a function of the saturation characteristic of the grounding bank. The protection offered by this grounding bank made up of 2,400-volt transformers is not as good as that which would be provided by a bank designed to operate at approximately line-to-neutral voltage because a relatively higher voltage is required to initiate the limiting action.

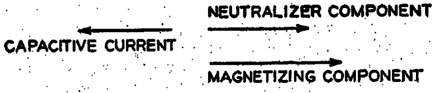


Figure 4. Components of current which flow when voltage at terminals of grounding transformer is excessive

The paper mentions that the preferred setting of the neutralizer is to an ohmic value less than that required for resonance to provide for loss of cable capacitance when a protective device operates. One additional reason for detuning on the low reactance side of resonance is that the sustained voltages on the unfaulted phases during a single line-to-ground fault are less than they are if detuning is on the high reactance side of resonance. Still another reason is that the arc at the point of fault is easier to extinguish if the net fault current is lagging rather than leading.

Eric T. B. Gross (Illinois Institute of Technology, Chicago, Ill.): This paper is of great value particular to the operating engineer, and for many reasons:

1. It indicates clearly that resonant grounding deserves much wider application in distribution systems[1] than this grounding method has had in the past.

2. It is emphasized that self-extinguishing of arcing grounds is accomplished by the prevention of restrikes, and that it is not necessary to keep the ground-fault neutralizer at resonant tuning. Previous discussions, published in the Transactions during the past 20 and more years, indicate that many engineers were under the impression it was of prime importance to keep the tuning always at exact resonance; they thought it was necessary to keep the fault current small, whereas only the out-of-tune ratio[2] is responsible for the rate of recovery voltage, and not the magnitude of the residual fault current as such.

3. It is pointed out that detuning in both directions, by ±15 per cent in this case from 85 to 115 per cent, has no detrimental effects on the self-extinguishing of arcing grounds; this statement is in agreement with experiences made in distribution and transmission system on numerous[3] coils at voltages up to 220 kv.

4. It is underlined that a complete analysis of tests[4] requires the resolution of the residual fault current into its harmonic components.

I am certain that the authors recognize the advantages of saturation of the ground-fault neutralizer at approximately phase voltage, and agree with me that such an iron-core coil is preferable to an air reactor. A properly designed ground-fault neutralizer will start to deviate from the linear characteristic on all its taps at or near phase voltage. However, if it does not, an air reactor will hardly do worse.

The equivalent diagram, Figure 19, is very helpful in studying ground-fault phenomena in resonant grounded systems; it may be worthwhile mentioning that a possible fault resistance would be added to Z_1+Z_2 and that E_0 (the neutral displacement of the system when $R_N+jx_L=\infty$) is really an equivalent electromotive force in the R_c and x_c branch and in series with it.[5] The particular type of ground-fault neutralizer described here is often referred to as Bauch Transformer[6] or indirect method of resonant neutral grounding, whereas the better known single-phase Petersen coil is the direct method of resonant neutral grounding.

It is hoped that this paper will lead many engineers to recognize resonant neutral grounding as a grounding method that provides one of the cheapest solutions of the problem by covering a high percentage of faults without voltage dips.

REFERENCES

1. RESONANT GROUNDING OF DISTRIBUTION SYSTEMS, E. Herzog. *Proceedings*, Midwest Power Conference (Chicago, Ill.), volume 11, 1949, pages 171–78.

2. THE ARC QUENCHING EFFECT OF PETERSEN COILS, W. Diesendorf. *Journal*, Institution of Engineers (Sydney, Australia), volume 18, 1946, pages 237–40.

3. THE WHY AND HOW OF RESONANT GROUNDING, Eric T. B. Gross. *Electric Light and Power* (Chicago, Ill.), volume 25, July and August 1947.

4. *Discussion* by Eric T. B. Gross of ENGINEERING FEATURES OF THE 230-KV BOULDER-CHINO TRANSMISSION LINE OF THE SOUTHERN CALIFORNIA EDISON CO., LTD., Alex A. Kroneberg, E. M. Hunter. *AIEE Transactions*, volume 59, 1940, page 1176.

5. ADJUSTMENT OF GROUND FAULT COILS, E. Gross. *Bulletin*, Swiss Institute of Electrical Engineers, volume 28, 1937, pages 165–69.

6. BAUCH TRANSFORMER DAMPS CABLE FAULTS, L. M. Nelson, H. N. Muller. *Electrical World* (New York, N. Y.), volume 122, 1944, page 86.

H. R. Tomlinson and F. B. Hunt: Mr. Hinton has called attention to the preference for saturating the grounding transformers rather than the neutralizer reactor to control excessive voltages by the nonlinear method. The authors concur in view of the fact that the system can be thrown out-of-tune at a much lower voltage than with the saturable reactor method. However, in the application to the lower voltage regulated distribution systems, it is questionable if this especially designed equipment has any merit over the use of standard transformers with an air-core reactor set to 90 per cent of the resonant value. If the special grounding transformers can be made to saturate effectively at a voltage moderately higher than phase-to-phase voltage, the maximum ground voltage to a regulated phase during fault-free conditions is in the same order as obtained when a linear device is set to the 90 per cent value. On the system described in the paper, saturation at approximately 2,500 volts will result in a ground voltage to the furthest regulated phase of approximately 2,700 volts, or 195 per cent of the system rated ground voltage. This is slightly higher than that shown for the full boost condition in Figure 7, resulting by the use of a 90 per cent air-core reactor.

The authors believe that the resonantly tuned condition obtainable by the nonlinear design when the system becomes faulted has no practicable advantage, as experience and tests indicate that neutralizer objectives are attained when the reactance is 90 per cent, and even lower, than the resonant value.

Inspection of oscillograms and recovery voltage curves included in the paper indicate that a residual capacitive current at the fault is no more difficult to extinguish than an equal reactive current.

In answer to the discussion by Professor Gross, the authors do not believe that the use of a tuned saturable iron-core reactor will limit the ground voltages as effectively as an out-of-tune air-core reactor. For equal reactance values, some cost reduction may be realized with iron cores by savings in space and materials.

Additional units similar to the one described in the paper will be installed at other locations within the territory served by the company with which the authors are associated. These will afford an opportunity to gain further experience with this type of equipment. These units will be installed primarily to avoid the disturbances caused by arcing grounds on cable systems. However, it is recognized that additional benefits are realized on the overhead portions of the distribution systems. In particular, the neutralizer will extinguish ground-fault currents caused by lightning flashovers between primary and secondary bushings of a pole top transformer which otherwise are sustained by the flow of capacitive current if the magnitude of such is insufficient to cause operation of the transformer primary fuses. These sustained ground currents have caused damage to transformers and customers' equipment.

Report on Survey of Unbalanced Charging Currents on Transmission Lines as Affecting Ground-Fault Neutralizers

AN AIEE COMMITTEE REPORT

DURING the preparation of the standards material for ground-fault neutralizers, which forms a part of AIEE Standard Number 32 for Neutral Gounding Devices,[1] there was considerable discussion in reference to the requirement that all ground-fault neutralizers be capable of carrying continuously a circulating current equal to 30 per cent of the current rating. Some expressed the view that this value might be too low, and some thought it might be too high. The value chosen was assumed to represent a balance among such factors as: the investment cost of the neutralizer; the fact that tests are made on these devices when first installed and 30 per cent current may be possible; the possible cost of transposing or interchanging line conductors; and in some cases detuning is not an acceptable solution. The investment cost of the neutralizer can be estimated closely, but the possible cost of transposing is rather difficult to determine.

In view of the question as to whether the requirement of 30 per cent continuous circulating current capability for ground-fault neutralizers was correct, it was decided to conduct a survey to determine this point.

From data submitted by the manufacturers on neutralizers built to the 30 per cent requirement, it appears that in about 15 per cent of the applications, further transpositions of the circuits were necessary to reduce circulating current to a value below 30 per cent. However, after the transpositions were effected, only about half of the systems which were transposed to meet the 30 per cent requirement then had less than 15 per cent circulating current. This would have necessitated further transpositions for some systems if the neutralizers had had a 15 per cent requirement. Furthermore, the submitted data indicate that if the neutralizers had had a 15 per cent continuously circulating current rating, then in 43 per cent of the applications it would have been found necessary to transpose. This percentage compares to the 15 per cent that were required to transpose with the present 30 per cent requirement.

On the basis of these facts, an investigation of a 15 per cent ground-fault neutralizer was made. It was confirmed that to carry 30 per cent circulating current continuously requires a 10-minute time rated coil (standards had intended to include this correlation), and that the 15 per cent neutralizer resulted in a 1-minute rating. For a 13,800-volt application the 15 per cent rated neutralizer equipment would sell for 97 per cent of the price of the 30 per cent unit, a saving of $210. At 34.5, 46, and 69 kv, the 15 per cent rated neutralizer equipment would sell for approximately 94 per cent of the price of the 30 per cent unit, a saving of $500, $550, and $600 respectively.

There is some indication that the tendency to omit transpositions, except where telephone co-ordination requires them, is increasing. If this is the case, unbalanced charging current will be more prevalent in the future than in the past. There is a strong probability that the over-all cost to the industry would be greater with a 15 per cent neutralizer than with the present standard 30 per cent neutralizer.

It has been found that a system may have acceptably balanced charging current as a whole but be badly unbalanced when sections are disconnected. This will be a more critical condition for the 15 per cent neutralizer than for the 30 per cent unit and would tend to increase the percentage of installations for which additional transpositions would be required.

When the standards were being written, there was considerable discussion as to whether a 1-minute rating or a 10-minute rating for the neutralizer would be better. There was a decided tendency to prefer the 10-minute rating because it was thought that one minute was too short a time from operating considerations. The present 30 per cent neutralizer correlates with a 10-minute time rating, while the 15 per cent neutralizer correlates with a 1-minute time rating. This feature is a definite handicap for the 15 per cent neutralizer.

Summary

The committee finds that the requirement that neutralizers be capable of carrying a continuous circulating current not less than 30 per cent of its rating appears to be sound in view of the foregoing conclusions, and therefore recommends that no change be made.

Reference

1. STANDARD FOR NEUTRAL GROUNDING DEVICES. *AIEE Standard Number 32*, May 1947.

Paper 49-284, recommended by the AIEE Committee on Protective Devices and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted August 31, 1949; made available for printing September 2, 1949.

This report was prepared by a Working Group of the AIEE Subcommittee on Fault Limiting Devices of the AIEE Committee on Protective Devices, under the sponsorship of W. D. Hardaway.

Personnel of the Working Group of the AIEE Subcommittee on Fault Limiting Devices: J. E. Clem, *Chairman;* H. H. Brown, S. S. Cook, N. E. Dillow, W. D. Hardaway, R. E. Neidig, P. B. Speer.

Discussion

Eric T. B. Gross (Illinois Institute of Technology, Chicago, Ill.): It seems worthwhile mentioning that "transposition," as used in this report, does not mean either a complete barrel of transpositions for each line section or "the interchange of the position of the several conductors of a circuit," as defined in ASA C42-1941[1] under 35.60.330. This report refers to electrostatic unbalances with respect to ground; it is desirable to equalize the capacitances-to-ground of all 3-phase conductors, irrespective of remaining unbalances of interphase capacitances. The transposition can be accomplished often by an interchange of two conductors only, and transposition towers are rarely necessary. In one of the severest cases that was considered in this country, altogether 28 transpositions were made; 26 of these were interchanges that could be made in substations and only two required transposition structures on overhead line towers: the system referred to comprised more than 200 miles of overhead line right of way. These figures indicate that it is not difficult nor expensive to balance an overhead line electrostatically with respect to earth.

I am in full agreement with the recommendation to design resonant neutral grounding devices, as defined in AIEE Standard No. 32[2] for not less than 30 per cent continuous circulating current. Some of the coils that have been in operation in this country for some time were designed rather conservatively and the operating companies have found out, through experiences with faults that were permitted to stay on for more than ten minutes, that the equipment may be used without overheating on permanent faults with full load, even if designed for "10-minute rating".

A recent survey concerning the application of resonant neutral grounding to systems at voltages up to 110 kv seems to indicate that there is a tendency towards preference of equipment that is designed for extended time rating. This tendency is in line with the established practices in foreign countries[3] to operate with permanent faults until the defective line has been located[4] with the help of sensitive ground relays, and may be switched off without voltage dips or service interruptions.

References

1. American Standard Definitions of Electrical Terms, ASA C42. AIEE, 1941.

2. Standard for Neutral Grounding Devices. *AIEE Standard Number 32*, May 1947.

3. The Why and How of Resonant Neutral Grounding, Eric T. B. Gross. *Electric Light and Power* (Chicago, Ill.), volume 25, July/August, 1947.

4. Sensitive Ground Protection for Transmission Lines and Distribution Feeders, Eric T. B. Gross. *Electrical Engineering* (*AIEE Transactions*), volume 60, November 1941, pages 968–71.

Governors and the AIEE-ASME Governor Performance Specification

A. F. SCHWENDNER
NONMEMBER AIEE

WAYNE C. ASTLEY
NONMEMBER AIEE

THE Joint AIEE-ASME Committee on a Recommended Specification for Prime Mover Speed Governing recently issued a recommended specification[1] for steam turbines intended to drive electric generators rated 500 kw and up. A similar specification for hydraulic turbines intended to drive electric generators has been submitted to the AIEE Standards Committee for approval. These specifications include definitions and descriptions of terms relating to governor functions, and standards of performance.

The purpose of this paper is to show the application of the terms and definitions to modern speed-governing systems; and indicate how these systems meet the requirements of the specifications.

Steam-Turbine Speed-Governing Systems

Two types of speed-governing systems are illustrated in Figures 1 and 2 to indicate how the component parts are defined in the specification. The system shown in Figure 1 does not represent any particular make, but contains features common or comparable to most of the flyball governing systems. The system shown in Figure 2 is an oil governing system which is illustrated more in detail in Figure 3. In Tables I and II the components which are labeled in the figures are grouped under the proper terms as described in the specifications.

Table I. Flyball Type of Speed-Governing System

Term	Components	Description
Speed governor	1	Flyball element responsive to speed
Speed-control mechanism	2_1	Force amplifier
	2_2	Force amplifier
	2_3	Valve servomotor
Governor-controlled valves	3	Steam inlet valve
Speed changer	4	
Load-limiting device	5	

Table II. Hydraulic Type of Speed-Governing System

Term	Components	Description
Speed governor	1	Impeller responsive to speed
Speed control mechanism	2_1	Pressure amplifier
	2_2	Force amplifier
	2_3	Force amplifier
	2_4	Valve servomotor
Governor-controlled valves	3	Steam inlet valve
Speed changer	4	
Load-limiting device	5	

The component parts of an electronic-type governor can be similarly grouped, as may elements of other systems in use which perform within the limits of the specification.

Having established the definitions and the grouping of the components of governor systems, it is intended to show how the system functions to meet the specifications. Practically all of the performance specifications deal with governor characteristics which involve every component of the system. For that reason operation of the system as a whole will be described as necessary under each specification.

Performance Specifications

STEADY-STATE SPEED REGULATION

Steady-state speed regulation is defined as the change in sustained speed expressed in per cent of rated speed when the power output of the turbine is gradually reduced from rated power output to zero power output with identical settings of all adjustments of the speed-governing system.

From the definition it is obvious that we must start with the governor-controlled valve lift to check the performance of the governors shown in Figures 1 and 2. Under rated speed and steam conditions the valve motion from rated load to no load will be a definite value. In the hydraulic type of governor shown in Figure 2 this definite motion can be obtained only by a fixed proportional change in the control pressure acting on the power amplifying device, 2_2. This control pressure change is obtained by a change in the governor pressure in response to a speed change of the turbine. The governor pressure in turn acts on the bellows of the pressure amplifying device, 2_1, and the ratio of amplification is fixed by the bellows and cup areas.

When an increase in control pressure acts on the area of the power amplifying piston, 2_2, the extension spring attached to the return link is stretched. The piston moves down a fixed amount which is dependent on the pressure change, the piston area, and the scale of the extension spring. The lower end of the piston extends into the pressure chamber of the power amplifying device, 2_3. The extension of the piston is made in the shape of a cup valve which covers a drain hole extending through the length of the servomotor relay. This relay is held against the extension of the piston by a strong compression spring.

High-pressure oil is admitted into the pressure chamber through an orifice. As the oil pressure in the chamber increases, it acts on the exposed area of the relay and overcomes the upward force of

---

Paper 49-288, recommended by the AIEE Power Generation Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17-21, 1949. Manuscript submitted October 29, 1948; made available for printing September 12, 1949.

A. F. SCHWENDNER is with the Westinghouse Electric Corporation, Philadelphia, Pa., and WAYNE C. ASTLEY is with the Philadelphia Electric Company, Philadelphia, Pa.

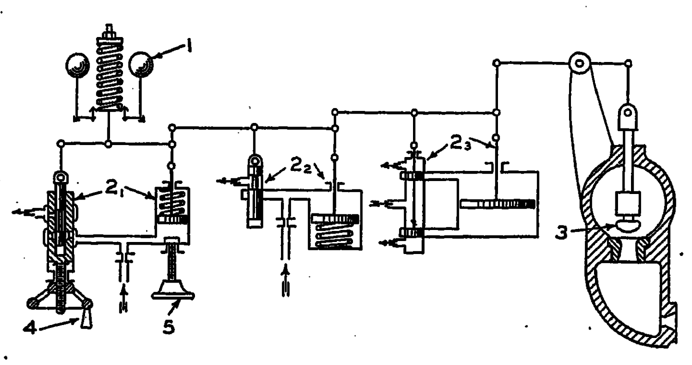


Figure 1. Flyball type of speed governing system

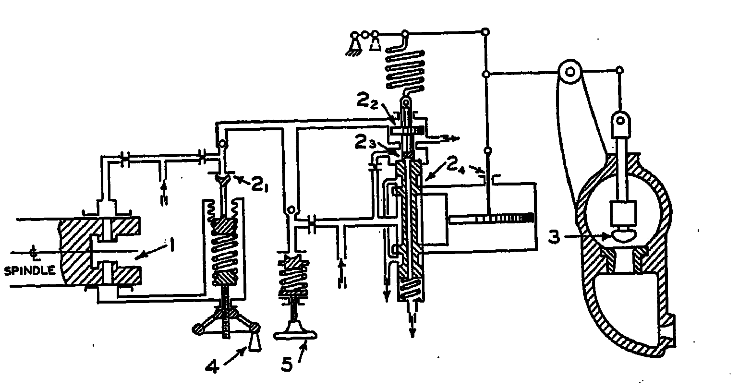


Figure 2. Hydraulic type of speed governing system

the compression spring. The relay is pushed down by oil pressure until the exposed drain area permits the pressure in the chamber to drop to a value where the spring force is balanced by the pressure acting on the area of the relay.

The motion of the relay has admitted high-pressure oil into the servomotor cylinder, 2_4, and moved the servomotor piston up. This motion of the servomotor piston continues until the return linkage, by means of the extended spring, moves the power amplifier piston and relay back to the neutral position. The travel of the servomotor piston depends on the change in control oil pressure. With a given ratio for the return linkage lever, it will require a given oil pressure change (and consequently a given speed change) to move the governor-controlled valve from the rated load-rated speed position to the no-load position.

A change in regulation is obtained by modifying the ratio of the return linkage lever, which controls the variation of spring extension for a given valve travel. The change in the extension of the spring changes the control pressure required to move the relay to the neutral position. The change in control pressure is obtained, of course, by a corresponding change in speed and impeller oil discharge pressure.

The procedure followed in tracing the operating of the hydraulic governor will be used in describing the flyball governing system, which has the same characteristics, Figure 1. The movement of the governor controlled valve from rated load to no load can be obtained only as a result of a proportional movement of the governor weight. This movement of the governor weights, due to a change in speed, moves the relay and operating mechanism of the force amplifier, 2_1. The operating piston of 2_1 moves the relay of the second force amplifier, 2_2, which in turn moves the relay valve of the governing valve servomotor. The ratio of the linkage between the governor-controlled valve and the governor establishes the governor speed regulation.

A change in speed regulation can be obtained by changing the lever ratio of any one of the force amplifying devices, as this changes the governor travel required to produce a given change in valve travel. Similarly, the spring scale of the governor could be changed, as this would produce the same effect without any change in the linkage. Both of these control systems require that the unit be stopped in order to change the regulation.

STEADY-STATE INCREMENTAL SPEED REGULATION

The steady-state incremental speed regulation at a given steady-state speed and power output is the rate of change of the steady-state speed with respect to the power output. Because of differences in steam flow at various governing valve openings, and the uneven characteristics of some governors' speed versus travel relationship, the incremental regulation can be different for each portion of the governing valve travel. In the two governing systems shown, the governors' response to a speed change will be identical at any load point when operating at rated speed. There is no change in the flyball position in Figure 1 regardless of valve position as long as rated speed is maintained. If, however, the speed is permitted to change, the uneven governor characteristics must be considered. The governing system shown in Figure 2 does not change its characteristics with changes in speed and/or load.

For the two governors described, the incremental regulation is dependent mostly on the governor-controlled valve flow versus travel characteristics and the valves have to be so designed that the resultant incremental regulation remains within the specified limits.

DEAD BAND

Dead band is the total magnitude of the sustained speed change within which there is no resulting measurable change in the position of the governor controlled valves. For turbines in excess of 5,000 kw, the specification states that the dead band shall not be greater than 0.06 per cent. This means that the motion of the governor-controlled valves must be reversed with a speed change not greater than 0.06 per cent of rated speed. The following analysis indicates the methods to be employed to bring the dead band of the governors illustrated within the required limits.

Considering the governor in Figure 2, it is evident that the governor-controlled valve is so attached to the operating piston of the servomotor, that all loads are taken in one direction and all dead travel between them is eliminated. That is not the case, however, in the relationship between the operating piston and the control pressure applied to the piston of the force amplifying device, 2_2. There is a slight dead travel in the relay, and the piston of the amplifying device is not completely frictionless. The control pressure change acting on the piston, 2_2, necessary to reverse the motion of the operating piston of the servomotor may be determined. Similarly, the oil pressure change, equivalent to a 0.06 per cent change in speed, which acts on the bellows of the pressure amplifier, 2_1, may be determined. To obtain the required dead band, it is then necessary to select a cup valve with an area such that the necessary control pressure change is obtained, as a result of the pressure change on the bellows. As an alternative, the area of the bellows could be changed.

Examination of the governor in Figure 1 indicates that again all dead travel is eliminated between the operating piston of the servomotor and the governor controlled valve. The lever moving the servomotor relay will have a small change in travel, however, before the operating piston will change its direction. The amount of travel is somewhat increased through the action of the force amplifier,

2_2. Therefore, to obtain the required dead band, the governor will have to be designed to travel a sufficient amount with 0.06 per cent change in speed so that the resulting motion of the force amplifier 2_1 will be equal to or greater than the motion required by 2_2.

STABILITY AND OVERSPEED

The requirement for stability and overspeed can be obtained only by considering the turbine characteristics as a whole and by building into the governor system the specified rate of response. In most cases, this means the selection of relay areas sufficient to pass the required quantities of oil with small displacement, oil pumps with sufficient capacity, and so forth. Each type and size of unit must be investigated as a governing system; that which is satisfactory for one design may not give identical performance on another design.

RANGE OF SPEED CHANGER ADJUSTMENT

In the hydraulic-type governor, Figure 2, the range of the speed changer is determined by the extent of travel of the speed changer 4 and the scale of the spring. The product of the two determines the force restraining the bellows against the governor pressure. The ratio of the bellows area to the cup valve area establishes the relationship between a change in governor pressure and the resultant change in control pressure. The force on the bellows, due to the tension of the speed changer spring, determines at what speed (governor pressure) the resultant control pressure will be equivalent to the no-load position or full-load position of the governor controlled valve. To change the range of the speed changer, consequently, the spring scale must be changed.

The flyball governing system shown in Figure 1 is equipped with a speed changer 4, whose range is determined by the extent of the travel of the relay sleeve, 2_1. The governor travel and linkage relationships must be established, so that the governor is capable of closing the governor controlled valves, when maximum load is dumped, even though the speed changer is in a position to carry maximum load at 102 per cent of rated speed. The speed changer also must have sufficient travel, so that at no load the speed can be carried 5 per cent below rated speed. To increase the range of the speed changer, it is necessary that the governor be designed for the increased travel necessary to cover this range.

ADJUSTMENT OF SPEED REGULATION WITH TURBINE IN OPERATION

A device to change the steady-state speed regulation with the turbine in operation is optional in the specification. This is accomplished by moving the fulcrum point of the follow-up linkage in either of the governing systems illustrated.

LOAD LIMITING DEVICE

The connection of a load limiting device to a hydraulic turbine governor is shown in Figure 2. It consists of a spring loaded cup valve supplied with high-pressure oil through an orifice. The compression of the spring and, consequently, the resultant oil pressure leaving the device is adjustable as indicated at 5. The oil pressure controlled by the load limit device is led into the top chamber of the force amplifying device, 2_2 in Figure 2, through a check valve. The control pressure from 2_1 is introduced into the same chamber through a second check valve. With this arrangement an increase in pressure in either line will close the check valve in the other line and meanwhile act to close the governing valve. A pressure, equivalent to any load, maintained on the load limit device will prevent the governor-controlled valve from opening further than the given load position.

During any period that the governor control pressure is higher, it will have full control over the governor-controlled valves, but as soon as the control pressure drops below the given load point, the pressure maintained by the load limit will open its check valve and close the check valve leading to the pressure amplifying device and hold the governor-controlled valve in one position. In the event that load is dropped from the system, the governor control pressure rises and assumes control of the governor-controlled valve.

A similar device can be used to limit the load on the flyball type of governor. This is accomplished by introducing an adjustable screw, 5, below the operating piston of the force amplifier 2_1 in Figure 1. The upper end of this screw prevents the piston from moving below the point for which it is set, and the governor will have full control over the governing valves above the setting of the load limit.

Care must be taken in the design of this system to allow sufficient over-travel between the relay of 2_1 and the sleeve adjusted by the speed changer. Otherwise, free travel of the governor for any position of the speed changer or load limit would not be permitted and excessive loads might be placed on the knife edges of the governor.

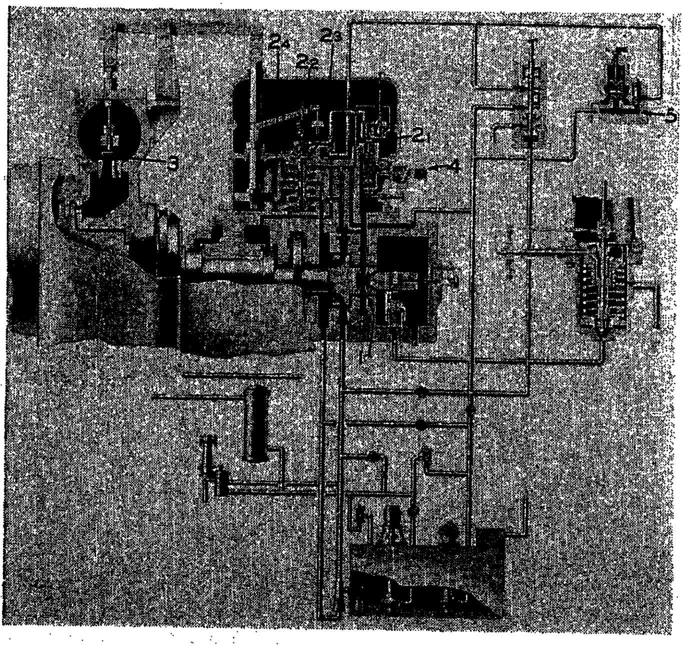


Figure 3. Steam turbine with a hydraulic type of speed governing system

Summary

It is evident that either of the two conventional governing systems shown can satisfy the conditions laid down in the specification. Similar analysis will indicate how other systems of less conventional design also meet the specification. It has been the intention of the authors to indicate the steps in these analyses and show how modern systems can be adjusted to operate within the limits of the specification.

Reference

1. RECOMMENDED SPECIFICATION FOR SPEED-GOVERNING OF STEAM TURBINES INTENDED TO DRIVE ELECTRIC GENERATORS RATED 500 KW AND UP. *AIEE Standard Number 600*, May 1949.

No Discussion

Performance Characteristics of Lightning Protective Devices

AN AIEE COMMITTEE REPORT

THIS report presents data covering the performance characteristics of lightning protective devices of present-day manufacture. It combines in one report the data on lightning arresters of various types published previously by the AIEE Lightning Protective Devices Subcommittee in a number of separate reports,[1–4] with such modifications as required to bring this material up to date. In addition, data covering line expulsion-type lightning arresters, and data on rod gap spark-over voltages, for co-ordination with lightning arrester characteristics, are included.

The data presented are the results of tests made in accordance with methods of testing covered in the proposed combined AIEE Lightning Arrester Standards for Valve and Expulsion Lightning Arresters, under preparation by the Lightning Protective Devices Subcommittee. The data in this report were compiled from information supplied by the lightning arrester manufacturers. The values are for lightning arresters of present-day manufacture and do not necessarily apply for older types.

The data are presented in both tabular and graphical form. Data for valve-type lightning arresters in the distribution, line, and station classifications are shown in Table I and Figures 1 to 16, inclusive, and for expulsion-type lightning arresters in the distribution classification in Table II and Figures 17 and 18. The tables give the minimum, average, and maximum spark-over values for the lightning arrester, using the rate of voltage rise specified in the proposed AIEE Standards for Lightning Arresters; that is, 100 kv per microsecond per 12 kv of lightning arrester rating, with an impulse of the polarity that gives the higher spark-over values. This information is shown graphically in Figures 1, 5, 9, 13, and 17.

The complete impulse spark-over characteristics of valve and distribution expulsion-type lightning arresters are shown in Figures 2, 6, 10, 14, and 18. Average spark-over voltages for the front-of-wave test are indicated for each lightning arrester rating.

Table I, for valve-type lightning arresters, also includes values of discharge voltages that appear across the lightning

Table I. Performance Characteristics of Valve-Type Lightning Arresters

Lightning Arrester Type and Rated Voltage, Kv	Front of Wave Impulse Spark-over: Rate of Rise,* Kv Per Microsecond	Front of Wave Impulse Spark-over: Kv** Min	Kv** Avg	Kv** Max	Discharge Voltage-Kv on 10 by 20 Microsecond Current Wave**: 5,000 Amperes Min	5,000 Amperes Avg	5,000 Amperes Max	10,000 Amperes Min	10,000 Amperes Avg	10,000 Amperes Max	20,000 Amperes Min	20,000 Amperes Avg	20,000 Amperes Max
Distribution													
3	25	12	18	23	9	14	17	10	16	20	12	18	23
6	50	22	34	45	16	26	34	19	30	38	22	34	44
9	75	32	48	62	24	39	51	28	44	57	33	51	66
12	100	42	61	77	32	49	62	37	55	69	43	62	78
15	125	52	73	91	39	61	77	44	69	87	52	79	99
Line													
20	167	56	75	94	66	83	100	73	92	110	81	101	121
25	208	70	93	116	81	101	121	89	111	133	97	121	145
30	250	82	110	138	97	121	145	108	135	162	119	149	179
37	308	102	136	170	119	149	179	131	164	197	145	181	217
40	333	110	147	181	129	161	193	140	177	213	157	196	235
50	417	137	183	229	161	202	242	177	222	265	194	243	292
60	500	165	220	274	193	242	290	217	271	325	238	298	358
73	608	200	267	333	237	297	355	261	328	392	288	360	432
Station													
3	25	10	13	16	9	10	11	9	11	13	10	12	14
6	50	18	23	28	17	20	23	19	22	25	20	24	28
9	75	28	35	42	26	30	34	28	33	38	30	35	40
12	100	34	43	52	34	40	46	37	44	51	40	47	54
15	125	42	53	64	43	50	57	46	54	62	50	59	68
20	167	58	72	86	57	67	77	61	72	83	66	78	90
25	208	71	89	107	71	83	95	77	90	103	85	100	115
30	250	85	106	127	85	100	115	92	108	124	100	118	136
37	308	105	131	157	105	124	143	112	132	152	123	145	167
40	333	109	136	163	114	134	154	122	144	166	130	153	176
50	417	143	178	213	142	167	192	152	179	206	163	191	219
60	500	171	214	257	170	200	230	185	217	249	199	234	269
73	608	209	261	313	208	245	282	223	262	301	241	283	325
97	808	276	345	414	275	323	371	297	349	401	321	377	433
109	908	310	388	465	309	363	417	335	394	453	361	424	487
121	1,008	344	430	516	343	403	463	372	438	504	400	470	540
145	1,208	412	515	618	414	487	560	445	523	601	480	564	648
169	1,408	482	602	722	481	566	651	519	610	701	559	658	757
196	1,633	553	691	829	550	647	744	593	698	803	642	755	868
242	2,017	688	860	1,032	685	806	927	741	872	1,003	799	940	1,081

* 100-kv per microsecond per 12-kv of lightning arrester rating.

** Impulse of polarity giving higher spark-over voltage.

Paper 49-286, recommended by the AIEE Protective Devices Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted August 2, 1949; made available for printing September 2, 1949.

Personnel of AIEE Lightning Protective Devices Subcommittee: H. R. Stewart, *Chairman*, H. A. Cornelius, F. M. Defandorf, R. H. Earle, M. Getting, I. W. Gross, E. H. Grosser, H. W. Hartzell, T. H. Mawson, T. I. Eldridge, W. A. McMorris, A. M. Opsahl, W. E. Rushlow, A. H. Schirmer, J. M. Towner, E. R. Whitehead, E. H. Yonkers.

Working group membership includes R. H. Earle, T. I. Eldridge, W. A. McMorris, A. M. Opsahl, under chairmanship of E. H. Grosser.

arrester following spark-over and during passage of discharge current. Test values are given for 10 by 20 microsecond current waves of 5,000, 10,000, and 20,000 amperes crest, of the polarity giving the higher voltage values. Figures 3, 7, 11, and 15 show graphically the minimum, average, and maximum discharge voltages at 5,000 amperes for distribution and line lightning arresters, and at 10,000 amperes for station lightning arresters. Average discharge voltages at 5,000, 10,000, and 20,000 amperes are given in Figures 4, 8, 12, and 16.

Data on the critical impulse spark-over voltages of line expulsion-type lightning arresters are shown in Table III and Figure 19. For shorter times to sparkover, the different laboratories have been unable to agree on comparable spark-over values.

Data are available, however, showing the co-ordination to be expected between these lightning arresters and line insulation. These data are included in Table III.

Table II. Performance Characteristics of Distribution Expulsion-Type Lightning Arresters

Rated Voltage of Lightning Arrester, Kv	Front of Wave Impulse Spark-over: Rate of Rise,* Kv Per Microsecond	Kv** Min	Kv** Avg	Kv** Max	Average Critical Impulse Spark-over 1.5 by 40 Microsecond Wave, Kv**
3	25	23	33	45	29
6	50	32	50	70	41
9	75	48	71	97	53
12	100	63	84	94	61
15	125	77	101	114	70

* 100-kv per microsecond per 12-kv of lightning arrester rating.

** Impulse of polarity giving higher spark-over voltage.

Table III. Performance Characteristics of Line Expulsion-Type Lightning Arresters

Rated Circuit Voltage,* Kv	Rated Voltage of Lightning Arrester, Kv	Average Critical Impulse Spark-over 1.5 by 40 Microsecond Wave Kv Positive	Average Critical Impulse Spark-over 1.5 by 40 Microsecond Wave Kv Negative	Number of 10-Inch Diameter-5³/₄-Inch Spacing Insulator Disks Protected
13.8	8.4	100	110	1
23	14	150	155	2
34.5	21	200	220	2
46	28	260	285	3
69	42	375	420	5
92	56	475	540	6
115	70	575	635	7
138	84	680	760	9

* Usual circuit voltage for grounded neutral systems.

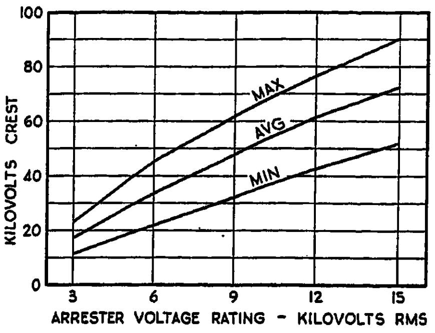


Figure 1. Front-of-wave impulse spark-over* of distribution valve-type lightning arresters

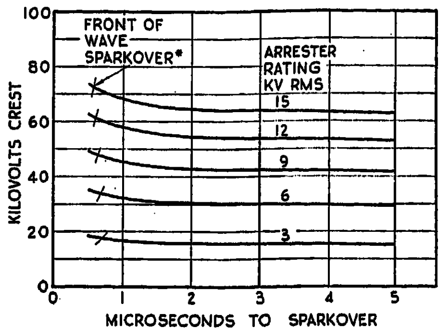


Figure 2. Average impulse spark-over volt-time characteristic of distribution valve-type lightning arresters

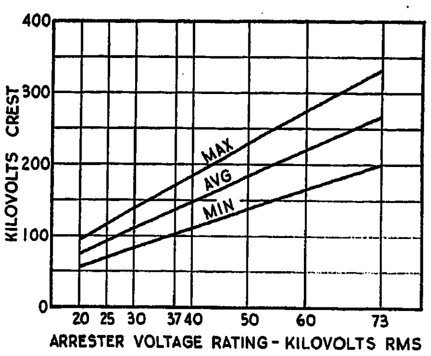


Figure 5. Front-of-wave impulse spark-over* of line valve-type lightning arresters

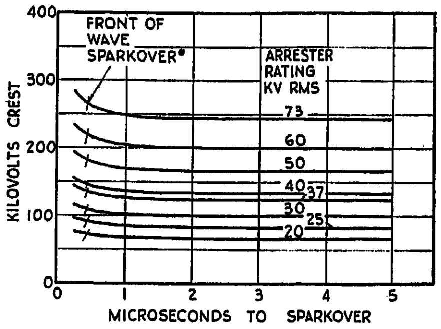


Figure 6. Average impulse spark-over volt-time characteristic of line valve-type lightning arresters

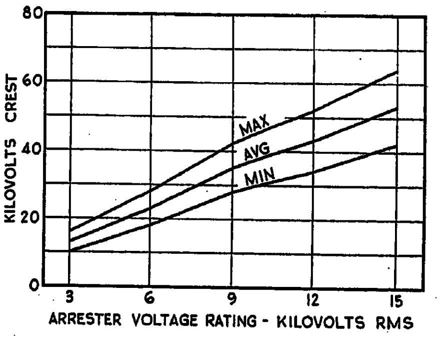


Figure 9. Front-of-wave impulse spark-over* of station valve-type lightning arresters rated 3–15 kv

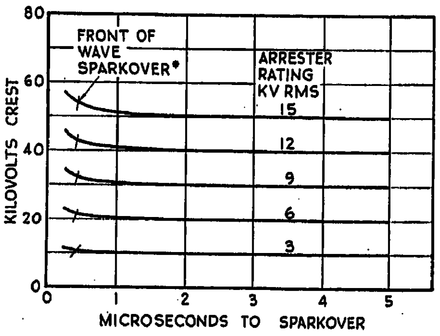


Figure 10. Average impulse spark-over volt-time characteristic of station valve-type lightning arresters rated 3–15 kv

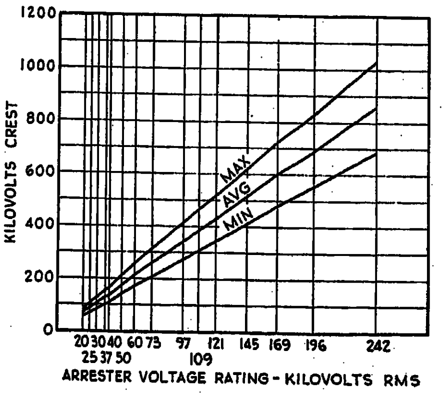


Figure 13. Front-of-wave impulse spark-over* of station valve-type lightning arresters rated 20–242 kv

*100-kv per microsecond per 12-kv of lightning arrester rating

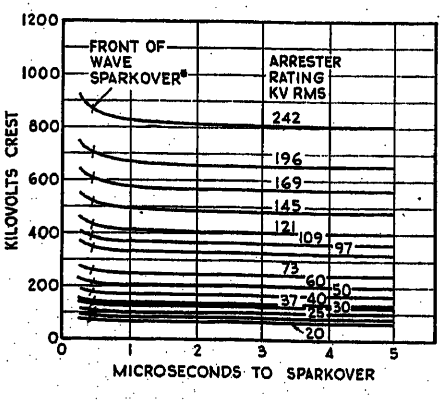


Figure 14. Average impulse spark-over volt-time characteristic of station valve-type lightning arresters rated 20–242 kv

*100-kv per microsecond per 12-kv of lightning arrester rating

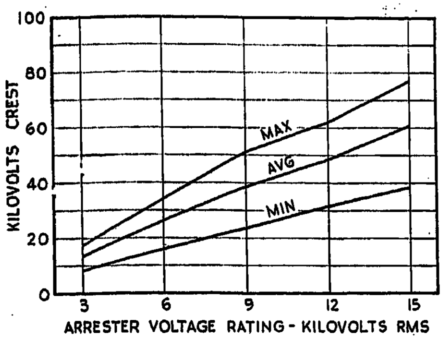


Figure 3. Discharge voltage at 5,000 amperes of distribution valve-type lightning arresters

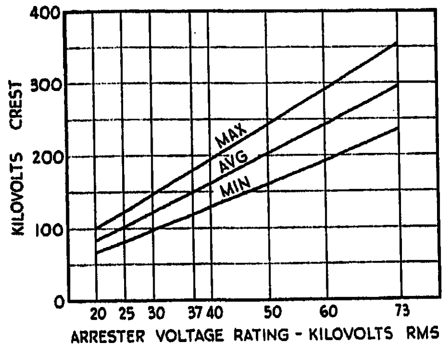


Figure 7. Discharge voltage at 5,000 amperes of line valve-type lightning arresters

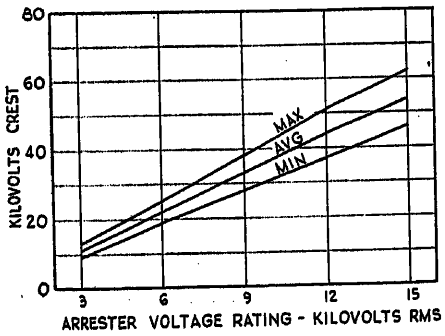


Figure 11. Discharge voltage at 10,000 amperes of station valve-type lightning arresters rated 3–15 kv

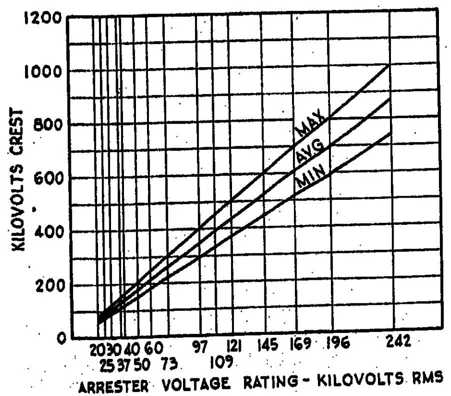


Figure 15. Discharge voltage at 10,000 amperes of station valve-type lightning arresters rated 20–242 kv

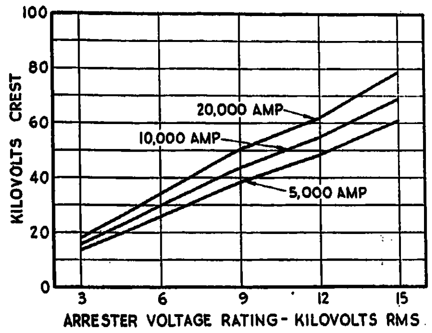


Figure 4. Average discharge voltage characteristic of distribution valve-type lightning arresters

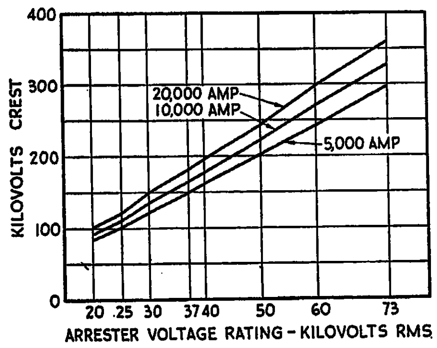


Figure 8. Average discharge voltage characteristic of line valve-type lightning arresters

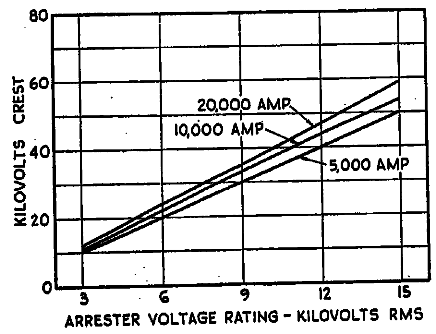


Figure 12. Average discharge voltage characteristic of station valve-type lightning arresters rated 3–15 kv

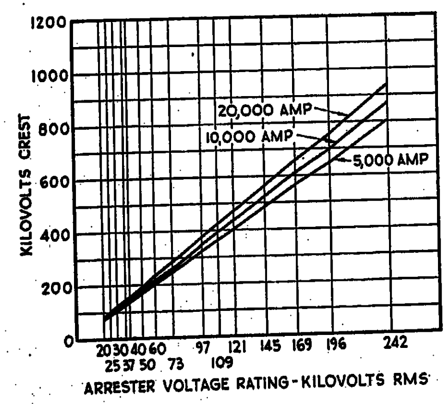


Figure 16. Average discharge voltage characteristic of station valve-type lightning arresters rated 20–242 kv

Rod-gap spark-over voltages are given in Table IV and Figures 20 and 21. These data have been taken from the latest available information, given in the report by the EEI-NEMA Subcommittee on Correlation of Laboratory Data.[5] Sixty-cycle and critical impulse spark-over voltages are included for the standard 1/2-inch square cut rod gap (AIEE Standards Number 4).[6] The rod-gap spark-over voltages may vary approximately ±8 per cent, where standard atmospheric conditions apply. For other conditions, additional variations may be as much as ± 10 to 15 per cent. As in the case of line expulsion-type lightning arresters, for times to spark-over shorter than those at which critical spark-over occurs, no agreement on spark-over voltages of rod gaps has been reached between various laboratories. Hence it is not possible to publish the complete volt-time spark-over characteristics of rod gaps at this time.

The data show variations in the characteristics of lightning arresters of all makes as high as ± 35 or 40 per cent. For lightning arresters of any one make, however, the tolerances permitted by the manufacturer do not exceed the following values: distribution and line valve- and expulsion-type lightning arresters, ± 25

Table IV. Rod-Gap Spark-over Voltages

Gap Spacing, Inches	60-Cycle Spark-over Voltage, Kv Rms	Critical Impulse Spark-over Voltage 1.5 by 40 Microsecond Wave, Kv Crest Positive	Negative
0.5	11.5	22	23
0.75	17.5	30	
1.0	22.5	38	38
1.5	32	51	51
2	40	60	62
3	50	75	82
4	58	91–95	102
5	66	106–114	123
6	74	128–141	143
7		141–155	163
8	89	159–166	183
9		175–178	203
10	105	190	224
12			259–271
14			293–318
15	150	275	309–342
16			323–359
18			360–386
20	200	350	395–415
25			490
30	295	505	575
40	385	650	740
50	480	800	910
60	575	945	1,070
70	665	1,095	1,235
80	755	1,240	1,405
90	835	1,385	1,570
100		1,530	

Test conditions: temperature 25 degrees centigrade; barometric pressure 76 centimeters mercury; humidity 0.6085-inch vapor pressure.

Dual values are due to unstable conditions, the cause of which is not known.

The rod-gap spark-over voltages may vary approximately ±8 per cent, where standard atmospheric conditions apply. For other conditions, additional variations may be as much as ±10 to 15 per cent.

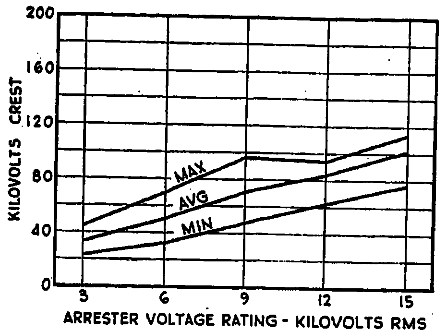


Figure 17. Front-of-wave impulse spark-over* of distribution expulsion-type lightning arresters

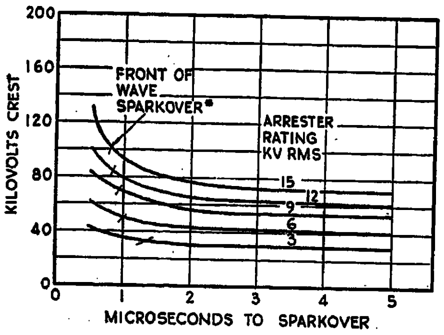


Figure 18. Average impulse spark-over volt-time characteristic of distribution expulsion-type lightning arresters

* 100-kv per microsecond per 12-kv of lightning arrester rating

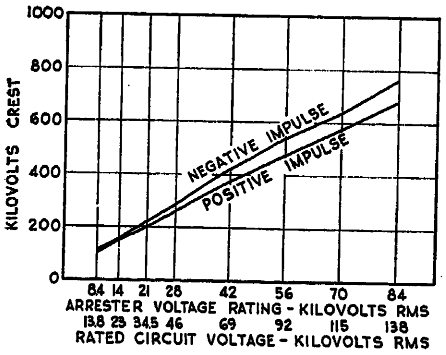
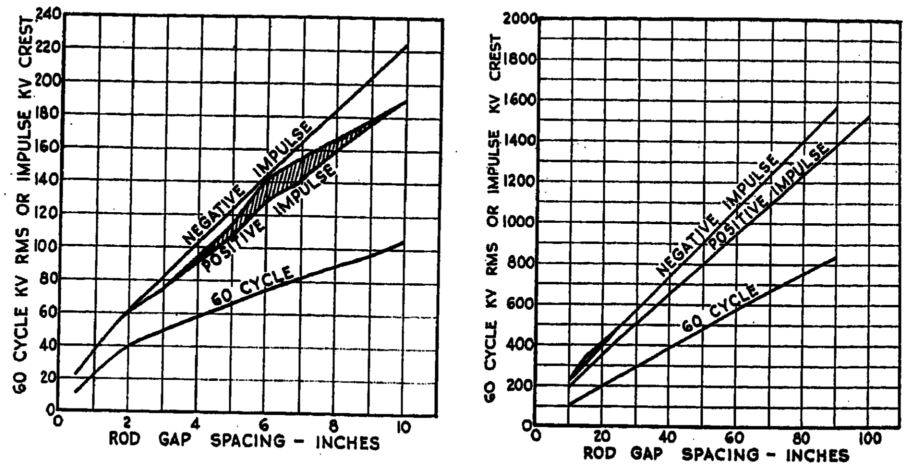


Figure 19. Average critical impulse spark-over voltage in 1.5 by 40 microsecond wave of line expulsion-type lightning arresters

per cent for spark-over voltages and (for valve-type lightning arresters only) ± 20 per cent for discharge voltages; station valve-type lightning arresters, ± 20 per cent for spark-over voltages and ± 15 per cent for discharge voltages.

Lightning arresters covered by this report have a 60-cycle spark-over voltage that is at least 1.5 times the voltage rating of the lightning arrester. The discharge current withstand test on a 5 by 10 microsecond wave is 65,000 amperes for distribution and line arresters of both valve- and expulsion-type, and 100,000 amperes for the station valve-type arresters.



Figures 20 and 21. Average 60-cycle and critical impulse spark-over voltages of rod gaps

Impulse voltages are crest values on 1.5 by 40 microsecond waves at 25 degrees centigrade, 76 centimeters barometric pressure, and 0.6085-inch vapor pressure. The rod-gap spark-over voltages may vary approximately ±8 per cent where standard atmospheric conditions apply. For other conditions, additional variations may be as much as ±10 to 15 per cent

References

1. STATION-TYPE LIGHTNING-ARRESTER PERFORMANCE CHARACTERISTICS, AIEE Committee Report. *Electrical Engineering* (*AIEE Transactions*), volume 59, June 1940, pages 347–48.

2. LINE-TYPE LIGHTNING-ARRESTER PERFORMANCE CHARACTERISTICS, AIEE Committee Report. *Electrical Engineering* (*AIEE Transactions*), volume 60, November 1941, pages 982–83.

3. DISTRIBUTION-TYPE LIGHTNING-ARRESTER PERFORMANCE CHARACTERISTICS, AIEE Committee Report. *Electrical Engineering* (*AIEE Transactions*), volume 61, March 1942, page 132.

4. EXPULSION-TYPE LIGHTNING ARRESTERS' IMPULSE SPARK-OVER VOLT-TIME CHARACTERISTICS AIEE Committee Report. *AIEE Transactions*, volume 67, part I, 1948, page 520.

5. FLASHOVER CHARACTERISTICS OF ROD GAPS AND INSULATORS, EEI-NEMA Subcommittee Report. *Electrical Engineering*, volume 56, June 1937, pages 712–14.

6. MEASUREMENT OF TEST VOLTAGE IN DIRLECTRIC TESTS, AIEE Standard Number 4, 1942.

Discussion

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): In looking through the wealth of data in this report, the thought occurred to me that discharge voltages are not given for high enough values of currents with the 10 by 20 microsecond waves. This report gives values with 5,000, 10,000, and 20,000 amperes crest.

When I was very active in matters relating to lightning and lightning protection 15 to 25 years ago, test values for such currents would have seemed quite satisfactory. It had been found in the 1920's that many distribution lightning arresters would blow up when the currents were even less than the minimum value previously mentioned, that is, less than 5,000 amperes. However, for a good number of years, the industry has had the requirement that distribution and line-type lightning arresters should withstand a discharge current of 65,000 amperes on a 5 by 10 microsecond wave and also has had the corresponding requirement of 100,000 amperes for station-valve-type lightning arresters.

The requirement to measure test voltages for discharge currents of 5,000, 10,000 or 20,000 amperes was formulated many years ago. Those currents cover a good majority of the discharge currents that pass through lightning arresters. There are, however, cases where the actual discharge currents approach in magnitude the discharge-current capabilities of lightning arresters, and sometimes exceed them.

In view of the foregoing points, it would seem desirable and consistent to have data on discharge voltages made available for much larger currents than the present maximum of 20,000 amperes.

E. H. Grosser: In preparing this report, the intention was to present data on the performance characteristics of lightning protective devices in strict conformance with the methods of testing prescribed in the proposed combined AIEE Lightning Arrester Standards for Valve and Expulsion Lightning Arresters. The discharge voltages were therefore reported for the surge current values specified in the proposed Standards, namely 5,000, 10,000, and 20,000 amperes.

It might be pointed out, however, that adequate justification exists for the values of current adopted for the standard tests. The transmission engineer, who designs his lines to withstand lightning stroke currents of 100,000 amperes or more, may be inclined to think in terms of such currents, overlooking the fact that usual or average stroke currents are much smaller. Currents discharged by lightning arresters have been found to be still smaller, due probably to less severe conditions of exposure for lightning arresters as compared with transmission lines. The 20,000-ampere test actually approaches the highest values of current measured in lightning arrester leads in service. It is believed that present-day lightning arresters, tested in accordance with the proposed Standards, represent a reasonable design compromise in being able to maintain good protective characteristics for moderately large surge currents.

A New Automatic Load Control for Turbine Generators

H. B. RUUD
NONMEMBER AIEE

S. B. FARNHAM
MEMBER AIEE

Synopsis: In an interconnected power system, load changes of sufficient magnitude to affect system frequency are normally shared by all the generators on the system in proportion to their ratings and the characteristics of their governors. This inherent sharing of load changes is highly desirable in the majority of cases. It may, however, present a problem in a few instances if some of the generators are remote geographically and electrically from that part of the system which includes a large fluctuating load. In such cases it would sometimes be preferable if the load changes could be confined in whole or in part to the local generators, thereby relieving the remainder of the system from having to respond to distant load fluctuations. This situation may become acute in the case of systems interconnected through long tie lines, where abrupt load changes in either system result in large transfers of synchronizing power through the interconnecting ties. This paper describes a new automatic load control arrangement which has the ability to respond to local load changes in anticipation of their effect on system frequency. At the same time, however, it does not deprive the conventional speed governor of its ability to control the prime mover input to help maintain constant system frequency. In this latter respect lies its essential and unique difference from other somewhat similar arrangements described previously.[1,2,3]

Introductory Background

THE decision to install automatic load-regulating control on the turbines at the Riverside Power Station of the Iowa-Illinois Gas and Electric Company near Davenport, Iowa, was prompted by the Aluminum Company of America constructing a rolling mill adjacent to that power station with all power requirements of the mill to be supplied by the utility company. The construction of this large capacity mill made automatic load-regulating control desirable for two reasons. First, the Tri-City area, (Davenport, Rock Island, Moline), which is served in part by the Riverside Power Station, was experiencing peak loads of approximately 100,000 kw. For comparison, the peak loads anticipated for the proposed rolling mill were in the order of 40,000 kw. The nature of the forthcoming load meant that the utility was to be faced with relatively large, sudden, and rapidly reoccurring load changes. The power company's previous experience with sudden load changes had been confined to electric steel furnace loads. While these furnace load changes were considerably smaller than those anticipated for the rolling mill, operating experience gained through them indicated that automatic load-regulating control for the new mill load would be highly desirable.

Second, the Iowa-Illinois Gas and Electric Company, in co-operation with other utilities in Iowa and Missouri, was in the process of constructing a 161-kv interconnecting transmission line, Figure 1. In order to achieve the maximum benefits from the interconnection facilities, the participating power companies agreed that each participant was to carry the load variations occurring in its own area. In brief, the interconnection facilities were to be essentially base loaded. This comprised the second reason for the installation of automatic load regulator control to carry the Alcoa mill load swings.

The Aluminum Company of America furnished the utility with representative kilowatt load curves taken at a similar, but smaller, rolling mill in Tennessee. From these curves the severity and frequency of the load swings for the mill under construction were predicted. It was estimated that the load swings would vary in magnitude from 10,000 kw to 16,000 kw. That is, the mill load would vary between 5,000 and 8,000 kw above and below the mean load. The frequency of the load swings was expected to be about one per minute. The rate of load change was estimated to vary between 2,000 and 5,000 kw per second.

Load-Regulating Equipment

TURBINE PARTS

A schematic diagram of the turbine speed-governing and control mechanism as applied to two of the four major generating units at Riverside is shown in Figure 2. This will be recognized as essentially the conventional speed governor and oil-actuated valve arrangement, although for purposes of this application it has several distinguishing modifications.

First, the synchronizing motor is sufficiently oversized to provide a travel time between the "full-open" and "full-closed" positions of approximately four to five seconds, as contrasted with a time of approximately 20 to 30 seconds achieved with the conventional governor motor. This higher speed of operation is essential in order to accomplish the desired objective of forcing the controlled generator to assume the local load fluctuations in advance of their tendency to change system frequency.

Second, instead of the split series field type of motor usually employed, this application makes use of a separately excited d-c shunt motor. This selection is determined by considerations of the control circuit, as will become apparent.

Third, a follow-up arrangement is added, which provides a voltage output whose magnitude is proportional to the speed-changer lead screw position. At constant system frequency, this voltage therefore also is essentially proportional

---

Paper 49-289, recommended by the AIEE Power Generation Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 14, 1949; made available for printing September 12, 1949.

H. B. RUUD is Chief System Supervisor, Iowa-Illinois Gas and Electric Company, Rock Island, Ill., and S. B. FARNHAM is with the General Electric Company, Schenectady, N. Y.

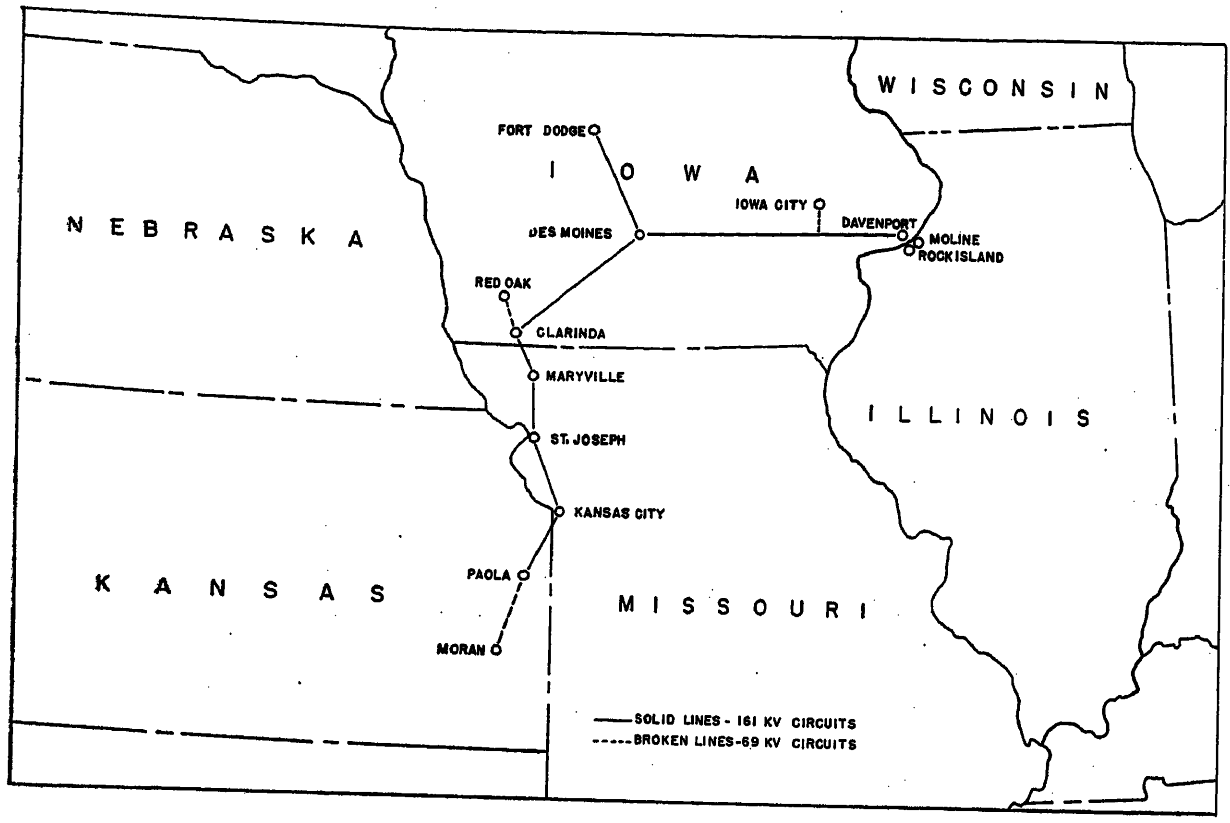


Figure 1. Map of interconnected system

to the kilowatt load on the turbine generator. If, however, for any reason there should be an appreciable nonlinearity between kilowatt load and mechanism position as might result for example from the inherent characteristics of the steam inlet valves and passages, this can be compensated for in the gearing or linkage through which the follow-up device is actuated. While the device itself may be anything capable of producing a voltage whose magnitude is proportional to position, such as a potentiometer, a selsyn, operating as an induction-voltage regulator, was chosen for convenience in this application.

Fourth, an initial steam-pressure regulator is included, whose function it is to prevent the possible "carry-over" of water from the boiler to the turbine inlet should excessive demands be placed upon the boiler by the load regulator. Normally the anticipated load increases will be within the ability of the boilers to handle without suffering an excessive pressure drop. If, however, for any reason circumstances were such that a sudden load demand might reduce the boiler pressure to a point where water carry-over could result, this initial pressure regulator is designed to assume control by pulling apart a telescoping connection, Figure 2, and thereby prevent any further increase in the loading of this machine until normal boiler pressure has been re-established. Rated steam pressure for this unit is 850 pounds per square inch. The initial steam pressure regulator is calibrated to assume control if the pressure falls below 750 pounds.

ELECTRICAL CONTROLS

The controlling quantity which initiates operation of the automatic load-regulating control equipment is current in the feeder circuits to the Alcoa mill. This current is totalized in the secondaries of two current transformers, one each in the same phase of the two 13.8-kv mill feeder circuits, as indicated in Figures 3 and 4. The totalized current produces a voltage drop across resistor R, of a bridge circuit, as indicated in Figure 4. Two arms of the bridge are the opposing control fields of a continuously driven amplidyne generator, connected through rectifiers. The other two arms are formed by a potentiometer or variable autotransformer, across which are established the reference voltages E_1 and E_2. The proportionality between these voltages determines the base load at which the controlled machine operates. Hence, the position of the movable arm of the potentiometer or variable autotransformer may actually be calibrated in base-load kilowatts. At each value of steady base load on the generator, the follow-up device described previously on the turbine governor re-establishes the balance of the bridge.

Any current in the Alcoa feeders, by producing a voltage across resistor R, unbalances the bridge, and thus causes the amplidyne generator to produce a direct voltage of the correct polarity to cause rotation of the synchronizing motor in the direction to pick up load. The magnitude of the direct voltage, and, consequently the rate at which load is picked up, depends on the magnitude of the Alcoa load change. Once the turbine generator output satisfies the new load requirements, the voltage from the follow-up device re-establishes the balance of the bridge, the amplidyne generator output voltage returns to zero, and the synchronizing motor comes to rest. These sequences take place with such rapidity that the turbine generator output follows the fluctuating Alcoa load demands within approximately 1 to $1^1/_2$ seconds; and at this rate the load swings do not cause a measurable change in system frequency.

It will be apparent that by adjusting the ohmic value of resistor R across which the Alcoa load current produces the controlling voltage drop, the controlled generator can be made to assume all or any desired part of the Alcoa load changes. In this manner, with all four of the large turbine generators at Riverside similarly equipped, the total fluctuating load can be carried by any one machine, or divided among them in any ratio desired.

It is to be noted in Figure 4 that the reference voltages and the follow-up voltages are supplied from a common source. Not only must these voltages be in phase with one another, but also they must be derived from the same phase in which the current transformers for the Alcoa load

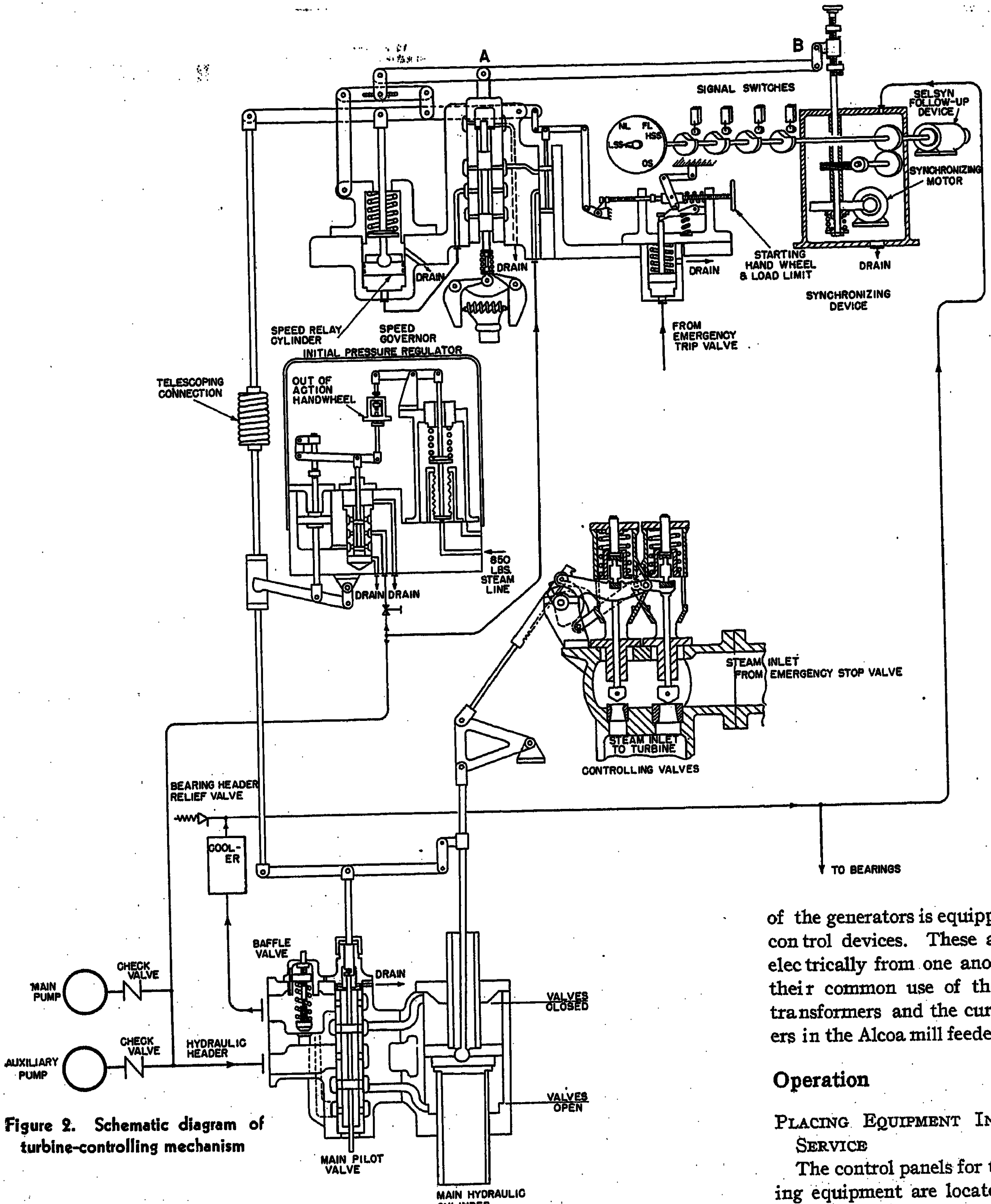


Figure 2. Schematic diagram of turbine-controlling mechanism

are connected. This is necessary both to reflect in the bridge circuit a full measure of the quantities involved, rather than only a component dependent on vector relationships, and also to avoid operation of the controls in response to the reactive power or kilovar demands of the Alcoa load. With the connections as indicated, or with any comparable arrangement in which the reference voltages and kilowatt component of load current are in phase, the bridge is insensitive to kilovars, since the kilovar component of load current produces a voltage vector which is at right angles to the reference voltages, and thus affects both sides of the balanced bridge equally. Hence the kilovar component produces no net change in the amplidyne control fields. In this manner, the over-all system is responsive to kilowatts alone, in spite of the fact that the controlling quantity is load current.

Single-phase quantities are utilized, both in the interest of simplicity, and because, with the expected loads being essentially balanced, an entirely acceptable degree of accuracy can be realized. Each of the generators is equipped with its own control devices. These are independent electrically from one another, except for their common use of the bus potential transformers and the current transformers in the Alcoa mill feeders.

Operation

PLACING EQUIPMENT IN AND OUT OF SERVICE

The control panels for the load-regulating equipment are located in the power station switchboard room near the conventional generator control panels. The switchboard operator is responsible for the loading of the generator, whether the unit is on manual or either of three different automatic controls. On the generator load regulating control panel there are mounted along with the necessary indicating instruments, two transfer switches. The first transfer switch selects the desired type of control. The controls available are:

1. Manual control.
2. Tie-line bias control under which the

load on the generator is automatically adjusted to maintain the requirements of the 161-kv interconnection with other power companies.

3. Automatic load regulator control under which the generator's load is determined by the base load setting and the load on the feeders to the Alcoa mill.

4. Tie-line bias control and load regulator control acting simultaneously.

The second transfer switch is used only for changing to or from automatic load-regulator control. This switch has the positions of "off", "test", and "regulate." The use of these two transfer switches makes the change from one type of control to another control a simple, orderly procedure. Although good operating practice requires a definite sequence of switch operations, the two transfer switches have their contacts so interconnected that it is impossible for the generator to take on or drop a large block of load due to improper operation of these switches.

The procedure for placing a generator on automatic load-regulator control will be explained step-by-step. Assume that the 40,000-kw generator in the Riverside Station is on manual control and is carrying 20,000 kw of system load. The system load, which also is shared by other machines, includes a varying component of 30,000 to 40,000 kw caused by the Alcoa mill. Assume that it is desired to confine one-half of this fluctuating load to the subject machine.

The operator first adjusts resistor *R* in the load-measuring bridge circuit, Figure 4, to the "all-out" position if it is not already so set. With *R* "all-out", the bridge cannot be affected by the mill load.

The operator then selects the new type of automatic load control by turning the first transfer switch to the automatic load regulator position. The operation of this switch connects the potential to the load-measuring bridge and energizes the amplidyne driving motor. At this state the load regulator does not take control because the output of the amplidyne is not connected to the synchronizing motor.

The second transfer switch is then turned to the "test" position. This position permits the operation of the motor-operated base-load adjustor by the operator. It also makes operative a synchronizing motor control switch located at the load regulator control panel. The operator then adjusts the actual load on the generator with this control switch, if this is necessary. With the generator carrying 20,000 kw, the variable autotransformer, which serves as base-load adjustor, is set so that the unbalance in the bridge circuit due to the output of the selsyn follow-up device on the governor lead screw is exactly equalized by the unbalance caused by the position of the base-load adjusting autotransformer. When these two quantities are equal, the output of the amplidyne is zero. The amplidyne output is indicated on a zero-center voltmeter.

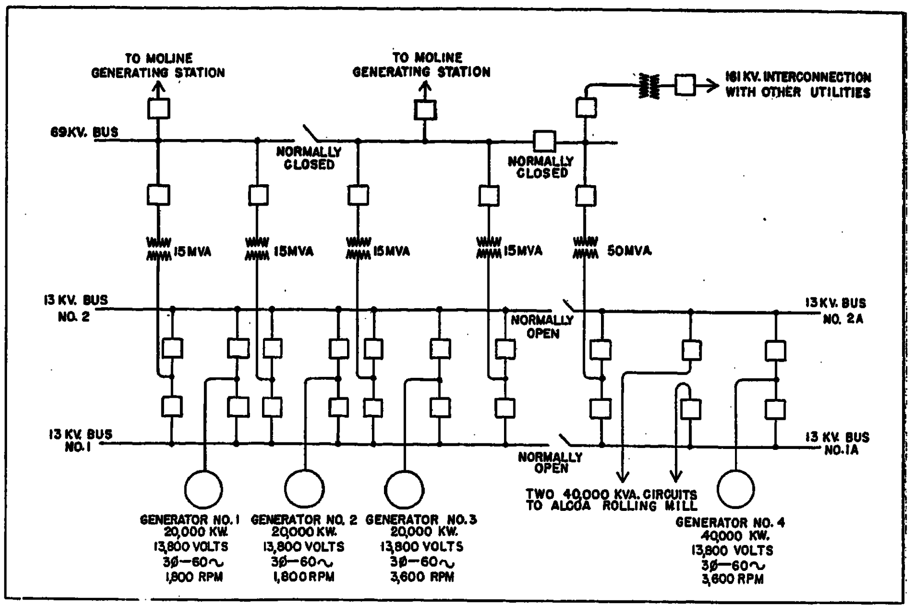

TO MOLINE GENERATING STATION
TO MOLINE GENERATING STATION
161 KV. INTERCONNECTION WITH OTHER UTILITIES
69KV. BUS
NORMALLY CLOSED
NORMALLY CLOSED
15MVA
15MVA
15MVA
15MVA
50MVA
13 KV. BUS NO. 2
13 KV. BUS NO. 2A
NORMALLY OPEN
13 KV. BUS NO. 1
13 KV. BUS NO. 1A
NORMALLY OPEN
TWO 40,000 KVA. CIRCUITS TO ALCOA ROLLING MILL
GENERATOR NO. 1 20,000 KW. 13,800 VOLTS 3ϕ—60~ 1,800 RPM
GENERATOR NO. 2 20,000 KW. 13,800 VOLTS 3ϕ—60~ 1,800 RPM
GENERATOR NO. 3 20,000 KW. 13,800 VOLTS 3ϕ—60~ 3,600 RPM
GENERATOR NO. 4 40,000 KW. 13,800 VOLTS 3ϕ—60~ 3,600 RPM

Figure 3. Electrical diagram of Riverside Station

The second transfer switch is then turned to the "regulate" position. This disconnects the synchronizing motor armature from its station battery source and connects it to the output of the amplidyne generator. The automatic load regulator equipment now controls the generator load.

The generator, at this stage, does not respond to the mill load changes, because *R* is in the "all-out" position. The machine carries only the 20,000 kw as dictated by the base load adjustor. The operator then proceeds to change the per cent of Alcoa load carried by this generator from zero to 50 per cent by adjusting *R*. As he increases *R* toward 50 per cent, this simultaneously removes 50 per cent of the Alcoa load from the other machines on the system.

The generator now carries 20,000 kw plus one-half of the Alcoa load; that is, the generator load varies from 35,000 to 40,000 kw as the Alcoa load changes. If the system load or the Alcoa mill load changes sufficiently to demand a new base load on the generator, this is accomplished by simply resetting the base load adjusting autotransformer. As indicated previously, this adjustment is actually calibrated in base load kilowatts.

The procedure for removing a generator from automatic load regulator control is a simple operation. The Alcoa mill load on the subject machine is reduced to zero by turning resistor *R* "all-out." As the mill load is removed from this load regulator, it must necessarily and simultaneously be added to the load on the other machines. When the mill load component has been removed from the load-measuring bridge circuit, the generator will be essentially base loaded; and the output of the amplidyne to the synchronizing motor will be zero. With the amplidyne output voltmeter reading zero voltage, the second transfer switch can be turned to the "off" position which removes the automatic load regulator from control with no shifting of generator loads. The desired type of generator control is then selected with first transfer switch.

SYNCHRONIZING

When synchronizing a generator with the system under manual control, a travel time of approximately 20 to 30 seconds between the "high speed" and "low speed" positions of the synchronizing device is desirable in order to give the operator the requisite degree of control. The 4- to 5-second travel time as provided in this application to realize the objective of automatically confining local load swings to the controlled machine must therefore be extended to approximately 20 or 30 seconds when the machine is under manual control. This is accomplished by supplying the synchronizing motor armature at reduced voltage obtained from the station battery through an appropriate series resistor or potentiometer.

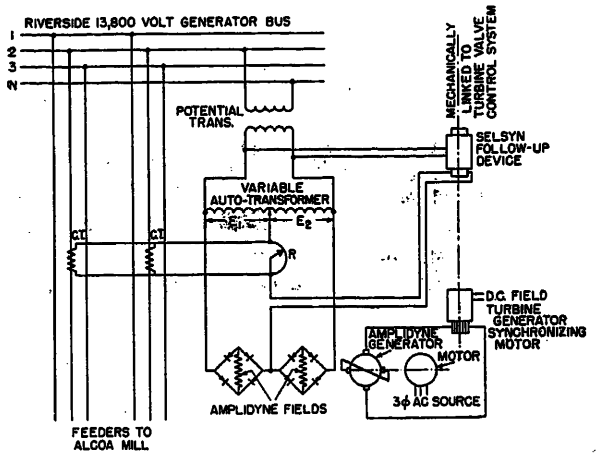

RIVERSIDE 13,800 VOLT GENERATOR BUS
1
2
3
N
POTENTIAL TRANS.
VARIABLE AUTO-TRANSFORMER
E1
E2
C.T.
C.T.
R
MECHANICALLY LINKED TO TURBINE VALVE CONTROL SYSTEM
SELSYN FOLLOW-UP DEVICE
D.C. FIELD
TURBINE GENERATOR SYNCHRONIZING MOTOR
AMPLIDYNE GENERATOR
MOTOR
3φ AC SOURCE
AMPLIDYNE FIELDS
FEEDERS TO ALCOA MILL

Figure 4. Simplified diagram of automatic control circuit

Joint Operation with Tie Line Loading Controller

Conventional slow-acting tie-line load bias control equipment also is employed at Riverside for regulating the loading of the 161-kv interconnection with the other utilities. Not only will this be required to operate alone in the usual manner, but also it must be suitable for simultaneous operation in conjunction with the automatic turbine load-regulating equipment. An obvious requirement of this joint operation is that each controller perform its alloted functions without interfering with the normal operation of the other.

During such times as both controllers are operating simultaneously, the turbine load control equipment will operate on the synchronizing motor in the manner described previously. The tie-line controller, however, will not operate directly on the synchronizing motor, but indirectly through the turbine base load adjusting autotransformer. Its operation, therefore, will be to reset the turbine base-load adjustment periodically, in accordance with the dictates of the tie-line loading. In this way, full freedom and independence of action is afforded to both types of control. Such could not be the case if both attempted to operate directly on the synchronizing motor, since it may be shown that one would nullify the corrective effects of the other.

When the Aluminum Mill is not operating, or if for any other reason the turbine load control equipment is removed from service, the tie-line controller is transferred directly to the synchronizing motor. The motor speed, however, is then reduced as described previously to a level consistent with that normally employed in such cases, that is, approximately 20 to 30 seconds for the range of travel between the "fully-open" and "fully-closed" valve positions.

The operations incident to transferring the control to conform to any of the several available methods of operation are all performed simultaneously by the first of the two previously mentioned control switches. This same switch also connects the synchronizing motor armature either to the amplidyne generator or to the reduced voltage source, as required. The motor field is at all times excited at constant rated voltage.

Normal Operating Practice

At the time of writing, the installation of load regulating equipment has not been completed, but this type of automatic control is in process of installation on the four major generating units at the Riverside Station. All four machines have 13,800-volt 3-phase 60-cycle generators. Control equipment as described in this paper is being installed on one 40,000-kw unit and one 20,000-kw unit, both of which are 3,600-rpm machines. Load regulating control equipment of a different manufacture is being installed on two 20,000-kw 1,800-rpm units.

Normal operating practice pertaining to the number of machines simultaneously under automatic load regulator control and the proportioning of Alcoa mill load between them has not been established. Normal practice can be determined only after the accumulation of actual operating experience. The ability of the boiler systems to respond to the demands will be a prime consideration. In the station there are three boiler systems operating at different pressures and temperatures. The reserve steaming capacity varies between systems. All boilers are equipped with full automatic control and normally burn either powdered coal or natural gas.

In view of the nature of the fuel, full automatic boiler control, and the fact that four generators will be equipped with automatic load regulator control, the proportioning of mill load between machines is not expected to pose a serious problem. The actual proportioning will vary from day to day and from hour to hour, dependent upon available steaming capacity on each boiler system and total system electric load. The Alcoa load will be divided between as many generators as is practical so as to cause the greatest number of boilers to share the sudden changes of steam demand. This practice will, of course, be modified by generating unit over-all efficiencies. For greatest economy, it appears that the newer, more efficient machines should be base loaded insofar as is practicable, leaving the older and generally less efficient machines to carry the predominant part of the fluctuating load.

Comparison With Other Similar Equipment

Other automatic load-control systems have been described previously.[1,2,3] They have had the common characteristic that when the automatic load control feature was in service, the speed governor was made inoperative, except that on overspeed it could take precedence over the other controls and shut down the machine. Conversely, if the machine was under the control of its speed governor, the automatic load control features were necessarily disconnected. Never was it possible to operate the machine under both types of control simultaneously. To the best of the authors' knowledge, the Riverside installation is the first embodiment of an arrangement permitting simultaneous control by both the speed governor and the automatic load regulating equipment.

The earlier load controls were applied to machines serving industrial power systems, wherein the generating capacity was relatively small compared with that of the utilities with which they were interconnected. The purpose of the automatic controls was to confine the local load swings insofar as practicable to the industries' own generators, and this was accomplished successfully. However, in the event of a drop in the frequency of the interconnected system, as might be occasioned by the tripping out of a transmission line or the loss of a major unit of the utilities' generation, there was no expectation that the industries' machine would pick up load in an endeavor to support frequency. This was entirely rational, since the industries' machines in general are relatively so small a part of the interconnected system generation as to be ineffectual in attempting to maintain sagging frequency.

Such a situation, however, could not be tolerated in load control equipment applied to the generators of a power company. In this service, every generating unit must, at all times it is in operation, be able to pick up load in response to its speed governor. Stated another way, even though the machine may be performing its intended functions under the control of the automatic load regulator as long as system frequency remains normal, any drop in frequency must be the signal for the machine to increase its output to help offset the drop. The new control equipment described herein fulfills this criterion.

Its operation may be visualized as simply that of a load regulator, so long as system frequency remains constant. Under this condition, the governor arm, top of Figure 2, pivots about point A as a fulcrum. If system frequency changes, point B becomes the fulcrum. Practically, of course, both motions can and do occur continually and simultaneously. Hence the load regulating function in no way impairs the ability of the machine to "step into the breach" whenever a system emergency calls for its full capabilities toward helping to maintain system frequency.

Conclusions

It had been hoped to include within this paper the results of actual operating experience with the new regulating equipment.

Unfortunately, however, at the time of writing the equipment had not yet been completely installed. Hence these remarks necessarily reflect expectation rather than realization. Notwithstanding this, the wide background of experience that has been accumulated with the essential components of this control as used for other governor applications, amplidyne generator voltage regulators, current controllers, and locomotive controls gives a basis for the following summary:

1. The control arrangement described herein affords a means for confining load swings to near-by generators, thereby relieving tie lines and distant generators from handling large fluctuating loads.

2. For the first time, the load-regulating function is obtained without sacrificing the ability of the controlled machine to help support system frequency.

3. The speed of response of approximately 4 to 5 seconds between "no load" and "full load" is within the capabilities of modern turbine generators, and probably approximates the maximum rate at which boiler loading can be increased without the likelihood of water carry-over.

4. Flexibility and economy of operation are assured since all machines in the station are similarly equipped, and since each of the controlled machines can be made to assume any desired portion of the total fluctuating load within its rating.

References

1. AUTOMATIC LOAD CONTROL FOR TURBINE GENERATORS, F. E. Crever, R. L. Jackson. *Electrical Engineering* (*AIEE Transactions*) volume 64, September 1945, pages 656–60.

2. AUTOMATIC REGULATION OF TURBINE GENERATORS TO RELIEVE POWER-SUPPLY SYSTEMS OF STEEL-MILL LOAD SWINGS, W. W. Eberhardt, C. W. Harrison, E. J. Kohn, A. G. Darling. *Electrical Engineering* (*AIEE Transactions*) volume 64, November 1945, pages 751–58.

3. SUDDENLY APPLIED LOADS CARRIED BY A VARIABLE-RATIO SYNCHRONOUS INDUCTION FREQUENCY CHANGER, A. G. Darling, G. A. Kaufman. *AIEE Transactions*, volume 67, part II, 1948, pages 91–94.

Discussion

G. S. Lunge (General Electric Company, Schenectady, N. Y.): This load regulator, if connected in accordance with Figure 4 of the paper, is obviously applicable if the line or lines feeding the fluctuating load leave the controlled generating station itself, so that instrument transformer secondary circuits can be connected directly to the regulator.

However, this load regulator is equally applicable if the generating station is many miles from where the active power flow to the fluctuating load can be measured, provided that this measurement is telemetered to the generating station and converted back to a suitable proportional value of single-phase alternating current. Both the telemetering and the reconversion to alternating current must take place continuously and with negligible time lag, as discontinuous action could not be tolerated.

Depending on the nature of the channel for transmitting the telemetered quantity, either a torque balance telewattmeter or a frequency-type telewattmeter would be suitable. If more than one fluctuating load circuit is involved, the telemetering equipment would be arranged in the usual manner to totalize the kilowatt load in the various circuits. A torque balance converter would be suitable for converting the d-c milliampere output of the telemetering equipment to a proportional single-phase alternating current. For this particular application, the restraining element of the torque balance converter would have its potential coils energized from a regulated (constant) voltage a-c source. This feature is essential in order that the converter current output (rather than its power output) may be linearly proportional to the power flow measurement telemetered to it.

A suitable amplifier would be needed to step up the 0.5-ampere 3-volt-ampere output of the torque balance converter to that required by the amplidyne field circuit.

In this case the regulated voltage a-c supply also should be used to supply the amplidyne field bridge circuit and the follow-up Selsyn supply circuit, to eliminate the effects of variation in generating station voltage.

The over-all time lag of the instrument combination suggested in this discussion is less than one second, regardless of whether a torque balance or a frequency-type telemeter is used, and there are no rotating parts involved in the instruments, other than the jewelled bearing shaft and disk assembly in the frequency-type telemeter transmitter.

Determining Incremental Regulation and Dead Band of Governors on Steam Turbine Generators Under System Load Conditions

E. L. TORNQUIST
MEMBER AIEE

I. L. WADE
NONMEMBER AIEE

Synopsis: The purpose of this paper is to present some of the reasons for testing steam turbine governors and to describe a method which makes it possible to determine the incremental regulation, dead band and stability of a governor connected to a large power system. The test equipment described has no mechanical inertia or lost motion and requires only connections to the potential and current metering circuits of the generator. The values obtained show the corresponding load change on the generator for any change in frequency. It is believed that these are the values which are most desired and are obtained directly under actual operating conditions of the unit without resort to assumptions as to the relationship between inlet valve movement and generator output. Some difficulties have been encountered and these are discussed with the hope that the work so far completed may be of interest to others and may stimulate thought towards a more simplified system for determining governor performance.

THE need for a method of testing that will determine the characteristics of a steam turbine governor is somewhat self-evident. However, a review of some of the factors that were considered important in the design of the test equipment described herein will be of interest since the objectives of the test dictated to a large extent the type of equipment desired.

The purpose of the test can be divided into the following:

1. To determine the performance of a governor with respect to purchase specifications.
2. To determine the necessity for and the results of maintenance work on the governor.
3. To determine the optimum settings of the governor through a knowledge of the governor characteristics and system requirements in order that each unit will perform most satisfactorily considering its size, heat rate, location on the system, and local plant steam generating conditions.

If the need is only for determining (1) above, somewhat elaborate equipment can be permitted, but to be effectively used for (2) and (3) the equipment should be simple to operate and provide the necessary data quickly.

The information necessary to evaluate the performance of a governor is the incremental regulation, dead band, and stability at all load points. The average steady-state speed regulation is only important in estimating the performance of the machine upon being disconnected from the system, since seldom is a turbine subject to frequency changes in the order of 4 or 5 per cent while connected to a large power system. However, each machine is being constantly influenced by small frequency changes in the order of 0.1 per cent and frequently 0.3 per cent. The response of the governor to these changes determines to a large extent the steadiness of the frequency, the division of a fluctuating load between the generators on the system, and the tendency for excessive changes in steam demand on the boiler room.

To maintain tie line loads at some predetermined value and to keep a constant integration of frequency for control of clocks most large power systems have installed remote automatic control on a number of turbines. A knowledge of the performance of a governor, therefore, is useful in determining which machines can best be used for load control. It can be seen, for instance, that if the remote load control calls for an increase of load at the moment the frequency is influencing the governor to reduce the load there will be a conflict between the sensitive governors on machines with load control and machines without load control. An understanding of these factors coupled with a knowledge of governor characteristics should assist in obtaining optimum performance of all machines on the system.

Method of Testing

A method of testing is desired that can be applied simply and does not require special attachments to the turbine. Around this principle a method was designed using a sensitive frequency bridge and a sensitive watt-converter connected into the current and potential metering circuits of the machine in such a manner that changes in both frequency and kilowatt output are converted into electrical quantities and these quantities appropriately indicated or recorded. These quantities are applied to an oscilloscope with a long persistence screen and the oscilloscope beam deflected horizontally with changes in frequency and deflected vertically with changes in kilowatts. The long persistence screen permits the pattern of the light beam to be observed for approximately 30 seconds. In this time a rotatable grid is adjusted to the slope of the figure and the degree of inclination read on a dial. If it is desired to obtain a record of the figure, a camera is placed in front of the oscilloscope. This gives a record of the type shown in Figure 1. The slope of line (*b*) is the incremental regulation. The line (*a*) is an index of the

---

Paper 49-290, recommended by the AIEE Power Generation Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 20, 1949; made available for printing September 12, 1949.

E. L. Tornquist and I. L. Wade are both with the Public Service Company of Northern Illinois, Chicago, Ill.

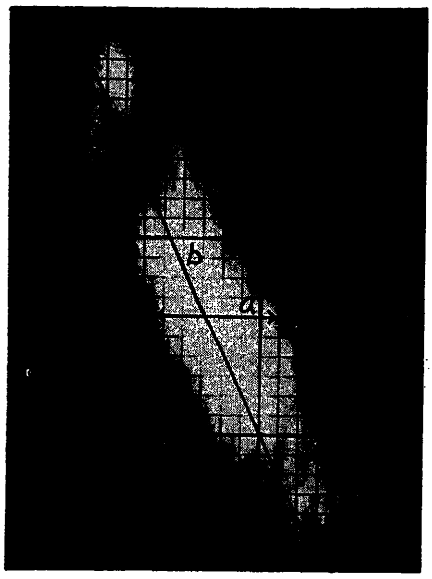


Figure 1. Kilowatts change versus frequency change with a 45-megawatt load on a 65-megawatt unit

Kilowatt change on vertical scale is 159 kw per small division and frequency change on horizontal scale is 0.005 cycle per small division

dead band. These readings are repeated at any desired intervals of load and the results plotted.

General Description of Equipment

The equipment used is shown in Figure 2. The standard cathode-ray oscilloscope with a long persistence screen comprises the heart of the system, and with the aid of the camera, gives a graphic record of the test data. The principle used to obtain these data by means of the oscilloscope is to apply an amplified direct voltage to the deflecting plates of the cathode-ray tube in such a manner that one set of deflecting plates will cause a horizontal deflection of the electron beam for changes in frequency and the other two deflecting plates will cause a vertical deflection of the electron beam for changes in kilowatt output. This principle is comparable to two mechanical forces displaced by 90 degrees. The resulting movement or deflection of the electron beam is, therefore, a combination of the two electrical forces acting upon the two sets of deflecting plates of the cathode-ray tube. The instrument thus becomes an X-Y recorder and the diagram thus obtained consists of a multiplicity of diagrams superimposed on a common axis and it usually resembles a parallelogram.

The amplifier, watt-converter, and bridge circuit are the means by which the various factors, such as frequency change and kilowatt output, are translated into proportional electrical quantities to be applied to the cathode-ray oscilloscope. The equipment and the method of operation is further described in Appendix I.

The AIEE Recommended Specifications for Speed-Governing of Steam Turbines Intended to Drive Electric Generators Rated 500 Kw and Up[1] defines incremental regulation in terms of kilowatt change with respect to speed change, or frequency change. These same specifications define dead band in terms of the speed change or frequency change over which there is no change in the position of the inlet steam valve, see Appendix II. Changes in the position of the steam valve are reflected in changes in the kilowatt output at some time interval later due to the inertia of the machine itself and the time it takes for the turbine to translate the changed steam flow into a changed power flow. Therefore, the method of determining dead band from the kilowatt output of the machine rather than from the valve movement does not conform to the AIEE specifications, but does give an index of the performance of the machine on the system under actual load conditions. Furthermore, it recognizes such factors as the inertia of the machine which may be useful in determining the response wanted or expected on a given machine. To illustrate, a machine that has a large dead band as determined by this method of testing may not be a good machine upon which to apply load control.

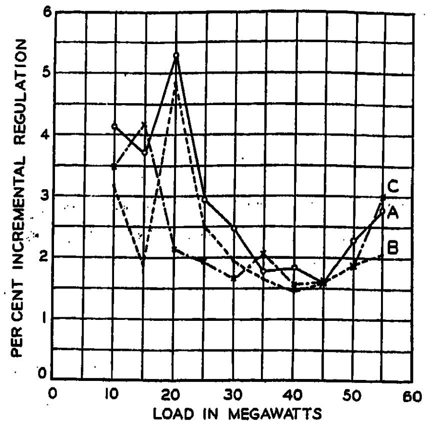


Figure 3. Comparison of three methods of obtaining Incremental Regulation on a 65 megawatt unit with one overload valve

Results of Incremental Regulation Tests

Curve A, Figure 3, shows the results of a test for incremental regulation on a 65,000-kw reheat turbine with one overload valve.

In order to compare this method of testing with other methods of testing, arrangements were made with one of the manufacturers to conduct a test on the above unit. This manufacturer's method, it is believed, determines incremental regulation in accordance with the AIEE specifications, but the equipment does not readily lend itself to quick determinations in the field. The incremental regulation obtained according to this manufacturer's procedure is shown as curve B, Figure 3. Another method of testing for incremental regulation was tried in which valve movement instead of kilowatt change was recorded on the vertical axis, and the kilowatts represented by this change in the valve movement were calculated, using the inlet valve characteristics curve, Figure 6. Curve C, Figure 3, represents incremental regulation as de-

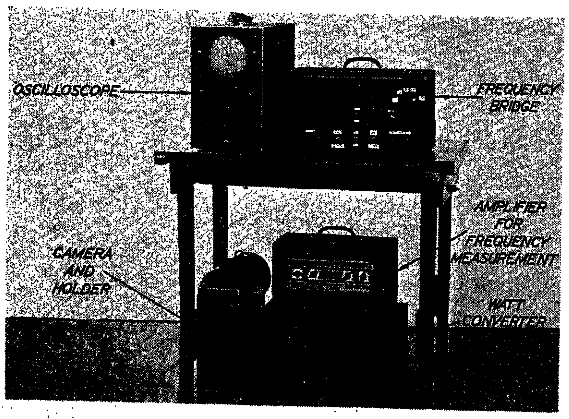
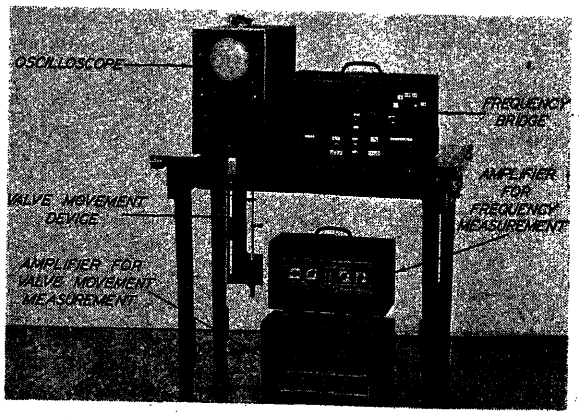


Figure 2 (left). Apparatus for determination of kilowatts change versus frequency change

Figure 4 (right). Apparatus for determination of valve movement versus frequency change


OSCILLOSCOPE
FREQUENCY BRIDGE
VALVE MOVEMENT DEVICE
AMPLIFIER FOR FREQUENCY MEASUREMENT
AMPLIFIER FOR VALVE MOVEMENT MEASUREMENT

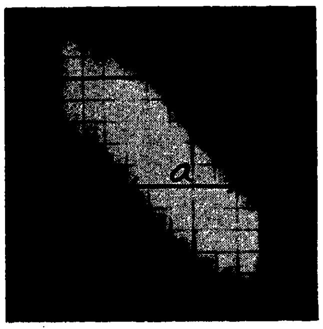


Figure 5. Valve movement versus frequency change with a 45-megawatt load on a 65-megawatt unit

Valve movement on vertical scale is 0.0075 inch per small division and frequency change on horizontal scale is 0.005 cycle per small division

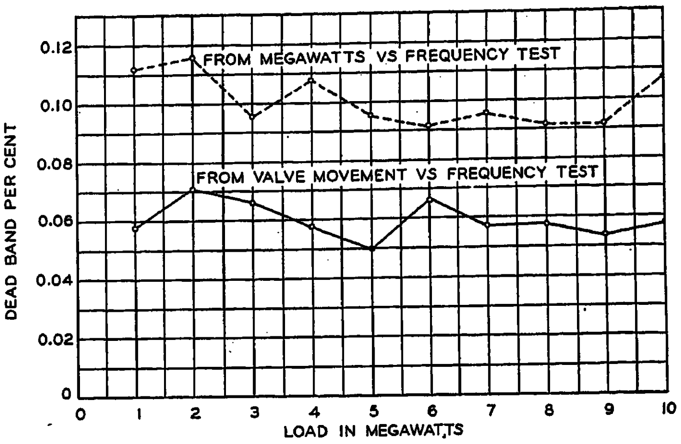


Figure 7. Dead band tests on 10,000-kw unit by kilowatts change versus frequency change method and by valve movement versus frequency change method

termined by this method on the same unit. It will be seen that the three methods of test give reasonably close agreement. The method of making these calculations is shown in Appendix II.

Results of Dead Band Determination

The dead band as determined by frequency change versus kilowatt change is not true dead band as defined in the aforementioned AIEE specifications. These specifications define dead band in terms of valve movement and frequency change. In order to study true dead band, a device as illustrated in Figure 4 was designed. The frequency bridge, amplifier and oscilloscope are the same as previously described. In place of the watt measuring equipment, a sensitive valve movement device was mounted on the inlet steam valve, and this movement electronically amplified so that changes in movement of the inlet valve are applied to the oscilloscope as an electrical quantity. A record obtained with this equipment is shown in Figure 5. Line (*a*) on Figure 5 therefore represents true dead band as interpreted from the AIEE specifications.

Figure 7 shows results of dead band obtained by these two methods. It is felt, however, that the index of dead band obtained by the kilowatt change versus frequency change method, although it cannot be used to determine conformance to purchase specifications, is a valuable index of the performance of the machine under load conditions, and when interpreted as such, and not as true dead band, has considerable value. While some of the tests show almost identical results by the two methods, many of the tests show the differences as indicated in Figure 7.

Difficulties Encountered

One of the difficulties encountered in applying this method of testing is the presence of oscillations in the kilowatt output of the generator having a frequency of about one cycle every 0.8 seconds. These are quite noticeable with the sensitive test apparatus used, and also may require recognition in other problems incident to the operation of a large power system. These oscillations are independent of system frequency changes and seem to be exaggerated by fluctuating loads. Under these conditions it is difficult to obtain a reliable index of performance. In one case, after a large fluctuating load was transferred to another generator, satisfactory results could be obtained. However, the cyclic variations under some load conditions have been sufficient to make determinations difficult and attention now is being directed towards eliminating these fluctuations from affecting the test apparatus. These cyclic changes in kilowatt output are natural phenomena in the operation of a generator on an interconnected system, and the frequency, but not the magnitude, of the fluctuations can be calculated quite readily from the constants of the system.

Future Work

The greatest need is for the development of test apparatus which will satisfactorily cancel out these oscillations. Recent tests show that this can be neutralized in the test circuit by using capacitors in the oscilloscope circuit. This gives excellent results for incremental regulation, but at the expense of the true picture of dead band.

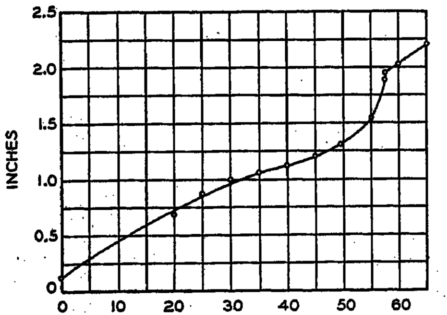


Figure 6. Inlet valve characteristics curve for 65-megawatt unit with one overload valve

Slope at 45 megawatts is 42.6 megawatts per inch of valve movement

Further work is needed to improve the technique of testing with the long persistence screen.

The equipment that is mounted on the inlet valve for determining true dead band also can be moved to other parts of the governor system for tracking down lost motion. It is planned to do some work along this line.

The frequency-sensitive bridge, watt-converter, and amplifiers were assembled or purchased separately and temporary connections used. A more compact and permanently wired instrument should be designed.

Summary

It is believed that there is need for a field method of determining incremental regulation and dead band of steam turbines under actual load conditions. The equipment described appears to be satisfactory for determining incremental regulation according to the AIEE specifications without making attachments to the turbine. Also an index of dead band can be determined by this method. True dead band as defined by the AIEE specifications can be obtained by the use of the auxiliary equipment described. However, this requires the attachment of a device on the inlet valve so that the small movements of the valve can be converted into electrical quantities suitable for the test apparatus.

Appendix I

Method of Test

To obtain the "power output (kilowatts) versus frequency" values, the watt-sensitive or watt-converter device is connected directly into the current and potential metering circuits of the generator under

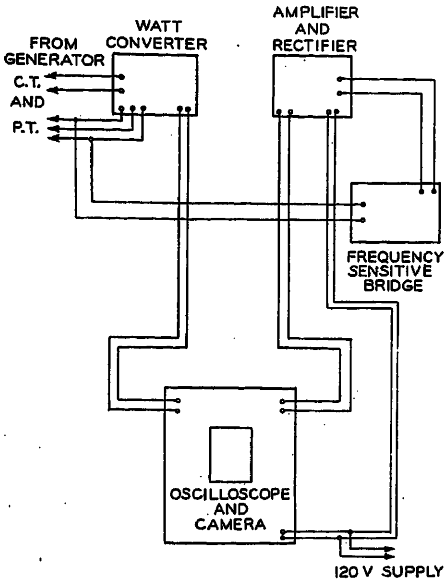


Figure 8. Block diagram of the apparatus used for the determination of kilowatts change versus frequency change

test and the frequency-sensitive bridge device or circuit is connected to the generator potential circuit. The watt-converter converts kilowatt output change directly to a proportional direct voltage change and the inherent characteristic of the converter circuit supplies sufficient amplification so that the direct output voltage can be applied directly to the cathode-ray oscilloscope. The frequency bridge is detuned to provide a polarizing effect so as to differentiate between increasing and decreasing frequency changes. The voltage output thus obtained from the bridge is a very small alternating voltage which is in turn amplified, rectified, and applied to the cathode-ray oscilloscope. Thus, the two corresponding forces proportional to kilowatt change and frequency change are applied to the cathode-ray deflecting plates and the resulting diagram is observed or photographed to obtain a measure of the machine performance.

The "valve movement versus frequency" test, which was mentioned in connection with "check tests" shown in Figure 3, is obtained in a similar manner, except that the watt-converter is replaced by a valve-movement detector. A variable inductance, comprising one leg of an inductive bridge circuit, is coupled mechanically to the main inlet valve or power piston of the turbine. This inductive bridge circuit is unbalanced in a manner somewhat similar to the detuning of the frequency bridge circuit, thereby polarizing the output voltage. Likewise, as with the frequency bridge, the output voltage is amplified and rectified and then applied to the cathode-ray tube. The resulting diagram thus obtained is another measure of the machine performance.

The feasibility of using a cathode-ray tube having a screen with extra long persistence, and thereby eliminating the need for photographing the screen, has proved satisfactory. This eliminates the need for the camera and holder shown in Figure 2, except in cases in which a photographic record is desired.

Experience has shown that a test run of one to three minutes is sufficient to determine the slope of a common major axis required in analysis.

Figure 8 shows a block diagram of the apparatus for measuring watts versus frequency, and the equipment is shown in Figure 2.

Figure 9 shows a block diagram of the apparatus for measuring valve movement versus frequency, and the equipment is shown in Figure 4. If it is necessary to make a photographic record, the camera and holder shown in Figure 2 also would be required.

The size of the equipment shown in Figures 2 and 4 could be reduced considerably if a redesign were made incorporating the experience gained and using parts that now are available but were not at the time of the original design. Such a redesign would probably incorporate the amplifier and frequency bridge of Figure 2 in one cabinet of about the same size and weight as the present frequency bridge. In Figure 4, the valve-movement amplifier should be much smaller, and as mentioned before, the frequency bridge and its amplifier would be combined.

Appendix II

Incremental Regulation

1. From an unpublished AIEE paper Recommended Specifications for Speed Governing of Steam Turbines Intended to Drive Electric Generators Rated 500 KW and Up, dated Nov. 1947, it is determined that, on a 60-cycle system

Steady-state incremental speed regulation in per cent

$$= \frac{\text{cycles change in speed} \times 100}{\text{system frequency}} \div \frac{\text{change in power output}}{\text{rated output}}$$

therefore

$$\text{Per cent IR} = \frac{\Delta \text{ cycles} \times 100}{60} \times \frac{\text{Rated output}}{\Delta \text{ power output}}$$

or

$$\frac{\Delta \text{ cycles} \times 1.67 \times \text{ Rated output}}{\Delta \text{ power output}}$$

Δ power output is obtained by the slope of the line as shown in Figure 1 and is expressed for convenience in terms of megawatts per 0.1 cycle. The formula, therefore, becomes

$$\text{Per cent IR} = \frac{0.1 \times 1.67 \times \text{rated megawatts}}{\Delta \text{ megawatts}} = \frac{\text{rated megawatts} \times 0.167}{\Delta \text{ megawatts}}$$

Accordingly, the per cent *IR* for the test shown in Figure 1 would be

$$\frac{0.167 \times 65}{6.8} = 1.6 \text{ per cent}$$

2. Also Δ megawatts = valve movement in inches per 0.1 cycle (from Figure 5) by megawatts per inch of valve movement (from inlet valve characteristics curve,

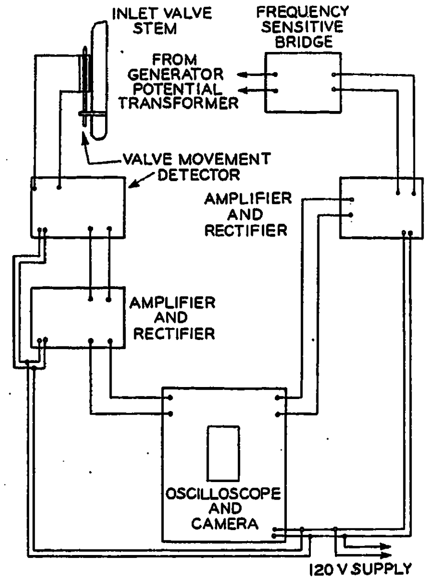


Figure 9. Block diagram of the apparatus used for the determination of valve movement versus frequency change

Figure 6.) Valve movement per 0.1 cycle is proportional to the slope of the parallelogram as shown in Figure 5, therefore, per cent IR = rated megawatts by 0.167 ÷ Δ valve movement by megawatts per inch, from inlet valve curve as per Figure 6. Accordingly, the per cent *IR* for the test shown in Figure 5 would be

$$\frac{0.167 \times 65}{0.16 \times 42.6} = 1.6 \text{ per cent}$$

Dead Band

Dead band is the total magnitude of the sustained speed change within which there is no resulting measurable change in the position of the governor-controlled valves. Dead band is a measure of the insensitivity of the speed-governing system and is expressed in per cent of rated speed.

Therefore

Per cent dead band on a 60-cycle system

$$= \frac{\text{cycles of no movement} \times 100}{60}$$

The total magnitude of sustained speed change within which there is no change in valve position is considered to be the change in frequency represented by the line (*a*) in Figure 1 which is 6.0 divisions or 6 by 0.005 cycles or 0.030 cycles.

Accordingly

Per cent index of dead band would be

$$\frac{0.03 \times 100}{60} = 0.05 \text{ per cent}$$

Similarly, dead band can be determined from valve movement versus frequency change, as shown in Figure 5, the line (*a*) represents five divisions, or 5.0 by 0.005 cycles or 0.0250 cycles. Accordingly

$$\text{Per cent dead band would be } \frac{0.0250 \times 100}{60} = 0.0416 \text{ per cent}$$

References

1. Speed-Governing of Steam Turbines Intended to Drive Electric Generators Rated 500 Kw and Up. *AIEE Standard Number 600*, May 1949.

2. Oscillograph Analyzes Governor Performance, J. E. Allen. *Power* (New York, N. Y.) volume 78, November 1934, pages 610–12.

3. Prime-Mover Speed Governors for Interconnected Systems, R. J. Caughey, J. B. McClure. *AIEE Transactions*, volume 60, 1941, pages 147–51. Discussion by J. E. Allen, pages 729–30.

4. A Turbine-Governor Performance Analyzer, W. O. Osbon. *Electrical Engineering* (*AIEE Transactions*) volume 60, November 1941, pages 963–67. Discussion, R. Sheppard, C. Concordia. *AIEE Transactions*, volume 61, 1942, pages 393-94.

5. Effect of Prime-Mover Speed Governor Characteristics on Power-System Frequency Variations and Tie-Line Power Swings, C. Concordia, S. B. Crary, E. E. Parker. *AIEE Transactions*, volume 60, 1941, pages 559–67.

6. Prime-Mover Speed Governors for Interconnected Systems, R. J. Caughey, J. B. McClure. *AIEE Transactions*, volume 60, April 1941, pages 147–50.

7. Power System Governing—The Problem, J. J. Dougherty, A. P. Hayward, A. C. Monteith, S. B. Griscom. *AIEE Transactions*, volume 60, 1941, pages 547–58.

8. Testing Governor Performance on Electric Power Systems with Improved Instruments, J. E. Allen, W. B. Hess. *AIEE Transactions*, volume 66, 1947, pages 329–39.

9. Methods of System Control in a Large Interconnection, Earle Wild. *AIEE Transactions*, volume 60, May 1941, pages 232–36. Discussion pages 737–39.

10. Steam Turbine Governors, R. J. Caughey. *Transactions*, American Society of Mechanical Engineers (New York, N. Y.) volume 62, April 1940, pages 191–98.

11. Constant System Speed and the Steam-Turbine Governor, A. F. Schwendner. *Transactions*, American Society of Mechanical Engineers (New York, N. Y.) volume 62, April 1940, pages 199–206.

12. The Field of System Governing, Albion Davis. *Transactions*, American Society of Mechanical Engineers (New York, N. Y.) volume 62, April 1940, pages 207–19.

No Discussion

The Performance of a Topped Steam Power Plant on Sudden Load Reduction

JOHN G. NOEST
MEMBER AIEE

AT THE present stage of the development of the system of the Consolidated Edison Company of New York, one of the company's power generating stations, namely the Sherman Creek Station, with a generating capacity considerably in excess of its local bus load, depends upon a single high-capacity tie feeder for the purpose of interconnecting it with the remaining system. The utilization of the major portion of the plant's capacity, therefore, depends on this tie feeder carrying the difference between the plant capacity and the local bus load. At certain times, the local plant load may be as little as 10 per cent of generation.

This situation exposes the plant to unusual operating conditions in that the trip-out of the single tie feeder will result in a sudden, severe unloading of the plant.

To discover whether or not this condition involved particular hazards to the continuity of service to the local consumer load and to plant equipment, as well as to formulate operating instructions for the guidance of the plant operating personnel, a study was undertaken to disclose the performance of the component parts of the plant when the plant was subjected to a sudden reduction of its load. This study was aimed particularly towards the determination of the performance of the local electric system, the turbine plant, and the steam generation equipment. This study was later expanded to include actual tests, involving the dropping of various percentages of the plant generation, and it includes the experience gained during unstaged, accidental trip-out of the tie feeder.

Description of the Plant and Its Interconnections

Prior to the year 1943, when the first of the present two topping units was being installed, the Sherman Creek Station had been developed to a point where it had eight condensing turbogenerators, varying in size from 15 to 30 megawatts, with a total capacity of 171 megawatts. The steam generating equipment consisted of 50 boilers, which were initially provided with stokers and some of which were later converted to oil burning. The steam pressure was 185 pounds per square inch gauge and the steam temperature was 500 degrees Fahrenheit. The steam conditions after the installation of the topping units remained, of course, the same as far as the eight low-pressure units, all of which are still in service, are concerned.

At that time, the eight generators were connected to a 13-kv bus of four sections and there were six 13-kv ties to the Company's Hell Gate Station, with a total capacity of 60 megawatts; there were two 45-kv ties, with a total capacity of 42 megawatts to the system via Laconia Avenue Substation, which still exist, but which now are operated from an isolated bus section at Sherman Creek; and there was and still is another 13-kv 8-megawatt tie to the system via Columbus Avenue Substation. The total tie capacity was therefore 110 megawatts and the locally distributed peak load was 80 megawatts.

As mentioned before, the first topping turbogenerator unit, rated 50 megawatts, at 0.8 power factor, was installed in 1943. By that time the two end bus sections had been modernized by the installation of metal-clad phase-isolated busses with modern switchgear. The tie-feeder situation remained virtually unchanged. By this installation the station capacity was increased from 171 to 221 megawatts.

The steam generating equipment for the first topping turbogenerator consists of a Combustion Engineering Company pulverized coal-fired wet-bottom boiler with a capacity of 1,000,000 pounds per hour at 950 degrees Fahrenheit. The furnace is equipped with gas ignition torches, operable from the control board. Automatic combustion control and boiler water level control are provided. The boiler feed pumps and fans are turbine-driven. In 1947 the second topping turbogenerator was installed and the 138-kv tie feeder, connected at the Dunwoodie Substation to an existing tie from Hell Gate to the Niagara-Hudson System, was established. This turbogenerator also is 50 megawatts, 0.8 power factor, and its installation added another 50 megawatts to the existing station capacity of 221 megawatts, giving a new total of 271 megawatts.

The steam generation equipment for the second topper is a duplicate of that provided for the first, except for the provision for pulverized coal and oil firing for load carrying, and for oil ignition using steam atomizing burners, and the bifurcation of the lower coal burner to provide better air-coal mixing to minimize sulphiding. The oil ignition torches must be lit manually at the burners.

To provide space for these installations, 18 of the old boilers were removed, leaving 32, with a total capacity of 190,000 pounds per hour.

With the installation of the new 138-kv tie, which has a capacity of 179 megavolt-amperes, the then existing six Hell Gate ties had to be discontinued since they could not be operated in parallel with the new 138-kv tie at high loads.

Paper 49-291, recommended by the AIEE Power Generation Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted July 20, 1949; made available for printing September 2, 1949.

JOHN G. NOEST is Division Engineer in the Electrical Engineering Department of the Consolidated Edison Company of New York, Inc., New York, N. Y.

The author wishes to acknowledge the valuable contributions made to this paper by W. J. Roberts and R. W. Clement, of the Consolidated Edison Company of New York.

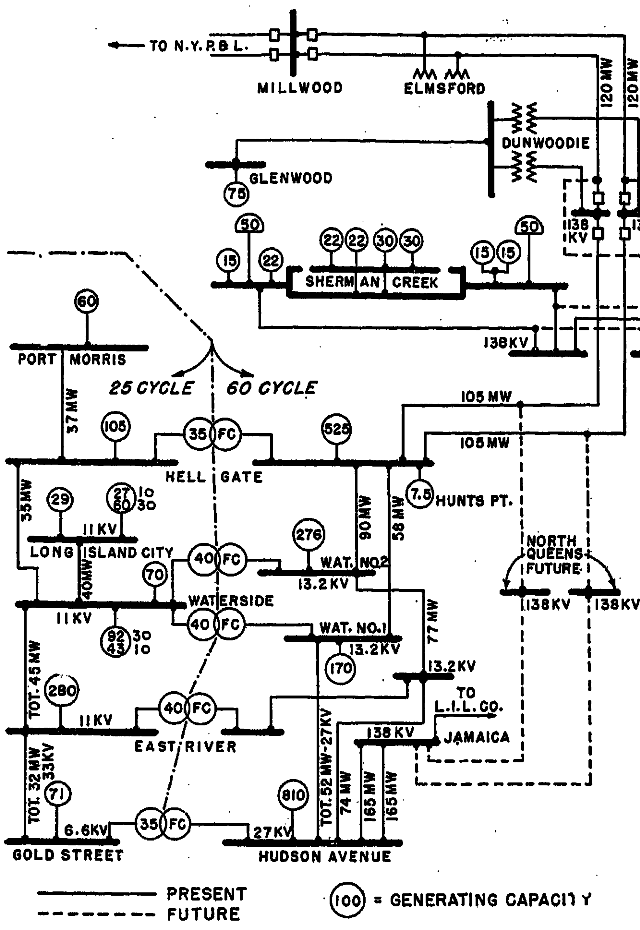


Figure 1. Diagram of the Consolidated Edison Company's Metropolitan System

The presently existing interconnection of the Sherman Creek Station with the remainder of the Company's system is shown on Figure 1, which also shows the planned future development. Figure 2 shows the present bus arrangement at the Sherman Creek Station together with planned future expansion. Figure 3 shows schematically the connections of the steam equipment.

The generating capacity versus the station bus load and tie-feeder capacity situation at the Sherman Creek Station is summarized in Table I. This tabulation shows that the Sherman Creek Station may be exposed to a sudden load reduction of up to 179 megawatts on trip-out of the 138-kv tie feeder at times when the station local bus load may be at any value between 20 and 100 megawatts.

The Operating Problem

Under normal operating conditions the load, served from the Sherman Creek Station, is essentially of the metropolitan type in that load changes are small in amount and of slow nature. For this reason it has never been found necessary to use automatic voltage regulation on any of the generators. Automatic load-frequency regulation is available for the purpose of maintaining the tie-feeder load.

Since the Sherman Creek Station is located on the northern end of the metropolitan system of the company, where, because of the available charging kilovolt-amperes from the capacitances of the 138-kv ties, of which the Sherman Creek-Dunwoodie tie is one, (the Hell Gate-Dunwoodie and the Sherman Creek-Dunwoodie ties being cable circuits) and where, because of the small local load, the reactive kilovolt-ampere requirements are small, this station normally operates with power factors ranging between 0.90 to 0.95.

The operating problem, which presents itself on sudden load reduction because of the trip-out of the 138-kv tie feeder, may be resolved into three major phenomena, which are, of course, interrelated:

1. The effect of the voltage and frequency rise upon the remaining consumer load.
2. The effect upon the low-pressure steam header pressure, to which the major boiler auxiliaries, such as the induced and forced draft fan and the boiler feed pumps, all of which are turbine-driven, are connected.
3. The effect on the high-pressure boilers with respect to water level and the maintenance of ignition.

Within these three divisions there is a question not only in relation to the performance of equipment but in relation to the performance of the operating personnel as well.

The Theoretical Investigation

Beginning with the effect of voltage rise on the remaining consumer load, there is the question of magnitude and duration. Since automatic voltage regulation is not available in this station, the duration of overvoltage depends largely upon the time it will take the operators to

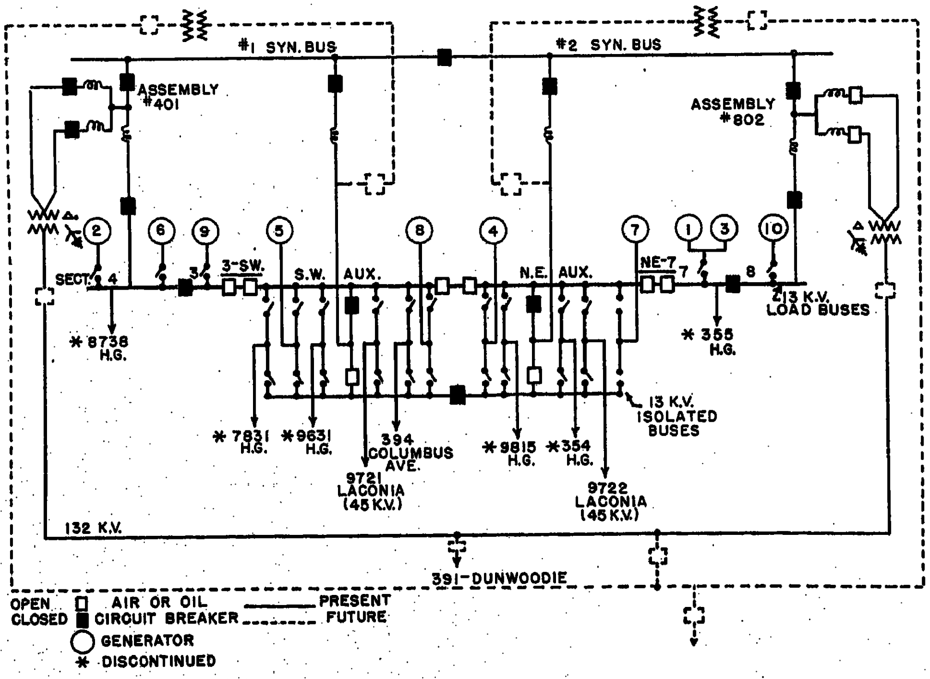


Figure 2. Diagram of the electric connections of Sherman Creek Station

Table I

Generating capacity	271 megawatts
Local bus load, incl. the Laconia Ave & Columbus Ave ties	190 megawatts peak 20 megawatts off-peak
Dunwoodie 138-kv tie capacity	179 megawatts

restore conditions to normal. In the theoretical investigation it therefore is assumed that the activities of the operators will have no effect upon the maximum voltage which is reached in a few seconds. In effect, therefore, it is assumed that the voltage will have reached steady-state conditions before the operator is able to intervene.

The factors, which affect the voltage rise, are:

1. Initial conditions, that is: tie-line load, bus load, power factor, and generator capacities.
2. The effect of the change of load and power factor on the generator voltage at constant speed.
3. The effect of the change of load on the frequency or the turbine speed, which in turn is a function of the behavior of the turbine governors, and which affects voltage cumulatively because of the action of pilot and main exciters with increasing speed.

Although the calculation of the first two factors is simple and straightforward, difficulty was experienced with the third, not so much because of the difficulties in the basic approach as because of the difficulty in obtaining the necessary governor constants. Appendix I shows the methods used in the calculation of the above three factors. The result of these calculations is given in Table II.

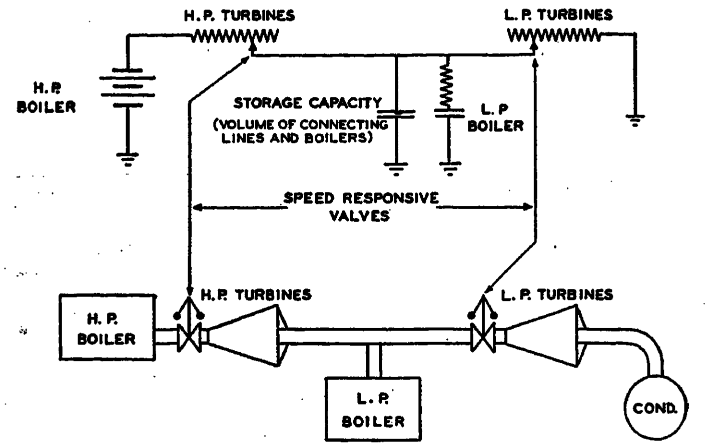


Figure 4. Electric analogue of steam flow

In the calculation of factor 2, the effect of load and power factor change on the generator voltage, there are two different conditions, which can be discerned by inspection of Figures 1 and 2. These two conditions are tripping of the 13-kv circuit breakers on the low-tension side of the tie-feeder transformers at Sherman Creek Station, and tripping of the Millwood Substation and the Dunwoodie Substation tie-feeder circuit breakers without simultaneous tripping of the Sherman Creek end, in which case the tie feeder with its capacitance charging current of 30 megavolt-amperes at rated voltage would remain connected to the Sherman Creek Station bus. Obviously, as far as voltage rise is concerned, the latter situation would represent the most severe case. However, since this case cannot arise under fault conditions without the simultaneous failure of the Sherman Creek Station, tie-feeder circuit breaker operating mechanisms or their control circuits, the probability of it happening is negligible. Under normal conditions, the tie-feeder capacitance could be left on the Sherman Creek bus only by the erroneous operation of three circuit breakers at different locations; this is an operation which is equally unlikely. For these reasons the probability of such an operation is insignificant and was not considered in these investigations.

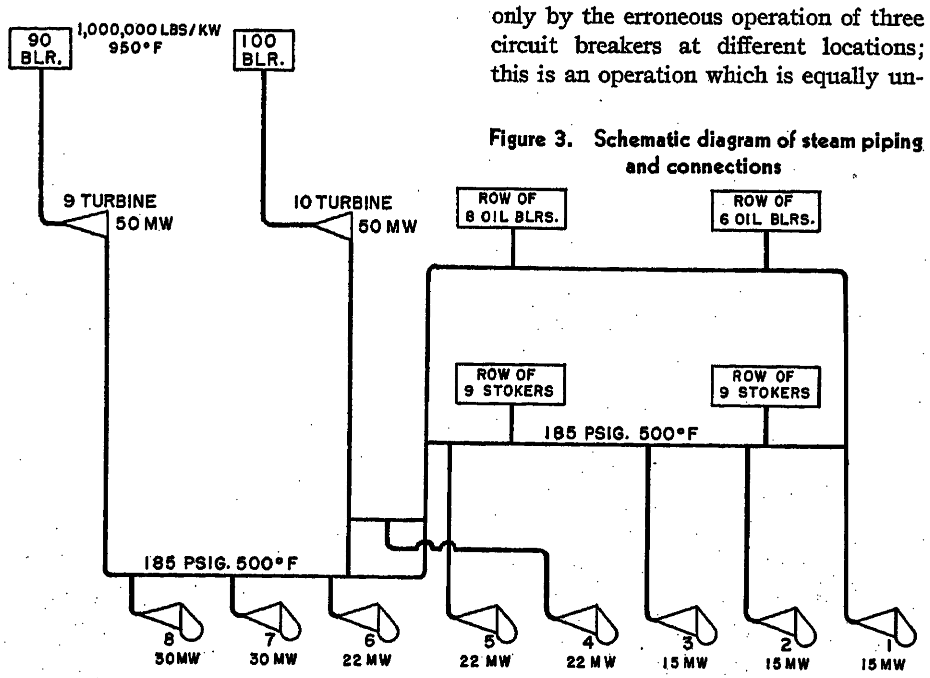


Figure 3. Schematic diagram of steam piping and connections

In the consideration of the voltage rise problem, the effect of the voltage rise upon the remaining consumer load was included by assuming the load to have constant impedance. From checks of data available from tests made previously on parts of the Company's system and from comparison with data in the technical literature[1,2,3] it was concluded that the error thus made was small in the expected voltage range. However, no consideration was given to the possible reduction of the consumer load because of lamp burnout, since it was known from previous experience that a voltage rise up to 25 per cent is acceptable from this standpoint, provided that the duration of such a rise does not exceed one minute.

In the study of the second part of the operating problem, namely the effect of sudden load reduction upon the behavior of the low-pressure steam header pressure, there also are a number of factors to be considered. These are:

1. The performance of the high-pressure and low-pressure turbine governors.
2. The amount of low-pressure boiler capacity and the amount of steam storage capacity, represented by banked boilers and by the volume of the steam piping.
3. The time limit imposed on the operators by the restrictions in motoring time on the topping turbines.

The data available for the study of the first of the above listed factors was meager indeed. Although the regulation at steady state was known for the different governors, there was no information available on the incremental regulation, nor on the time constants involved in the problem. Because of this lack of definite information, assumptions were made as follows:

1. That the steady-state regulation curves

Table II. Calculated Performance

		Nominal Load Drop in Megawatts 50	100	170
Assumed conditions, adjustments*		None	None	None
Total load	Megawatts	76	122	220
	Megavars	43	60	107
Tie-line load	Megawatts	51	100	170
	Megavars	24	43	70
Bus load	Megawatts	25	22	50
	Megavars	19	17	37
Generating capacity on bus, megavolt-amperes	Initial	133	220	254
	Final	133	220	254
Bus voltage, kilovolts	Initial	13.5	13.6	13.6
	Maximum	15.6	15.9	17.1
	Final**	15.4	15.9	17.1
Frequency, cycles per second	Initial	60	60	60
	Maximum	62.7	63.3	63.3
	Final**	61.2	61.5	61.9
Low-pressure header pressure, pounds per square inch gauge	Initial	200	202	200
	Final**	216	196	—

* No manual adjustments were assumed to be made for a 15-second period following the trip-out.

** At 15 seconds.

are combinations of straight lines within the range involved, and that these curves would give the ultimate speed.

2. That governors, having reasonably equal regulation and time constants, may be lumped together and represented by an equivalent governor.

3. That for the low-pressure turbine governors a maximum valve gear travel time could be assigned, and that the valve gear travel speed would be proportional to the instantaneous difference between the actual speed at the moment and the speed called for by the regulation curves for the respective units.

4. That a linear relation exists between the output of the generator and the position of the valve gear.

For the determination of the behavior of the low-pressure steam header pressure, the system can be represented by an electric circuit as shown in Figure 4. From this representation it can be seen that the two regulating devices, representing the turbine governors, are not independent so far as the regulation of the steam flow is concerned. They do, however, independently affect the low-pressure header pressure.

Using the method outlined in Appendix I, the set of curves shown in Figure 5, as well as the data given in Table II, were derived.

From this study the following conclusions can be reached:

1. With the usual power factor of the load on the tie feeder of 0.95 or higher, the voltage rise would not exceed 25 per cent.

2. For the maximum load reduction by tie-feeder trip-out, the frequency rise would not exceed 6 per cent.

3. The faster governors of the topping turbogenerators are primarily effective in limiting the frequency or speed rise.

4. The behavior of the low-pressure steam header pressure is determined by the incremental regulations of the two groups of governors, which also determine the load division, and by initial loading.

5. Low-pressure steam storage helps in reducing the rate at which the pressure in the low-pressure header changes.

6. The ratio of available low-pressure steam generation to the remaining bus load determines the extent to which the topping units will tend to motor.

7. Judicious removal of excess generating capacity is the quickest way for the restoration of normal conditions.

With the data obtained from these theoretical investigations on hand, the boiler operating problem was discussed. The outcome of these discussions is summarized below:

If on trip-out at high loads the operator promptly removes one of the high-pressure generators together with the proper number of low-pressure generators, the low-pressure header pressure can be kept within reasonable limits so that the boiler feed pumps will be available to prevent

Figure 5. Comparison of theoretical computations with test results—50-megawatt load drop

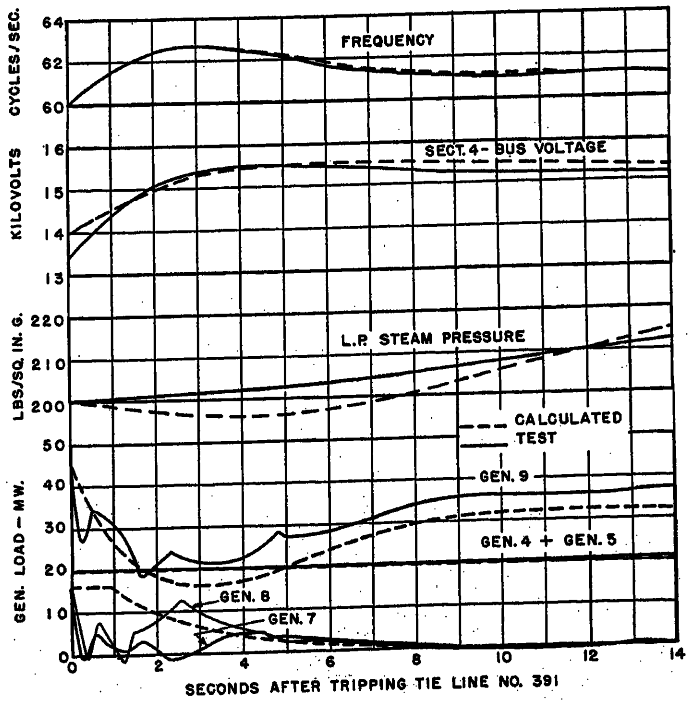
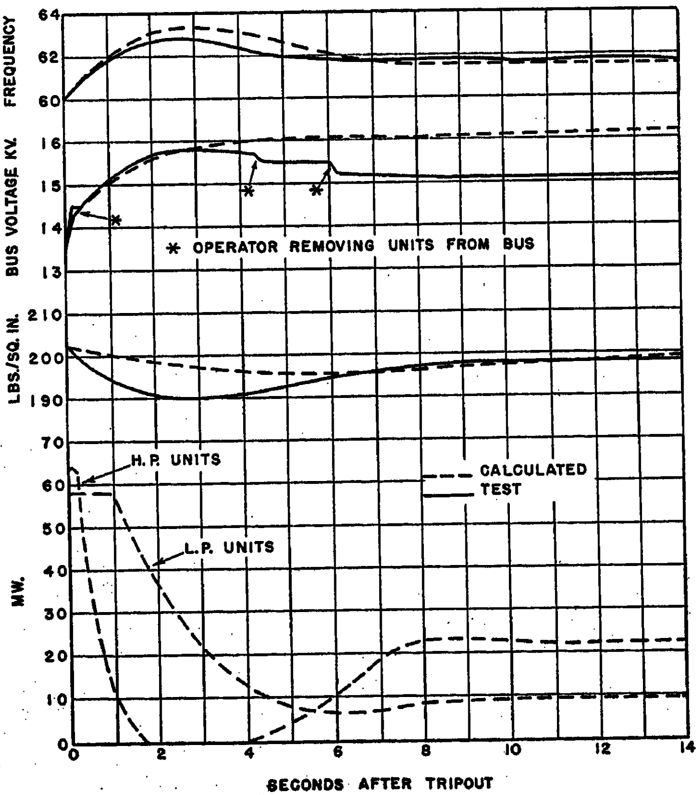


Figure 6. Comparison of theoretical computations with test results—100-megawatt load drop



dangerously low-water levels in the high-pressure boilers, while these boilers are blowing off steam through their safety valves. Since number 90 boiler has gas ignition, it should be removed rather than the number 100 boiler (number 100 boiler is being changed to gas ignition).

The prompt removal of excess generating capacity also will reduce the tendency toward motoring of the remaining high-pressure unit, thereby avoiding the need to shut it down when the permissible motoring time of 30 seconds on one, 45 seconds on the other, has expired. By avoiding shutdown, because of motoring, the boiler operation problem was considered well within the capability of the operating personnel and no damage to station equipment was anticipated.

Tests and Actual Unstaged Trip-outs

Because the investigation discussed previously shows that the Sherman Creek Station would be able to ride through the most severe sudden load reduction without damage to company or consumer equipment, provided that the operators act quickly and correctly, it was decided to stage tests. Such tests would provide the means for checking the method of calculation and the correctness of the assumptions as well as give valuable training to the operators. In accordance with the foregoing, arrangements were made for two tests the first involving a load drop of 50 megawatts, and the second, 100 megawatts, that is 52 per cent and 60.5 per cent of the initial station load, respectively.

The first test was made in January 1948 and the second was made in March. However, on February 4, 1948, there occurred an accidental trip-out of the tie feeder giving a load drop of 170 megawatts. The results of the tests, as well as those of the accidental trip-out, are shown in Table III. It should be noted that in the accidental trip-out, a complete shut-down of the local load was avoided only because of the availability of low-pressure steam generation at the time. Both topping turbogenerators had to be removed from the bus because of motoring. Number 9 unit motored because of a faulty throttle valve, which closed and could not be reopened with full steam pressure. Number 10 unit motored because its valves closed due to an incorrect adjustment of the overspeed device. These conditions have since been repaired.

By comparing Tables II, III and from inspection of Figures 5 and 6, respectively, it can be seen that the theoretical method employed in these investigations can closely predict the actual performance.

In respect to boiler operation during both staged tests for which the operating personnel was fully prepared and armed with detailed instructions, all equipment functioned correctly and the operators easily controlled all essential boiler operating functions without difficulty. Prolonged motoring of the high-pressure turbines was avoided and there was no trouble with the maintenance of the boiler drum water levels.

However, the unstaged trip-out of February 4, 1948, because of the closing of the turbine valves on both topping units, created additional problems.

Because of the loss of both topping units, the electrical operator did not trip low-pressure units, but tried to maintain the remaining station load with them. In so doing, he caused the low-pressure header pressure to drop to 145 pounds per square inch gauge, which, in turn, resulted in water carry-over and, because of the sudden chilling, leakage in all flanged joints in the low-pressure piping system.

Delayed by having to manually light the oil ignition on the boiler, it was ten minutes before number 10 could be synchronized, whereas, if remote controlled gas ignition had been available, number 10 could have been back on load in one or two minutes.

Anticipating the momentary opening of number 9 throttle, the number 90 boiler operator cut in the gas ignition and held a mill in service for one minute. During this period, the steam pressure rose to 1,950 pounds per square inch and was still climbing when the ignition and mill were cut off. At this high pressure, much steam escaped through the safety valve expansion slip joints and clouded the entire area, rendering the boiler water columns invisible. However, the drop in drum water level was held to 13 inches by manual operation of the boiler feed pumps. (The safety valve piping joints have since been modified to prevent a recurrence of the condition.) As number 9 throttle had closed due to unbalanced pressures, the boiler pressure had to be reduced materially before the valve could be reopened. As a result, it was 17 minutes before number 9 unit was returned to service.

The exact sequence of events and operations was made more difficult to reconstruct as some of the instruments did not record rapidly enough. These were principally the multipoint recording type. Some of the instruments were not set exactly on the correct time. Some of them stopped at the crucial moment due to blown fuses following overvoltage at the time of trip-out. The operators were busy and many of the indicating instruments went unread and unlogged.

Automatic Devices for Alleviating the Problem

During one stage of the investigation, consideration was being given to the installation of various automatic devices for assisting the operators in controlling the voltage rise and for controlling the low-pressure header pressure.

For the control of overvoltages, consideration was given to the installation of standard voltage-regulating equipment on all generators. This would have involved the installation of new exciter field rheostats on all but the two topping units because the existing rheostats were not suit-

Table III. Actual Performance

Nature of trip-out		Nominal Load Drop in Megawatts 50 Staged Test, No Adjustments for 15 Seconds	100 Staged Test, Immediate Manual Adjustments	170 Accidental Relay Operation
Total load	Megawatts	76	122	220
	Megavars	43	60	107
Tie-line load	Megawatts	51	100	170
	Megavars	24	43	70
Bus load	Megawatts	25	22	50
	Megavars	19	17	37
Generating capacity, megavolt-amperes	Initial	133	220	254
	Final	133	98	129
Bus voltage, kilovolts	Initial	13.5	13.6	13.6
	Maximum	15.5	15.6	16.9**
	Final*	15.2	15.0	
Frequency, cycles per second	Initial	60	60.0	60
	Maximum	62.7	62.8	
	Final*	61.2	61.6	
Low-pressure header pressure, pounds per square inch gauge	Initial	200	202	200
	Final*	212	198	

* Final means at end of 15-second period.

** Maximum up to point where potential transformer fuse blew.

able for use with automatic voltage-regulating equipment. The costs of such an installation would have had to be justified purely on the basis of alleviating a rather infrequently expected emergency condition.

As a cheaper means of reducing voltage rise, a scheme, using voltage relays, set above the normal operating voltage, to lower the exciter field rheostats to a preset position, was investigated. This would have obviated the need to purchase new field rheostats. This scheme was abandoned because of the possibility of creating instability by depriving a loaded generator of a sufficiently high field excitation to assure maintenance of synchronism.

For the purpose of maintaining low-pressure header pressure and to prevent prolonged motoring of the remaining high-pressure turbine, an automatic scheme for tripping excess generator capacity was considered. With this scheme, the operator would preselect the units he would want quickly removed from the bus in case of a trip-out of the tie feeder. A voltage relay, set to a certain overvoltage, would then trip these preselected units. Because of the operator's understandable aversion to a scheme which would automatically remove the generating capacity several units in case of failure of a single device, such as a relay, this scheme was abandoned.

No additional automatic devices were ever considered for application to the boilers; in fact, the operators invariably resort to manual control of the boiler in all emergencies.

In view of the tests, and on the basis of actual unstaged trip-out experience of which, to date, five cases have occurred, a final decision was made not to provide automatic devices specifically for the trip-out problem.

Operating Guides

From the wider understanding of the problem, provided by the theoretical investigation and from the experience, gained by the operators through tests and unstaged trip-outs, the following operating guides were derived:

For the Electrical Operator

He must be prepared for a sudden load reduction, equal to the load carried by the 138-kv tie feeder, under all station load conditions and at all times. Depending on the load conditions, he must know whether or not generating units have to be dropped, and if so, he must have decided which units. The electrical operator's plan for action must be agreed on by the turbine and the boiler operators, and all three must be kept informed of changing conditions and plans.

Depending on load conditions, the electrical operator is to proceed as follows on sudden loss of load:

Trip off the preselected units promptly.

Reduce frequency further, if required, by reducing the load on the heaviest loaded remaining units.

Reduce voltage if required, by reducing the excitation, beginning with the lightest loaded units, and taking care to maintain lagging power factor.

In reducing load, be guided by the low-pressure header pressure and by motoring limitations on the higher pressure generators.

For the Turbine Operator

Secure all turbines, tripped by the electrical operator and prepare them for resynchronizing when required.

For the Boiler Operator

Switch to manual combustion and feed water control.

If a high-pressure turbine has been tripped, secure associated boiler in a normal manner.

On the remaining high-pressure boiler, keep a heavy coal feed on the bottom burners to prevent loss of ignition. Cut in the ignition torches immediately. Should the drum water level drop below the danger point, cut off all coal feed.

Cut in low-pressure boilers and force these to steaming in accordance with the prearranged program.

Conclusion

Theoretical investigation, staged tests, and actual case experiences show that a topping turbine steam generating station can successfully operate through sudden load reductions up to 90 per cent of initial load without damage to plant or remaining consumer load equipment. The complete understanding of the problem by the operators, together with preparedness and co-operation on their part, is a prerequisite for the successful handling of such an emergency.

The experience, gained through tests and through actual cases, shows that even with unforeseen complications, the required performance lies well within the capability of properly trained operating personnel.

Appendix I

The following describes the methods used in calculating the electrical and mechanical performance of turbogenerators when subjected to sudden reduction of load. These methods were developed to enable the prediction by relatively simple calculations of voltage and frequency rises, as well as of the unbalance in steam flows.

Step-by-step calculations were found to be most suitable because of the large number of variables involved. The calculations were made for the periods up to 15 seconds following the load drop, by which time equilibrium conditions are generally reached (except for the low-pressure boilers).

All generators on the load busses were assumed to be combined in an equivalent generator, having the adjusted synchronous and transient reactance, and the adjusted transient short-circuit time constant, of the paralleled units. The inertia of this equivalent generator was taken as equal to the sum of the inertias of the rotating assemblies of the paralleled units.

When only a few units are involved, the output of each turbine may be considered separately and derived from its governor characteristic curve. When many units are involved, a simplification may be achieved by use of an equivalent high-pressure turbine and an equivalent low-pressure turbine to combine the characteristics of all the high- and low-pressure turbines, respectively. The turbine output, derived from the governor characteristic curve, is the steady-state condition, which must be modified for the travel time and dead time of the governors for the transient condition.

In calculating the generator behavior, the d-c and subtransient components were neglected as is done usually in cases involving load impedances.

Although the electrical and the mechanical factors of the problem are not mutually independent, the following division establishes the primary factors of each group:

1. Voltage Rise
   - A. At constant speed.
   - B. Effect of the speed variations.
2. Speed Variation
   - A. Governor characteristics.
   - B. Computations.
3. Steam Pressure
   - A. Effect of the volume of the steam piping and effect of low-pressure steam generation.

1 (A). Voltage Rise at Constant Speed

The basic equations are

$$\mathrm{e}=\mathbf{E}_{Tb}+\mathbf{I}_b\mathbf{X}_{eg}$$

$$\mathbf{E}_{Ta}=\mathrm{e}\left[\frac{Z_L}{Z_L+\mathbf{X}_{eg}}\right]$$

$$\mathbf{I}_{La}=\frac{\mathbf{E}_{Ta}}{Z_L}$$

The reduction in load is followed by an almost instantaneous rise in voltage due to the smaller leakage reactance drop; thus

$$\mathrm{e}_b'=\mathbf{E}_{Tb}+\mathbf{I}_b\mathbf{X}_e$$

prior to the load drop and immediately thereafter

$$\mathbf{E}_{Ta}'=\mathrm{e}_b'-\mathbf{I}_a\mathbf{X}_e=\mathrm{e}_b'\left[\frac{Z_L}{Z_L+\mathbf{X}_e}\right]$$

The time constant of the equivalent generator was computed from the weighted time constants of the paralleled units and modified for the external impedance in the usual manner.

$$T_{de}'=\left[\frac{X_d'+X_e}{X_d+X_e}\right]T_{do}'$$

Table IV. Sample Calculation, 50-Megawatt Load Drop Test

Time-Seconds	Bus Kv		Megawatts			Degrees Per Second Squared	Δt	Incremental Degrees Per Second $\Delta\omega$	Cumulative Degrees Per Second ω	N	Valve Position						Steam MW		
	From Curve	Corresponding for Speed	Input	Load	Accel.						Unit	Initial	Corresponding to Speed	Final	MW		High-Pressure Units	Low-Pressure Units	Unbalance
Before Trip = 0	13.5	13.5	76	76	0	0		0	0	1.000	9	0.88	0.88	0.88	44	=76	44	32+20*	0
											8	0.50	0.50	0.50	15				
											7	0.57	0.57	0.57	17				
0–0.1	14.0	14.0	76	27	49	720	0.1	72	72	1.003	9	0.88		0.88	44	=76	44	52	0
											8	0.50		0.50	15				
											7	0.57		0.57	17				
0.1–0.2	14.01	14.05	76	27	49	720	0.1	72	144	1.006	9	0.88	0.80	0.80	40	=72	40	52	−4
											8	0.50		0.50	15				
											7	0.57		0.57	17				
0.2–0.4																			

* Includes units 4 and 5 on isolated bus.

Unit Number	Rating		M	Governor		Rate
	Megavolt-Amperes	Megawatts		Dead Time	Travel Time	
9	62.5	50	3.7	0.1	1.0	Uniform
8	35.3	30	5.5	1.0	2.5	Proportional to
7	35.3	30	5.5	1.0	2.5	Proportional to

Total M 14.7 seconds per 100 megavolt-amperes

$$K=\frac{360f}{M(MVA)}=\frac{360\times 60}{14.7\times 100}=\frac{1}{0.068}$$

$\alpha = K.\Delta MW$
$\Delta\omega = \alpha.\Delta t$

$N = 1 + w/21{,}600$
M = inertia constant

$$=\frac{0.462(WR^2)(RPM)^2}{100}10^{-6}$$

This time constant was of the order of six seconds for most calculations and was not critical in affecting the results. The range of correct application of this equation is questionable in view of the large resistance component of the load. However, the difference between T_{de}' and T_{do}' was found to be small.

Using the preceding time constant, the bus voltage at any time after the load drop is

$$E_T = E_{Ta} + (E_{Ta}' - E_{Ta})\epsilon^{\left(-\frac{t}{T_{do}'}\right)}$$

[constant speed]

It will be recognized that this treatment assumes all the paralleled units to have the same internal angles as well as the same terminal voltages. This is approximately true prior to the load drop, when the units operate at comparable per-unit loads, while having similar per-unit reactances. However, following the load drop, the regulation of the governors may result in some machines motoring (even in the steady state) and the internal voltages of the different units will be too far out of phase to yield sufficiently accurate results. Therefore a second, more accurate method is given. This method may be used with almost equal facility where the number of units is not too great.

The vector diagram of Figure 7 shows the internal voltages prior to the load drop while Figure 8 gives the vector diagram following the load drop. From these diagrams the following equations are derived

$$h_1 - E_{Ta} = I_{a1}X_1 \sin\theta_{a1}$$
$$h_2 - E_{Ta} = I_{a2}X_2 \sin\theta_{a2}$$
$$h_n - E_{Ta} = I_{an}X_n \sin\theta_{an}$$
$$I_L \sin\theta_L = I_{a1}\sin\theta_{a1} + I_{a2}\sin\theta_{a2} + I_{an}X \sin\theta_{an}$$

Substituting and reducing

$$E_{Ta} = \frac{\frac{h_1}{X_1}+\frac{h_2}{X_2}+ --- +\frac{h_n}{X_n} - I_L\sin\theta_L}{\frac{1}{X_1}+\frac{1}{X_2}+ --- \frac{1}{X_n}}$$

Since

$$I_L \sin\theta_L = \frac{E_{Ta}}{Z_L}\cdot\frac{X_L}{Z_L} = E_{Ta}b_L$$

$$E_{Ta} = \frac{\frac{h_1}{X_1}+\frac{h_2}{X_2}+ ---- +\frac{h_n}{X_n}}{\frac{1}{X_1}+\frac{1}{X_2}+ ---- \frac{1}{X_n}+b_L}$$

The values of K_n are proportional to $I_nX_n \cos\theta_{an}$ and hence to the load carried by these units in the steady state following the load drop, and they may be determined from the governor characteristic curves and the approximate loads.

The advantage of deriving E_{Ta} in this form lies in the resulting good accuracy because the measured quantities (h_n) are in phase with the voltage and therefore little affected by errors in approximating the loads.

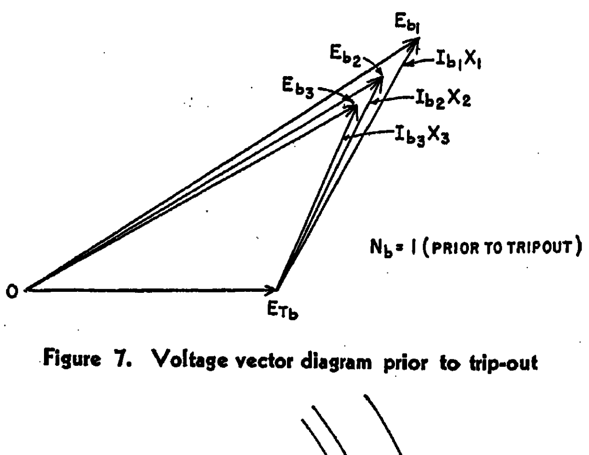


Figure 7. Voltage vector diagram prior to trip-out

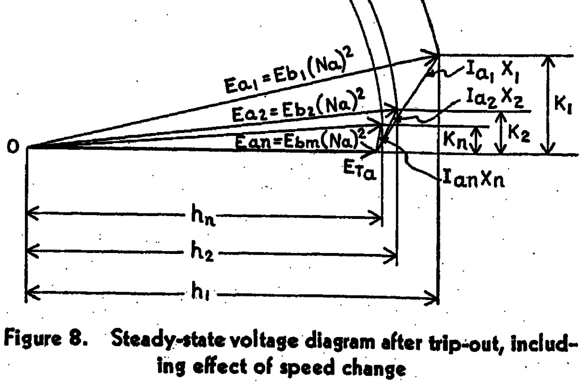


Figure 8. Steady-state voltage diagram after trip-out, including effect of speed change

1(B). Effect of Speed Variation on Voltage

The load saturation characteristic of the pilot exciter, the main exciter, and the generator may be approximated between two points lying sufficiently close together, by

$$I_f=K'E^n=K''\phi^n \text{ or } \phi=K'''E^{1/n}$$

where $n>1$.

For the pilot exciter and the main exciter

$$E_p=K_p\phi_p N$$
$$\phi_p=K_p'E_p{}^{1/n_1}$$
$$E_p=K_p''N^{\left(\frac{n_1}{n_1-1}\right)}$$
$$E_m=K_m\phi_m N$$
$$\phi_m=K_m'E_p{}^{1/n_2}$$
$$E_m=K_m''N^{\left[\frac{n_1}{n_2(n_1-1)}+1\right]}$$

respectively. The time delays in both the main and pilot exciters are neglected, since they are small compared with that of the generator field.

For the generator, then

$$E_g=K_g\phi_g N$$
$$\phi_g=K_g'E_m{}^{1/n_3}$$
$$\frac{E_N}{E}=\left[1+\left\{N^{\left[\frac{n_1}{n_2(n_1-1)}+1\right]\frac{1}{n_3}}-1\right\}\times \left\{1-\epsilon^{-t/T}\right\}\right]N$$

Since in most units $n\approx 2$ the equation reduces to

$$\frac{E_N}{E}=N[1+(N-1)(1-\epsilon^{-t/T})]$$

E_N = voltage at speed N
E = voltage at $N=1.0$
$T\approx T_{de}'$

The foregoing equations are based on sudden speed change. When used in a step-by-step method the result will be approximately correct. For steady state the ratio $E_N/E=N^2$. This relationship was used for rough work.

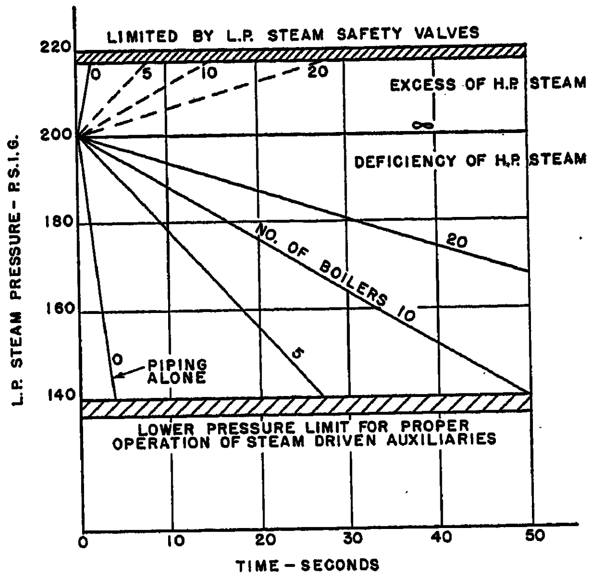
2(A). Speed Variations—Governor Characteristics

With the steady-state governor characteristics available for different loads, the steady-state load division is easily determined. For the transient performance, however, the steady-state characteristics must be modified, which was done through the following assumptions:

	Dead Time, Seconds	Travel Time, Seconds
High-pressure governors	0.1	1.0
Low-pressure governors	1.0	2.5

The travel rate of the high-pressure governor valves is assumed constant, because of the relatively high speed of valve travel, while that of the low-pressure governors was assumed to be proportional to the difference between the actual turbine speed at the moment and the speed required by the steady-state regulation.

Figure 9. Effect of banked low-pressure boilers on steam pressure for 5-megawatt unbalance in steam flow, flash steam not included



2(B). Computations

The step-by-step calculation is performed as shown in Table IV. Starting with the initial conditions of no steam flow unbalance, and with no turbine accelerating torque, $t=0$ marks the time at which the tie line load is assumed to be dropped. The difference between the prior load and the bus load is applied to accelerate the rotating masses.

Using methods similar to those used in stability studies, the speed is found. Intervals of 0.1 to 1.0 second are used; the smaller, when changes are rapid, and the larger, when steady state is approached. After the first 0.1 second, the high-pressure turbine governors are assumed to move to a position corresponding to the speed at the end of the next interval, providing that the assumed travel rate is not exceeded. From the turbine input (assumed to be proportional to the instantaneous position of the valve gear) a new accelerating torque is derived.

When the dead time of the low-pressure turbine governors has been passed, they are treated in a similar manner, except that an allowance is made for the rate of travel being a function of displacement.

The load is treated as a constant impedance; it varies, therefore, as the square of the bus voltage. The bus voltage, corrected for the actual speed, is plotted as previously developed.

Curves of governor position versus load for actual speed-changer settings are used to obtain governor valve position.

Starting from the initial condition of balanced steam flow, any decrease in high-pressure turbine generation, not matched by a corresponding decrease in low-pressure generation, is considered to result in a deficiency of high-pressure steam flow, thus decreasing the low-pressure header pressure, and vice versa.

The power contribution of each unit represents the turbine input, which, during acceleration, may be quite different from the generator output.

3(A). Low-Pressure Steam Pressure as Affected by Load Drop and by Low-Pressure Steam Generation

Due to the different valve gear travel speeds and governor regulations, the steam flow balance, which existed before the load drop, will generally be destroyed and the low-pressure steam header pressure will either rise or fall, depending on the direction of the unbalance. The larger the volume of stored steam, the slower will be the change in pressure.

Three conditions may exist:

No Low-Pressure Boilers On. In that case the available energy is only that stored in the volume of the connecting piping.

Low-Pressure Boilers Floating on the Line. With this condition, there is considerable stored energy available which is utilized when the low-pressure header pressure drops because of the inherent volume and the release of flash steam. If the low-pressure header pressure rises, the boiler stop-check valves may prevent the complete utilization of this energy. However, the rate of rise of pressure generally is so slow, and the pressure differential usually small enough, that the loss of energy from this cause may be inconsequential.

Low-Pressure Boilers on the Line and Steaming. This is similar to the second condition, except that more low-pressure steam will be available with dropping pressure. Even if the stop-check valves should close on rising pressure, the low-pressure steam flow will be reduced by the amount of initial low-pressure boiler contribution.

The energy available to the turbines may be corrected for the change in the low-pressure header pressure in the step-by-step calculations. For the cases at hand, the net change was relatively slow for the first 15 seconds. After a few check steps, this correction generally could be ignored.

It should be noted that, where very little storage volume exists, very severe low-pressure header pressure drops may occur because of the faster action of the high-pressure turbine governors shutting off the supply of steam while the slower low-pressure turbine governor valves are still nearly wide open.

Figure 9 shows the effect of steam storage volume. If full utilization of the stored energy in the boiler water were possible, the flash steam so supplied, on decreasing pressure, would decrease the rate of pressure drop to 1/15 of the rate shown in this graph.

Nomenclature

e = voltage behind the adjusted synchronous reactance
e' = voltage behind the leakage reactance
X = adjusted synchronous reactance
X_e = leakage reactance of equivalent generator
Z_L = impedance of load
b_L = susceptance of load
I = current
θ = angle between current and bus voltage
E, E_T = bus voltage
K = constant
ϕ = useful magnetic flux
N = per unit speed
T_{do}' = direct axis transient open circuit time constant
T_{de}' = transient short circuit time constant, adjusted for the load reactance
n = exponent a = constant

Subscripts 1, 2, . . . n, refer to generators 1, 2, . . . n
b, a = before or after load drop, respectively
eg = equivalent generator
p = pilot exciter
m = main exciter
E_n = voltage at speed N

References

1. WESTINGHOUSE TRANSMISSION AND DISTRIBUTION REFERENCE BOOK. Westinghouse Electric Corporation (East Pittsburgh, Pa.) 1942.

2. SYSTEM LOAD SWINGS, H. A. Bauman, O. W. Manz, Jr., J. E. McCormack, H. B. Seeley. *AIEE Transactions*, volume 60, 1941, pages 541–47.

3. GENERATOR STABILITY AT LOW EXCITATION, E. L. Michelson, L. F. Lischer. *AIEE Transactions*, volume 67, part I, 1948, pages 1–9.

No Discussion

A-C Tripping and Reclosing of Line Sectionalizing Circuit Breakers

C. E. WINEGARTNER
MEMBER AIEE

H. E. BONHEIMER
ASSOCIATE AIEE

THE application of power supply for control at a remote unattended station is discussed. This problem requires a design for automatic sectionalizing and reclosing two 34.5-kv circuits without the use of a control battery.

The control and tripping scheme for the two 34.5-kv oil circuit breakers uses a-c tripping and reclosing power. Circuit breakers automatically sectionalize and re-energize the overhead portions of two overhead-underground cable circuits when faulted. The use of a-c tripping in a remote unattended small sheet-steel building, normally unheated, eliminates the hazards and difficulties of battery maintenance. This description covers the operation of the various control devices for manual and automatic switching during fault condition.

General

In this application of automatic switching, Figure 1, a large transmission station *W* feeds two 34.5-kv underground cable circuits of 18-megavolt-ampere capacity. The underground cable parts are tapped to feed one or more distribution stations *X* through step-down transformers paralleled on the low side only. To utilize all the capacity of the underground cables, they were connected through circuit breakers at sectionalizing station *Y*, to overhead lines which feed one or more distribution stations *Z*. These small stations *Z* are fed through step-down transformers which are paralleled on the low side only.

The control scheme provides for automatically isolating the overhead portion of a faulted circuit at sectionalizing station *Y* by tripping the circuit breaker; phase induction overload relays and ground induction overload relays actuate the tripping. Backfeed is removed at distribution stations *Z* by circuit breaker tripping from reverse power relays.

After clearing the fault, automatic reclosure is made at sectionalizing station *Y* by a conventional timer. The circuit breaker on the low side of transformer recloses by an induction-type potential relay after line is re-energized. The closing and tripping controls of the circuit breakers are alternating current at all stations except station *W*.

Control and Tripping of 34.5-Kv Circuit Breakers at Sectionalizing Station *Y*

NORMAL OPERATION

Normally all circuit breakers and air switches, Figure 1, are closed. Supply station *W* feeds distribution stations *X* through underground cable. Supply station *W* also feeds distribution stations *Z* through underground cable to sectionalizing station *Y* and by overhead line beyond.

FAULT ON OVERHEAD LINE

1. A fault on overhead line at *A*, Figure 1, is cleared by tripping circuit breakers at sectionalizing station *Y* and distribution stations *Z*.

At sectionalizing station *Y*, the 34.5-kv circuit breaker trips (Figure 2) by either: phase induction overload relays; or a ground induction overload relay. The phase relays with circuit opening contacts direct the current transformer circuits to trip coils. The ground relay with circuit closing contacts connect trip circuit from a tripping transformer energized by two sets of bushing current transformers.

At distribution station *Z*, Figure 1, reverse power relays with circuit opening contacts provide protection; control of circuit opening contacts is similar to overload relay trip circuit at station *Y*, Figure 2.

2. Automatic re-energizing of overhead line and power transformer is attempted immediately at sectionalizing station *Y*.

An a-c operated timer introduces three impulses to closing relay *X* of oil circuit breaker, Figure 3. The first impulse through contact *BC* is instantaneous with

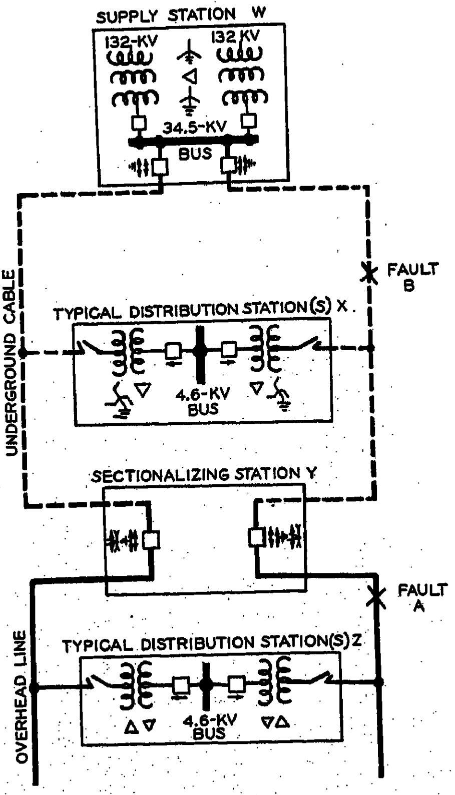


Figure 1. 1-line diagram of 34.5-kv underground-overhead circuits

---

Paper 49-293, recommended by the AIEE Switchgear Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted February 16, 1949; made available for printing September 7, 1949.

C. E. Winegartner and H. E. Bonheimer are both with The Cleveland Electric Illuminating Company, Cleveland, Ohio.

the second and third at 15 and 150 second intervals respectively.

3. Automatic reconnecting the main power transformer to 4.6-kv bus at distribution stations *Z*, Figure 1, follows successful energizing of transformers. A potential time delay relay actuates an a-c closing coil of 4.6-kv circuit breaker after three seconds of sustained voltage.

FAULT IN CABLE

1. A fault at *B* in cable is cleared by trippings at supply station *W*, distribution stations *X* and *Z;* a single-phase-to-ground fault will clear at sectionalizing station *Y* instead of stations *Z*. No automatic reclosure is attempted for cable faults.

At stations *W* and *X*, grounded neutral of power transformers provide tripping current for phase-to-ground faults. At attended station *W*, phase and ground overload relays initiate d-c impulse to trip coils.

At distribution stations *X* and *Z*, reverse power relays with circuit opening contacts control current transformer output by a circuit similar to overload relays at station *Y*, Figure 2. Phase-to-ground faults in cable are cleared from load end at sectionalizing station *Y* instead of *Z*. With no neutral grounding beyond load side of station *Y*, an over-under voltage relay instead of ground overload relay was required to detect phase-to-ground faults. Both over- and under-voltage contacts are circuits closing, Figure 2. An *AA* auxiliary time delay relay discriminates to prevent false trippings from surges. Automatic reclosure is locked-out by a back contact on *AA* auxiliary relay, Figure 3, opening the timer closing circuit. In this manner the cable is protected from over-voltage to ground, which would result if an arcing ground were not removed, as well as an automatic reclosure into the grounded cable.

Solenoid Operating Mechanism

TRIPPING

The maximum number of trip coils available per oil circuit breaker is limited to four by standard design, Figure 2. Having used three trip coils for phase overload relays, it was necessary to have triple control on the fourth trip coil. This fourth trip coil is operated by a common circuit from (1) the manual remote control switch, (2) the ground overload relay for "overhead-line" phase-to-ground faults, and (3) the over-under voltage relay for underground cable phase-to-ground faults.

For overhead line phase-to-ground faults, this fourth 3-ampere trip coil is actuated by current transformers feeding through a tripping transformer. Remote manual control and underground cable phase-to-ground faults also actuate this same 3-ampere trip coil from a distribution feeder 230-volt a-c supply. The 230-volt a-c supply is connected to a 3-ampere trip coil through a suitable resistor.

RECLOSING

The use of alternating current to actuate this size solenoid operated mechanism requires a large size rectifier. Automatic reclosure and remote manual closing become impossible during interruptions of the a-c distribution feeder supply.

Pneumatic-operated mechanism for this type of circuit breaker of present design was offered with: a-c operation of compressor motor; d-c operation of one trip coil standard, with one manufacturer adding three current transformer trip coils special; and an exhaust valve used for pneumatic trip-free operation requiring d-c operation (one large manufacturer has mechanical trip-free operation).

Change to complete a-c operation was not generally available. Hence, the advantage of reliable stored operating capacity of the pneumatic mechanism at sectionalizing station *Y* was not advisable without installing a battery.

Conclusions

The control and tripping schemes described demonstrate the feasibility of

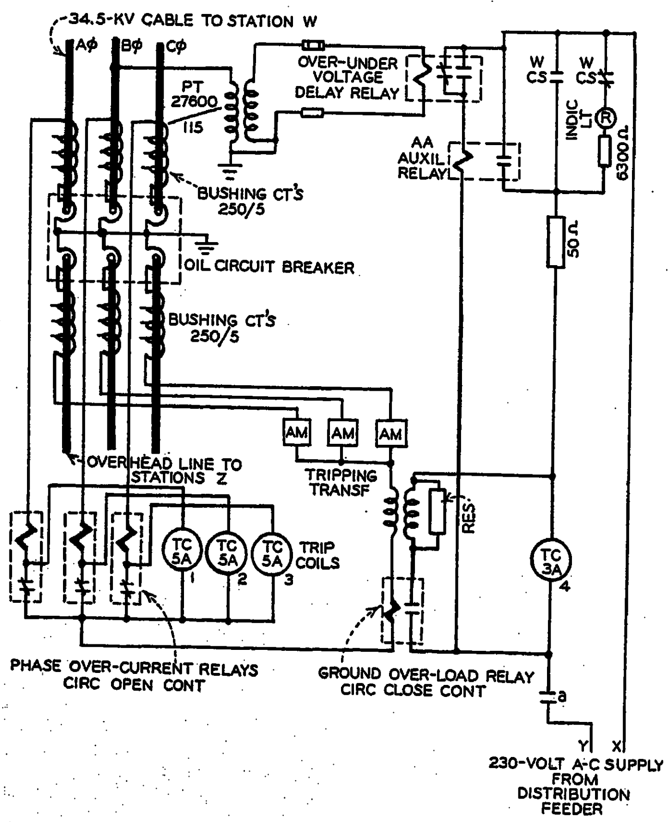
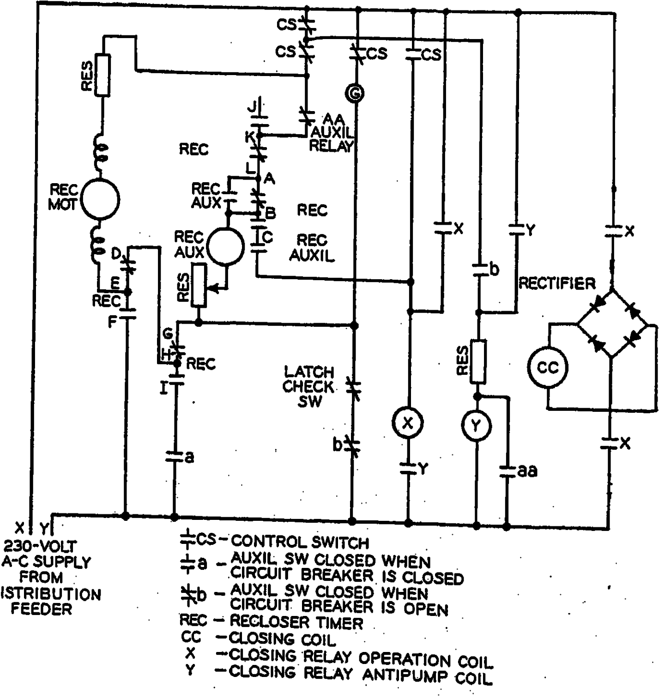

34.5-KV CABLE TO STATION W
Aϕ
Bϕ
Cϕ
PT
27600
115
OVER-UNDER VOLTAGE DELAY RELAY
W CS
W CS
INDIC LT
R
6300 Ω
AA AUXIL RELAY
BUSHING CT'S 250/5
OIL CIRCUIT BREAKER
50 Ω
BUSHING CT'S 250/5
AM
AM
AM
OVERHEAD LINE TO STATIONS Z
TRIPPING TRANSF
RES
TC 5A
TC 5A
TC 5A
TRIP COILS
1
2
3
TC 3A
4
PHASE OVER-CURRENT RELAYS CIRC OPEN CONT
GROUND OVER-LOAD RELAY CIRC CLOSE CONT
a
Y
X
230-VOLT A-C SUPPLY FROM DISTRIBUTION FEEDER

Figure 2 (left). Schematic wiring diagram of tripping control for solenoid-operated mechanism at station *Y*

Figure 3 (below). Schematic wiring diagram of closing control for solenoid-operated mechanism at station *Y*


CS
CS
CS
CS
RES
G
J
K
AA AUXIL RELAY
REC
L
A
REC MOT
REC AUX
B
REC
REC AUX
C
REC AUXIL
X
Y
X
D
b
E
REC
RES
RECTIFIER
F
G
H
REC
RES
CC
I
LATCH CHECK SW
X
Y
b
a
Y
aa
X
X
Y
230-VOLT A-C SUPPLY FROM DISTRIBUTION FEEDER
CS – CONTROL SWITCH
a – AUXIL SW CLOSED WHEN CIRCUIT BREAKER IS CLOSED
b – AUXIL SW CLOSED WHEN CIRCUIT BREAKER IS OPEN
REC – RECLOSER TIMER
CC – CLOSING COIL
X – CLOSING RELAY OPERATION COIL
Y – CLOSING RELAY ANTIPUMP COIL

using alternating current to eliminate battery maintenance problems at remote unattended stations.

The applications of circuit opening relays and tripping transformer, while not generally used, have proved satisfactory in service. However, it would improve this a-c tripping if a 3-phase network transformer could be developed. Such a network transformer, into which 3-phase currents were fed, could connect its output to one trip coil through circuit closing contacts. Thus three trip coils, and the objection to relay contacts in the current transformer circuits would be eliminated.

The use of large rectifiers to close a-c solenoid operated circuit breakers from an a-c source is common. However, a complete a-c pneumatic operated circuit breaker would insure faster operation and several reclosures with stored air, if a-c control supply failed. Development of an a-c operated exhaust valve, operation from the main trip bar or a change to a mechanically trip-free mechanism, would give added stimulus to a-c tripping and automatic reclosing.

No Discussion

Transients in 2-Phase Synchronous Machines

N. E. WILSON
MEMBER AIEE

Synopsis: The analytical study of 3-phase synchronous machines has been well covered by many authors.[1,2,3,4] Not so much attention has been devoted to the 2-phase machine because much of the development of the methods of analysis occurred after the trend away from 2-phase systems was well established. Although the present use of 2-phase generators represents a small percentage of total generation, it is believed that there is some justification for analytical study of 2-phase machine performance. Such a study has value for the 2-phase systems now in use, and it has value in an analytical sense by showing how the relations developed for 3-phase generators may be adapted to generators with a different number of phases.

The discussion in this paper is confined to L-connected, or quarter-phase machines with salient poles and without damper windings. It is limited to short-circuit or transient conditions. The inductances are defined and the relations expressing the variations of the several inductances of the windings are developed. The general flux-linkage equations for all windings are determined in terms of stator and rotor currents. With this basis the operation of the machine under all possible short-circuit conditions is considered with the object of determining the transient and steady-state currents. The possible short-circuit conditions for the L-connected quarter-phase machine are the double line-to-neutral fault, the line-to-line fault, and the single line-to-neutral fault. The equations developed are compared with similar relations for 3-phase machines to show the similarities and the differences.

The method of solution for the currents uses the theorem of constant flux linkages. This theorem has been used[5] successfully to obtain an approximate solution for fault conditions on 3-phase machines. It has been shown[6,7] that the solution for a symmetrical short circuit on a 3-phase generator is but very little modified when it is based on rigorous analysis instead of the theorem of constant flux linkages. Since this theorem is the basis of most of the analytical study of the behavior under transient conditions of synchronous machines, and even of d-c machines,[8] it is used here.

Inductances of the Windings

THE SELF inductance of a winding on the stator of a machine with salient poles is not a constant, but varies with rotor position. This is shown in Figures 1 and 2. In Figure 1 the self inductance of phase A is greatest when the centerline of the poles (direct axis) is in line with the centerline of the phase. The self inductance of phase B is minimum at this time. When the poles have rotated through 90 electrical degrees so that an interpolar space (quadrature axis) is in line with the centerline of phase A the self inductance of this winding is minimum and of phase B it is maximum. If the variation in reluctance of the magnetic circuit is sinusoidal, then

$$L_{aa}=L_{sa}+L_{sv}\cos 2\sigma \tag{1}$$

$$L_{bb}=L_{sa}+L_{sv}\cos 2(\sigma-\pi/2)=L_{sa}-L_{sv}\cos 2\sigma \tag{2}$$

where L_{aa} and L_{bb} are the self inductances of phases A and B respectively, L_{sa} is the average value of these inductances, and L_{sv} is the amplitude of variation from the average value. The instantaneous angular displacement, σ, of the direct axis of the poles from the reference point, the centerline of phase A, is measured in electrical radians. The double frequency variation in reluctance of the magnetic circuit causes the double frequency variation in inductance.

Nomenclature

L_{aa}, L_{bb} = self inductance of phase A or B
L_{sa} = average value of L_{aa} or L_{bb}
L_{sv} = amplitude of variation of L_{aa} or L_{bb}
L_{ab} = mutual inductance between phases A and B
L_{mv} = amplitude of variation of L_{ab}
L_{af}, L_{bf} = mutual inductance between phase A or B and the field
L_{afM} = maximum value of L_{af} or L_{bf}
L_{ff} = self inductance of the field
L_d = direct-axis inductance
L_d' = direct-axis transient inductance
L_q = quadrature-axis inductance
X_d, X_d', X_q = reactance values of L_d, L_d', and L_q
$T_{d\phi}'$ = time constant used with transient terms arising from direct current in the field with the armature circuits open
T_d' = time constant used with transient terms arising from direct current in the field with one or more armature circuits closed
T_a = time constant used with transient terms arising from direct current in the armature circuit
σ = instantaneous displacement of the direct axis from the center-line of phase A
$\psi=\sigma+\pi/4$
Λ = instantaneous value of the flux linkages of a circuit
ϕ = angle of lead or lag of the armature current with respect to the internally generated voltage
i = instantaneous current in a winding
E_t = effective value of the voltage generated in one phase of the armature on open circuit by the flux linkages with the field circuit
E_{tM} = maximum value of E_t

The mutual inductance between phase A and phase B is the mutual inductance between windings which are $\pi/2$ electrical radians apart. Therefore, the average value of such an inductance will be zero. However, in a machine with a salient-pole rotor, the unsymmetrical distribution of flux produced by current in one phase produces at times during one cycle of variation of σ a net linkage of flux with the other phase. When σ is zero, the distribution of flux produced by cur-

---

Paper 49-294, recommended by the AIEE Rotating Machinery Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted March 21, 1949; made available for printing September 13, 1949.

N. E. WILSON is Associate Professor of Electrical Engineering at the University of Massachusetts.

This paper is based upon a thesis supervised by Dr. W. A. Lewis, Dean of Graduate School, Illinois Institute of Technology, and submitted by Mr. Wilson as Westinghouse Fellow in Power Systems Engineering to the faculty of the Illinois Institute of Technology in partial fulfillment of the requirements for the degree of Master of Science in Electrical Engineering.

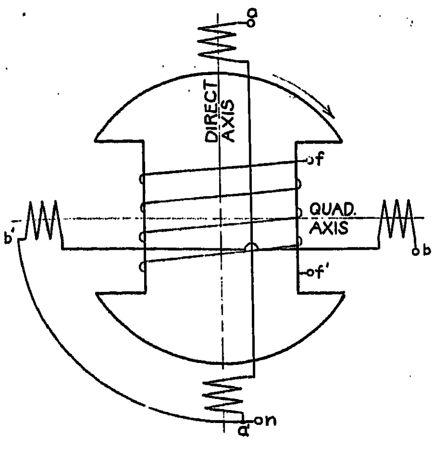


Figure 1. Position of the rotor for maximum self inductance of phase A

rent in phase A is symmetrical and the net flux linkages with phase B are zero. As the rotor turns toward phase B (assumed to be $\pi/2$ radians behind phase A), the flux is shifted toward the axis of this winding and a positive linkage of flux with phase B occurs. This effect reaches a maximum when $\sigma=\pi/4$ radians. When the rotor has turned $\pi/2$ radians, the flux is again symmetrically disposed and the flux linkages are again zero. When the rotor turns beyond $\pi/2$ radians, the flux again is distorted, being shifted backward so that there is a negative linkage of flux with phase B when phase A carries current. The effect reaches its maximum value when $\sigma=3\pi/4$. This variation in mutual effect may be stated in equation form as

$$L_{ab}=L_{mv}\cos 2(\sigma-\pi/4)=L_{mv}\sin 2\sigma \tag{3}$$

in which L_{ab} is the mutual inductance between phase A and phase B, and L_{mv} is the amplitude of variation of the mutual inductance.

It has been shown[9] that for 3-phase machines the amplitudes of variation of the self and mutual inductances are very nearly equal. The assumption of equality is normally made in the analysis of 3-phase machine performance, and since the pole structure would be the same for a 2-phase machine, the same relationship will be assumed here. Thus, since $L_{sv}=L_{mv}$,

$$L_{ab}=L_{sv}\sin 2\sigma \tag{4}$$

The self-inductance of the field winding is constant since the reluctance of the magnetic path of the flux produced by current in this winding does not change with rotor position (slot effects not considered).

The mutual inductances between rotor and phase windings are variables, being maximum for a particular phase when the direct axis of the field is in line with the axis of the phase winding. These variations for a 2-phase machine may be expressed as

$$L_{af}=L_{afM}\cos\sigma \tag{5}$$

$$L_{bf}=L_{afM}\cos(\sigma-\pi/2)=L_{afM}\sin\sigma \tag{6}$$

in which L_{afM} is the maximum value of the mutual inductance, L_{af}, between phase A and the rotor winding, and of the mutual inductance, L_{bf}, between phase B and the rotor winding.

It is convenient in 3-phase work to resolve the inductance of each phase winding into components, one to be used with the component of armature current producing magnetization along the direct axis, and the other to be used with the component of armature current producing magnetization in the quadrature axis. These are called the direct-axis inductance and the quadrature-axis inductance respectively. It also is useful in 2-phase machine work to make this separation because of the simplification it brings to the form of the equations, and as a means of comparison with the 3-phase case. In Appendix I it is shown that these inductances are

$$L_d=L_{sa}+L_{sv} \tag{7}$$

$$L_q=L_{sa}-L_{sv} \tag{8}$$

from which L_{sa} and L_{sv} may be obtained as

$$L_{sa}=(L_d+L_q)/2 \tag{9}$$

$$L_{sv}=(L_d-L_q)/2 \tag{10}$$

Double Line-to-Neutral Short Circuit

In applying the theorem of constant flux linkages, it is necessary to determine the flux linkages before the fault when the phase currents are zero (for short circuits occurring on an unloaded machine), and to equate the expressions thus obtained to those for flux linkages at any time with armature currents present. The equations resulting then may be solved for the currents for the first instant after short circuit. In Appendix I the general flux-linkage equations are developed. The circuit for a double line-to-neutral short circuit is shown in Figure 3. For this condition $e_{an}=e_{bn}=0$. Since each of the phase windings has zero voltage and is closed on itself, the constant-linkage theorem applies to these windings directly and

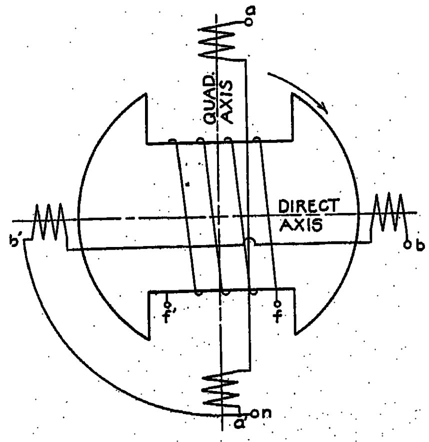


Figure 2. Position of the rotor for minimum self inductance of phase A

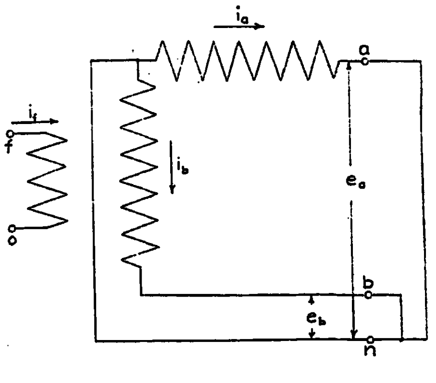


Figure 3. Circuit for a double line-to-neutral short circuit

$$\Lambda_a=\Lambda_{a0} \tag{11}$$

$$\Lambda_b=\Lambda_{b0} \tag{12}$$

The process indicated by these equations carried out in Appendix II and the solutions for i_a, i_b, and i_f are obtained. The result is

$$\begin{aligned} i_a=&\frac{\sqrt{2}E_i(X_d'+X_q)}{2X_d'X_q}\cos\sigma_0-\\ &\frac{\sqrt{2}E_i}{X_d'}\cos(\omega t+\sigma_0)-\\ &\frac{\sqrt{2}E_i(X_d'-X_q)}{2X_d'X_q}\cos(2\omega t+\sigma_0) \end{aligned} \tag{13}$$

$$\begin{aligned} i_b=&\frac{\sqrt{2}E_i(X_d'+X_q)}{2X_d'X_q}\sin\sigma_0-\\ &\frac{\sqrt{2}E_i}{X_d'}\sin(\omega t+\sigma_0)-\\ &\frac{\sqrt{2}E_i(X_d'-X_q)}{2X_d'X_q}\sin(2\omega t+\sigma_0) \end{aligned} \tag{14}$$

$$\begin{aligned} i_f=&i_{f0}+i_{f0}(X_d-X_d')/X_d'-\\ &i_{f0}[(X_d-X_d')/X_d']\cos\omega t \end{aligned} \tag{15}$$

In these equations some new terms have been introduced. All are defined in Appendix IV. All have the same significance as they do when used for 3-phase machines, but the values of the reactances involve the actual winding reactances in a slightly different relationship as will be discussed later.

Present in these equations are five components of current. They are:

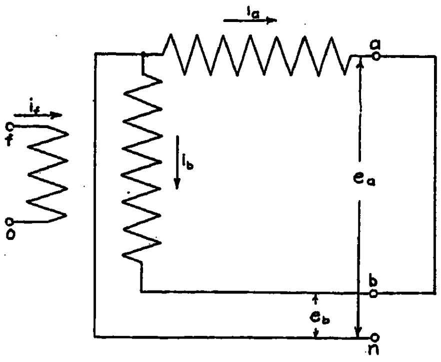


Figure 4. Circuit for a line-to-line short circuit

1. A d-c transient in the field resulting from the demagnetizing effect of the steady magnetomotive force which is a result of balanced currents in the armature.

2. An a-c fundamental-frequency component in the armature caused by the direct current in the field.

3. A d-c transient component in the armature. This is the usual d-c transient that occurs whenever an alternating voltage is suddenly applied to an inductive circuit. It depends on the value of the voltage at the time the switch is closed.

4. An a-c component of twice fundamental frequency in the armature. This is part of component 3 and would not be present if the inductances of the windings were constant. The variation of inductances is caused not only by saliency, but also by the presence of rotor circuits in one axis.

5. An a-c fundamental-frequency component in the field which results from the efforts of the field in preventing components 3 and 4 from making any changes in the field flux linkages.

Equations 13, 14, and 15 are for the currents at the first instant after short circuit occurs. If resistances were not present, these currents would continue unchanged in amplitude. The resistances will cause exponential changes in the amplitudes with time constants to be determined in a later section.

Line-to-Line Short Circuit

The circuit diagram for a line-to-line short circuit is shown in Figure 4. The phase voltages, e_{an} and e_{bn}, are not zero as in the previous case, but

$$i_a = -i_b \tag{16}$$

$$e_{an} - e_{bn} = e_{ab} = 0 \tag{17}$$

With resistances neglected

$$e_{ab} = -d\Lambda_a/dt - (-d\Lambda_b/dt) = -d(\Lambda_a - \Lambda_b)/dt$$

and the flux linkages comprising those of phases A and B may be defined as

$$\Lambda_{ab} = \Lambda_a - \Lambda_b \tag{18}$$

Therefore

$$-\Lambda_{ab} = 0 \tag{19}$$

This being the case, Λ_{ab} must be constant at the initial value once the short circuit has been established, and

$$\Lambda_{ab0} = \Lambda_{ab} \tag{20}$$

The substitutions for equation 20 are made in Appendix III and the resulting equation solved for i_a and i_b. With these currents known, i_f also may be found and the resulting equations are

$$i_a = -i_b = -\frac{\sqrt{2E_{tM}}}{X_d' + \sqrt{X_d'X_q}} \times \left[\cos\psi + \sum_{n=1}^{n=\infty} b^n \cos(2n+1)\psi\right] + \frac{\sqrt{2}E_{tM}\cos\psi_0}{2\sqrt{X_d'X_q}}\left[1 + 2\sum_{n=}^{n=\infty} b^n \cos 2n\psi\right] \tag{21}$$

$$i_f = i_{f0} + \frac{(X_d - X_d')i_{f0}}{X_d' + \sqrt{X_d'X_q}} \times \left[1 + \frac{1+b}{b}\sum_{n=1}^{n=\infty} b^n \cos 2n\psi - 2\cos\psi_0 \times \left(\cos\psi + \sum_{n=1}^{n=\infty} b^n \cos(2n+1)\psi\right)\right] \tag{22}$$

In these equations

$$\psi = \sigma + \pi/4 = \omega t + \sigma_0 + \pi/4 \tag{23}$$

$$b = (\sqrt{X_d'X_q} - X_d')/(X_d' + \sqrt{X_d'X_q}) \tag{24}$$

Equations 21 and 22 contain the five components of current listed for the double line-to-neutral short circuit. They also contain two additional sets of harmonic currents. These are:

1. A series of even harmonics in the field and of odd harmonics in the armature which are dependent on the direct current in the field.

2. A series of even harmonics in the armature and of odd harmonics in the field which are dependent on the direct current in the armature.

These series of harmonic currents are caused by the fact that a line-to-line fault is a case of single-phase loading. A single-phase load on a synchronous machine results in distorted currents in the field, or armature, or both. Since some depend on direct current in the field, not all will have their amplitudes decreased to zero when transient conditions have passed.

Single Line-to-Neutral Short Circuit

The circuit for a single line-to-neutral short circuit is shown in Figure 5. In this case the current in phase B and the voltage in phase A are both zero. Since e_{an} is zero, the theorem of constant flux linkages applies to phase A, and

$$\Lambda_a = \Lambda_{a0} \tag{25}$$

When the values of Λ_a and Λ_{a0}, as given in Appendix I, are substituted in equation 25, the equation for current in phase A may be found by a mathematical development identical with that used for the line-to-line case in Appendix III. Solutions for armature and field currents are

$$i_a = -\frac{2E_{tM}}{X_d' + \sqrt{X_d'X_q}} \times \left[\cos\sigma + \sum_{n=1}^{n=\infty} b^n \cos(2n+1)\sigma\right] + \frac{E_{tM}\cos\sigma_0}{\sqrt{X_d'X_q}}\left[1 + 2\sum_{n=1}^{n=\infty} b^n \cos 2n\sigma\right. \tag{26}$$

$$i_f = i_{f0} + \frac{(X_d - x_d')i_{f0}}{X_d' + \sqrt{X_d'X_q}} \times \left[1 + \frac{1+b}{b}\sum_{n=1}^{n=\infty} b^n \cos 2n\sigma - 2\cos\sigma_0 \times \left(\cos\sigma + \sum_{n=1}^{n=\infty} b^n \cos(2n+1)\sigma\right)\right] \tag{27}$$

In these equations, b has the value given in equation 24.

Inspection of equations 26 and 27 for the single line-to-neutral short circuit, and of equations 21 and 22 for the line-to-line short circuit shows that the two faults are very little different for the 2-phase machine. The differences are: the magnitude of each component in the armature is greater by the factor $\sqrt{2}$ for a single line-to-neutral short circuit; and the angular position of the currents in phase A is shifted by $\pi/4$ radians for the line-to-line fault. The reaction on the field circuit is the same for both faults if the rotor occupies the same position relative to the axis of the single winding short-circuited that it occupies relative to the two windings short circuited. If the positions are not the same, the amplitudes of the odd-harmonic components are affected by the difference in initial position. The maximum possible amplitudes are the same in each case.

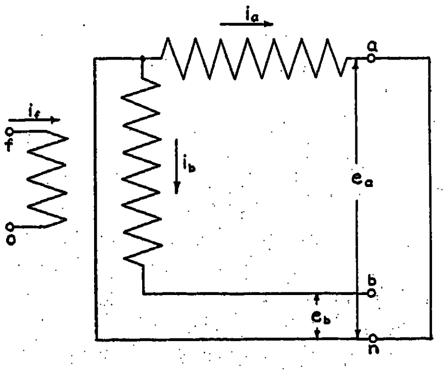


Figure 5. Circuit for a single line-to-neutral short circuit

Time Constants and Complete Expressions for the Currents

All of the preceding equations for currents in the windings of a 2-phase machine under various conditions of short circuit are for the first instant after the short circuit occurs, and they were obtained by neglecting the effects of resistance in each winding. The effect of the resistances is small in determining the amplitudes of these currents, but it is the instrument by which some of the components are caused to decay to steady-state values, and others to zero. In determining and applying the time constants to be used with these equations, all components of current in the field and armature which are related to direct current in the field are assumed to decay together at a rate governed by a time constant determined from field circuit parameters. The ratio of the final to initial amplitudes of these components will be the same as the ratio of final to initial values of direct current in the field.

Further, all of the components of current in the field and armature which are related to direct current in the armature are assumed to decay together at a rate governed by a time constant determined from armature circuit parameters. The ratio of final to initial amplitudes of these components will be the same as the ratio of final to initial values of direct current in the armature.

In general, the fundamental-frequency and odd-harmonic components of current in the armature are associated with the direct and even-harmonic components in the field. The fundamental-frequency and odd-harmonic components of current in the field are associated with the direct and even-harmonic components in the armature. The first of these two groups of components will not decay to zero. The direct current in the field is increased on short circuit to counteract the demagnetizing effect of the armature reaction. The increase in direct current diminishes until the total direct current in the field has returned to its original value (before short circuit) because there is no additional field voltage to support the increase.

Thus, the final value of field current for each of the cases of short circuit is i_{f0}.

The second of these two groups of components will decay to zero. All of this group are dependent on direct current in the armature, and, there being no source of sustained direct current in that circuit, their final values will become zero.

Double Line-to-Neutral Short Circuit

The initial value of direct current in the field is contained in the first two terms of equation 15. The ratio of final to initial values is, then

$$i_{f0}/i_{f(d\text{-}c)}=X_d'/X_d \tag{28}$$

When this ratio is applied to the amplitude of the fundamental-frequency components of current in armature-phase A, the result is

$$I_{aM(ss)}=\frac{E_{tM}}{X_d} \tag{29}$$

where $I_{aM(ss)}$ is the amplitude of the steady-state current in phase A. By separating the initial amplitude into its steady-state and transient parts, and remembering that the final amplitudes of all other components in the armature, and of the transient components in the field, are zero, the equations for current in phase A and in the field may be written

$$\begin{aligned}i_a=&\left[\frac{E_{tM}}{X_d}+\left(\frac{E_{tM}}{X_d'}-\frac{E_{tM}}{X_d}\right)\epsilon^{-\frac{t}{T_d'}}\right]\times\\&\cos\ (\omega t+\sigma_0)+\frac{E_{tM}}{2X_d'}\left(\frac{X_d'+X_q}{X_q}\cos\sigma_0-\right.\\&\left.\frac{X_d'-X_q}{X_q}\cos\ (2\omega t+\sigma_0)\right)\epsilon^{-\frac{t}{T_a}}\end{aligned} \tag{30}$$

$$\begin{aligned}i_f=i_{f0}&\left[1+\frac{X_d-X_d'}{X_d'}\ \epsilon^{-\frac{t}{T_d'}}-\right.\\&\left.\frac{X_d-X_d'}{X_d'}\epsilon^{-\frac{t}{T_a}}\cos\ \omega t\right]\end{aligned} \tag{31}$$

In these equations T_d' and T_a are the short-circuit time constants of the field and armature respectively for a double line-to-neutral fault.

The time constant of an inductive circuit is the ratio of its inductance to its resistance. The effective inductance of the field circuit during short-circuit conditions may be determined by dividing its flux linkages at any time by the value of the direct current in the field at the same time. Both of these quantities are known at $t=0$, immediately after the short circuit is established. Using equation 65, in Appendix I, and the first two terms of equation 15, the result is

$$L_{ff(eff)}=\frac{L_{ff}I_{f0}}{i_{f0}\dfrac{X_d}{X_d'}}=L_{ff}\ \frac{X_d'}{X_d} \tag{32}$$

$$\begin{aligned}T_d'=L_{ff(eff)}/R_{ff}=(L_{ff}/R_{ff})(X_d'/X_d)=\\T_{d\phi}'(X_d'/X_d)\end{aligned} \tag{33}$$

where $T_{d\phi}'$ is the time constant that would be obtained for the field with armature circuits open.

The effective inductance of the armature is not a constant but varies in each phase with rotor position. However, an average value may be found by dividing the flux linkages at $t=0$ by the average value of the direct current in a given phase at the same time. The flux linkages of phase A at this time are given by equation 63, in Appendix I, and the average value of the direct current by the first term of equation 13. Then

$$L_{a(eff)}=\frac{L_{afM}i_{f0}\cos\ \sigma_0}{E_{tM}\dfrac{X_d'+X_q}{2X_d'X_q}\cos\ \sigma_0}=\frac{2X_d'X_q}{\omega(X_d'+X_q)} \tag{34}$$

and

$$T_a=\frac{L_{a(eff)}}{R_a}=\frac{2X_d'X_q}{\omega R_a(X_d'+X_q)} \tag{35}$$

Line-to-Line Short Circuit

The initial value of direct current in the field is contained in the first two terms of equation 22. The final value is the same as before. Thus

$$\frac{i_{f0}}{i_f}=\frac{X_d'+\sqrt{X_d'X_q}}{X_d+\sqrt{X_d'X_q}} \tag{36}$$

The steady-state value of the amplitude of the fundamental-frequency and odd-harmonic components in the armature is obtained as before by multiplying the initial value by the ratio given in equation 36. The result is

$$\begin{aligned}I_{aM(ss)}=-&\left(\frac{\sqrt{2}E_{tM}}{X_d'+\sqrt{X_d'X_q}}\right)\times\\&\left(\frac{X_d'+\sqrt{X_d'X_q}}{X_d+\sqrt{X_d'X_q}}\right)=-\frac{\sqrt{2}E_{tM}}{X_d+\sqrt{X_d'X_q}}\end{aligned} \tag{37}$$

Then the complete approximate equation for armature current becomes

$$\begin{aligned}i_a=-i_b=-&\left[\frac{\sqrt{2}E_{tM}}{X_d+\sqrt{X_d'X_q}}+\right.\\&\left(\frac{\sqrt{2}E_{tM}}{X_d'+\sqrt{X_d'X_q}}-\right.\\&\left.\left.\frac{\sqrt{2}E_{tM}}{X_d+\sqrt{X_d'X_q}}\right)\epsilon^{-\frac{t}{T_{d'(LL)}}}\right]\times\\&\left(\cos\ \psi+\sum_{n=1}^{n=\infty}b^n\cos\ (2n+1)\psi\right)+\\&\frac{\sqrt{2}E_{tM}\cos\ \psi_0}{2\sqrt{X_d'X_q}}\times\\&\left(1+2\sum_{n=1}^{n=\infty}b^n\cos 2n\psi\right)\epsilon^{-\frac{t}{T_{a(LL)}}}\end{aligned} \tag{38}$$

The amplitude of the even-harmonic currents in the field change in the same ratio as the direct current there, and with a field time constant. The amplitude of the odd-harmonic components in the field follow the direct current in the armature to zero with an armature time constant.

Therefore

$$\begin{aligned} i_f=i_{f0}+i_{f0}\Bigg[&\frac{X_d-X_d'}{X_d+\sqrt{X_d'X_q}}+\left(\frac{X_d-X_d'}{X_d'+\sqrt{X_d'X_q}}-\frac{X_d-X_d'}{X_d-\sqrt{X_d'X_q}}\right)\epsilon^{-\frac{t}{T_{d'(LL)}}}\Bigg]\times\\ &\left(\frac{1+b}{b}\sum_{n=1}^{n=\infty}b^n\cos 2n\psi\right)+\\ &i_{f0}\Bigg[\frac{X_d-X_d'}{X_d'+\sqrt{X_d'X_q}}\epsilon^{-\frac{t}{T_{d'(LL)}}}-\frac{2(X_d-X_d')\cos\psi_0}{X_d'+\sqrt{X_d'X_q}}\times\\ &\left(\cos\psi+\sum_{n=1}^{n=\infty}b^n\cos(2n+1)\psi\right)\epsilon^{-\frac{t}{T_{a'(LL)}}}\Bigg] \end{aligned} \tag{39}$$

The effective inductances are determined as for the preceding case by dividing the flux linkages of each circuit at $t=0$ by the average value of direct current in the respective circuit required to maintain these flux linkages. The ratios of these inductances to the resistances of the circuits are the time constants. For the field circuit, equation 65 is divided by the first two terms of equation 22 to obtain the effective inductance.

$$L_{ff(eff)}=\frac{L_{ff}i_{f0}}{\dfrac{X_d+\sqrt{X_d'X_q}}{X_d'+\sqrt{X_d'X_q}}i_{f0}}=L_{ff}\frac{X_d'+\sqrt{X_d'X_q}}{X_d+\sqrt{X_d'X_q}} \tag{40}$$

$$T_{d'(LL)}=\frac{L_{ff(eff)}}{R_{ff}}=\frac{L_{ff}}{R_{ff}}\frac{X_d'+\sqrt{X_d'X_q}}{X_d+\sqrt{X_d'X_q}}=T_{d0}'\frac{X_d'+\sqrt{X_d'X_q}}{X_d+\sqrt{X_d'X_q}} \tag{41}$$

For the armature circuit the flux linkages are required from equation 81 (in Appendix III). They are

$$\Lambda_{ab0}=L_{afM}i_{f0}(\cos\sigma_0-\sin\sigma_0)=\sqrt{2}L_{afM}i_{f0}\cos(\sigma_0+\pi/4)=\sqrt{2}L_{afM}i_{f0}\cos\psi_0 \tag{42}$$

The direct current of the armature is contained in equation 21 so that

$$L_{ab(eff)}=\frac{\sqrt{2}L_{afM}i_{f0}\cos\psi_0}{\dfrac{\sqrt{2}E_{tM}\cos\psi_0}{2\sqrt{X_d'X_q}}}=\frac{2\sqrt{X_d'X_q}}{\omega} \tag{43}$$

$$T_{a(LL)}=\frac{L_{ab(eff)}}{2R_a}=\frac{\sqrt{X_d'X_q}}{\omega R_a} \tag{44}$$

Single Line-to-Neutral Short Circuit

The final and initial values of direct current in the field are the same for this case as for the line-to-line short circuit. Since the amplitude of the fundamental-frequency and odd-harmonic components of current in the armature are also the same except for a factor $\sqrt{2}$, the result for the steady-state current amplitude may be written as

$$I_{aM(ss)}=\frac{-2E_{tM}}{X_d+\sqrt{X_d'X_q}} \tag{45}$$

Then the approximate complete expression for armature current is

$$\begin{aligned} i_a=-\Bigg[&\frac{2E_{tM}}{X_d+\sqrt{X_d'X_q}}+\left(\frac{2E_{tM}}{X_d'+\sqrt{X_d'X_q}}-\frac{2E_{tM}}{X_d+\sqrt{X_d'X_q}}\right)\epsilon^{-\frac{t}{T_{d'(LN)}}}\Bigg]+\\ &\left(\cos\sigma+\sum_{n=1}^{n=\infty}b^n\cos(2n+1)\sigma\right)+\frac{E_{tM}\cos\sigma_0}{\sqrt{X_d'X_q}}\times\\ &\left(1+2\sum_{n=1}^{n=\infty}b^n\cos 2n\sigma\right)\epsilon^{-\frac{t}{T_{a(LN)}}} \end{aligned} \tag{46}$$

For the field, the result is

$$\begin{aligned} i_f=i_{f0}+i_{f0}\Bigg[&\frac{X_d-X_d'}{X_d+\sqrt{X_d'X_q}}+\left(\frac{X_d-X_d'}{X_d'+\sqrt{X_d'X_q}}-\frac{X_d-X_d'}{X_d+\sqrt{X_d'X_q}}\right)\epsilon^{-\frac{t}{T_{d'(LN)}}}\\ &\left(\frac{1+b}{b}\sum_{n=1}^{n=\infty}b^n\cos 2n\sigma+\right)\Bigg]+\\ &i_{f0}\Bigg[\frac{X_d-X_d'}{X_d'+\sqrt{X_d'X_q}}\epsilon^{-\frac{t}{T_{d'a(LN)}}}-\frac{2(X_d-X_d')\cos\sigma_0}{X_d'+\sqrt{X_d'X_q}}\Bigg(\cos\sigma+\\ &\sum_{n=1}^{n=\infty}b^n\cos(2n+1)\sigma\Bigg)\epsilon^{-\frac{t}{T_{a(LN)}}}\Bigg] \end{aligned} \tag{47}$$

Since the amplitude of the direct current in the field is the same for both the line-to-line and the single line-to-neutral faults, both initially and immediately after short circuit, the effective inductance is the same for the two cases, as will be the time constant also. Thus

$$T_{d'(LN)}=T_{d'(LL)}=T_{d\phi}'\frac{X_d'+\sqrt{X_d'X_q}}{X_d+\sqrt{X_d'X_q}} \tag{48}$$

The effective inductance per phase of the armature circuit is determined as before. The flux linkages at $t=0$ are given by equation 63 (in Appendix I). The direct current in the armature is obtained from equation 26. Then

$$L_{a(eff)}=\frac{L_{afM}i_{f0}\cos\sigma_0}{\dfrac{E_{tM}\cos\sigma_0}{\sqrt{X_d'X_q}}}=\frac{\sqrt{X_d'X_q}}{\omega} \tag{49}$$

Thus the effective inductance of phase A of the armature circuits under the conditions of a line-to-neutral short circuit is one-half the effective inductances of phases A and B in series for a line-to-line short circuit. Since the resistance of the circuit involved is also one-half as large in the present case, the armature circuit time constant is the same as given by equation 44. Therefore

$$T_{a(LN)}=T_{a(LL)}=\frac{\sqrt{X_d'X_q}}{\omega R_a} \tag{50}$$

Comparison With 3-Phase Machines

Some of the essential differences between the 2-phase and 3-phase synchronous machines have been absorbed in the definitions of the inductances in the direct and quadrature axes. The result is that the equations for the short-circuit currents in the 2-phase machine, when expressed in terms of these inductances, are very similar to equivalent equations for 3-phase machines. The differences in the inductances are shown in Table I. In the expressions for 3-phase machine inductances is the term L_{ma} which is the average value of the mutual inductance between phases. For the 2-phase case this term is zero, as noted previously.

Equations 30 and 31 for the currents in phase A and in the field for a double line-to-neutral short circuit are identical with equivalent equations for a 3-phase machine with a 3-phase short circuit. Thus, the only differences here are included in the definitions of inductances.

Equation 38 for the current in phase A for a line-to-line short circuit is the same as the equivalent equation for a 3-phase machine with a similar fault excepting a common multiplier of $\sqrt{2}$ for the 2-phase, and $\sqrt{3}$ for the 3-phase cases. There is a necessary difference in phase angle because in this paper the equation is written for the reference phase, and for 3-phase work it is usually written for one of the two nonreference phases. The equation for field current is the same in both cases.

Equations 46 and 47 for a single line-to-neutral fault are considerably different from the equivalent equations for a 3-phase machine. The difference involves a term X_0 used to account for the zero-sequence reactance of 3-phase systems. The value of X_0 is given by

$$X_0=\omega L_0=\omega(L_{sa}-2L_{ma})$$

The term L_0 does not appear in this paper because of the absence of an average mutual inductance, L_{ma}. Thus the various inductances of the 2-phase machine may

Table I. Inductances and Time Constants for 2-Phase and 3-Phase Machines

Quantity	2-Phase	3-Phase
L_d	$L_{sa}+L_{sv}$	$L_{sa}+L_{ma}+3/2L_{sv}$
L_q	$L_{sa}-L_{sv}$	$L_{sa}+L_{ma}-3/2L_{sv}$
L_d'	$L_d-L_{afM}^2/L_{ff}$	$L_d-(3/2)(L_{afM}^2/L_{ff})$
$T_{d\phi}'$	L_{ff}/R_{ff}	L_{ff}/R_{ff}
T_d'*	$T_{d\phi}'\dfrac{X_d'}{X_d}$	$T_{d\phi}'\dfrac{X_d'}{X_d}$
$T_{d'(LL)}$	$T_{d\phi}'\dfrac{X_d'+\sqrt{X_d'X_q}}{X_d+\sqrt{X_d'X_q}}$	$T_{d\phi}'\dfrac{X_d'+\sqrt{X_d'X_q}}{X_d+\sqrt{X_d'X_q}}$
$T_{d'(LN)}$	$T_{d\phi}'\dfrac{X_d'+\sqrt{X_d'X_q}}{X_d+\sqrt{X_d'X_q}}$	$T_{d\phi}'\dfrac{X_d'+\dfrac{X_0}{2}+\sqrt{\left(X_d'+\dfrac{X_0}{2}\right)\left(X_q+\dfrac{X_0}{2}\right)}}{X_d+\dfrac{X_0}{2}+\sqrt{\left(X_d'+\dfrac{X_0}{2}\right)\left(X_q+\dfrac{X_0}{2}\right)}}$
T_a*	$\dfrac{2X_d'X_q}{\omega R_a(X_d'+X_q)}$	$\dfrac{2X_d'X_q}{\omega R_a(X_d'+X_q)}$
$T_{a(LL)}$	$\dfrac{\sqrt{X_d'X_q}}{\omega R_a}$	$\dfrac{\sqrt{X_d'X_q}}{\omega R_a}$
$T_{a(LN)}$	$\dfrac{\sqrt{X_d'X_q}}{\omega R_a}$	$\dfrac{2}{3\omega R_a}\sqrt{\left(X_d'+\dfrac{X_0}{2}\right)\left(X_q+\dfrac{X_0}{2}\right)}$

* T_d', T_a are for a double line-to-neutral fault on a 2-phase machine and for a 3-phase fault on a 3-phase machine.

be completely defined by L_d and L_q alone. This would not be the case for short circuits involving neutral impedance. However, it can be shown[10] that when neutral impedance is not involved, zero-sequence voltages do not enter the equations for 2-phase 3-wire generators. If equation 26, for current in phase A for a single line-to-neutral short circuit, is written in terms of machine inductances, L_{sa}, L_{sv}, L_{afM} and L_{ff} instead of with the fictitious inductances, L_d, L_d', and L_q, a result will be obtained as follows

$$i_a=-\frac{2E_{tM}}{L_{sa}-L_{afM}^2/L_{ff}+\sqrt{(L_{sa}+L_{sv}-L_{afM}^2/L_{ff})(L_{sa}-L_{sv})}}\times\left(\cos\sigma+\sum_{n=1}^{n=\infty}b^n\cos(2n+1)\sigma\right)+\frac{E_{tM}\cos\sigma_0}{\sqrt{(L_{sa}+L_{sv}-L_{afM}^2/L_{ff})(L_{sa}-L_{sv})}}\times\left(1+2\sum_{n=1}^{n=\infty}b^n\cos(2n\sigma)\right)$$

Equations 7, 8, and 72, in Appendix II, were used to effect this change. If the same procedure is followed in writing an equivalent equation for a 3-phase machine with a single line-to-neutral short circuit, an identical expression results when the various inductances are defined in the same manner, and the analysis is carried out by the method of constant flux linkages. The effect of L_{ma} cancels. This is, of course, as it should be since, with equal windings in each case and no other armature windings involved in the fault, it can make no difference how many other windings are on the armature.

Included in Table I are the expressions for the various time constants of both 2-phase and 3-phase machines. They are alike for all but the single line-to-neutral case. However, if the same procedure is followed in expressing these time constants in terms of winding inductances, they become identical. That they should do so is evident when it is considered that in each case but one winding is involved. It should be noted that for the other two faults the expressions appear alike when written in terms of direct- and quadrature-axis inductances (or their reactances) but would be dissimilar if expressed in terms of winding inductances because L_{ma} would not cancel.

Appendix I. Flux Linkages of the Windings

The flux linkages of the phase A and B windings are given by

$$\Lambda_a=L_{aa}i_a+L_{ab}i_b+L_{af}i_f \tag{51}$$

$$\Lambda_b=L_{bb}i_b+L_{ab}i_a+L_{bf}i_f \tag{52}$$

Substituting for the inductances from equations 1, 2, 4, 5, and 6, these become

$$\Lambda_a=L_{sa}i_a+L_{sv}i_a\cos 2\sigma+L_{sv}i_b\sin 2\sigma+L_{afM}i_f\cos\sigma \tag{53}$$

$$\Lambda_b=L_{sa}i_b-L_{sv}i_b\cos 2\sigma+L_{sv}i_a\sin 2\sigma+L_{afM}i_f\sin\sigma \tag{54}$$

The flux linkages of the field winding are

$$\Lambda_f=L_{ff}i_f+L_{af}i_a+L_{bf}i_b \tag{55}$$

where L_{ff} is the constant self inductance of the field winding. Substituting again equations 5 and 6 gives

$$\Lambda_f=L_{ff}i_f+L_{afM}i_a\cos\sigma+L_{afM}i_b\sin\sigma \tag{56}$$

Equations 53, 54, and 56 are general for the three windings on an L-connected salient-pole 2-phase machine and apply under all conditions if there are no additional windings carrying current. If the armature currents are known, the expressions for flux linkages may be used to determine the armature and field voltages.

It is useful to determine the flux linkages for the particular case where the armature windings are carrying symmetrical, sinusoidal currents. Thus, let

$$i_a=I_{aM}\sin(\sigma+\phi_i) \tag{57}$$

$$i_b=I_{aM}\sin(\sigma+\phi_i-\pi/2) \tag{58}$$

where I_{aM} is the amplitude of the armature current and ϕ_i is the angle between the armature current and the internally generated voltage. Using these equations to substitute in equation 53, and combining terms gives

$$\Lambda_a=I_{aM}[L_{sa}\sin(\sigma+\phi_i)+L_{sv}\sin(-\sigma+\phi_i)]+L_{afM}i_f\cos\sigma \tag{59}$$

When the sines of the double angles are expanded and terms combined, this equation becomes

$$\Lambda_a=I_{aM}[(L_{sa}+L_{sv})\sin\phi_i\cos\sigma+(L_{sa}-L_{sv})\cos\phi_i\sin\sigma]+L_{afM}i_f\cos\sigma \tag{60}$$

If ϕ_i is zero, representing unity power factor, the first term in brackets becomes zero and the flux linkages become those caused by field current plus a component at right angles to the flux linkages caused by field current; that is, a component in the quadrature axis, and the inductance used is $L_{sa}-L_{sv}$. If ϕ_i is $\pm\phi/2$, representing leading or lagging zero power factor, the second term inside the brackets becomes zero and the flux linkages become those produced by the field current plus or minus a component produced by armature current that is in the same (direct) axis, and the inductance to be used is $L_{sa}+L_{sv}$. Thus two values of inductance become apparent, one for the direct axis, and one for the quadrature axis. They are

$$L_d=L_{sa}+L_{sv} \tag{61}$$

$$L_q=L_{sa}-L_{sv} \tag{62}$$

L_d and L_q are called the direct-axis inductance and the quadrature-axis inductance respectively. These quantities have the same significance as their counterparts in 3-phase machines, but their values are different. This point is discussed in a later section.

The values of initial flux linkages, before short circuit occurs, are found by substituting for the values of current at that time. They are

$$i_a=i_b=0$$
$$i_f=i_{f0}$$

From these equations and equations 53, 54, and 56 the flux linkages are

$$\Lambda_{a0}=L_{afM}i_{f0}\cos\sigma_0 \tag{63}$$

$$\Lambda_{b0}=L_{afM}i_{f0}\sin\sigma_0 \tag{64}$$

$$\Lambda_{f0}=L_{ff}i_{f0} \tag{65}$$

If the flux linkages of the field before short circuit are equated to the flux linkages of this circuit after short circuit, an expression may be obtained for the instantaneous value of field current in terms of i_a and i_b. Performing this operation gives

$$L_{ff}i_{f0}=L_{ff}i_f+L_{afM}i_a\cos\sigma+L_{afM}i_b\sin\sigma$$

$$i_f=i_{f0}-(L_{afM}/L_{ff})(i_a\cos\sigma+i_b\sin\sigma)\qquad(66)$$

When this equation is substituted in equations 53 and 54, expressions for the phase flux linkages are obtained which contain only armature currents.

$$\begin{aligned}\Lambda_a=L_{sa}i_a+L_{sv}i_a\cos 2\sigma+L_{sv}i_b\sin 2\sigma+\\ L_{afM}i_{f0}\cos\sigma-(L_{afM}^2/L_{ff})\times\\ (i_a\cos\sigma+i_b\sin\sigma)\cos\sigma\end{aligned}\qquad(67)$$

$$\begin{aligned}\Lambda_b=L_{sa}i_b-L_{sv}i_b\cos 2\sigma+L_{sv}i_a\sin 2\sigma+\\ L_{afM}i_{f0}\sin\sigma-(L_{afM}^2/L_{ff})\times\\ (i_a\cos\sigma+i_b\sin\sigma)\sin\sigma\end{aligned}\qquad(68)$$

Appendix II. Currents for a Double Line-to-Neutral Short Circuit

Applying the method of the theorem of constant flux linkages, equations 63 and 64 for the flux linkages before short circuit are set equal to equations 67 and 68 for the flux linkages before short circuit are set equal to equations 67 and 68 for the flux linkages after short circuit. When this is done for phase A, the result is

$$\begin{aligned}L_{afM}i_{f0}\cos\sigma_0=L_{sa}i_a+L_{sv}i_a\cos 2\sigma+\\ L_{sv}i_b\sin 2\sigma+L_{afM}i_{f0}\cos\sigma-\\ (L_{afM}^2/L_{ff})(i_a\cos\sigma+i_b\sin\sigma)\cos\sigma\end{aligned}\qquad(69)$$

The values of L_{sa} and L_{sv}, as given in equations 9 and 10, may now be substituted. When this is done, and simplification accomplished, the result is

$$\begin{aligned}&2L_{afM}i_{f0}(\cos\sigma_0-\cos\sigma)\\ &=i_a[L_d-L_{afM}^2/L_{ff}+L_q+\\ &(L_d-L_{afM}^2/L_{ff}-L_q)\times\cos 2\sigma]+\\ &i_b[(L_d-L_{afM}^2/L_{ff}-L_q)\sin 2\sigma]\end{aligned}\qquad(70)$$

The expression $L_{afM}i_{f0}$ gives the maximum flux linkages of the phase windings caused by initial current in the field. When this is multiplied by ω, the angular velocity of the field, it is equal to the maximum value of the voltage generated in the phase windings for a field current i_{f0}. Thus

$$E_{tM}=\sqrt{2}E_t=\omega L_{afM}i_{f0}\qquad(71)$$

This equation may be used to simplify equation 70.

Before this is done, a further simplification can be made by defining the direct-axis transient inductance as

$$L_d'=L_d-L_{afM}^2/L_{ff}\qquad(72)$$

and

$$X_d'=\omega L_d'=X_d-\omega L_{afM}^2/L_{ff}\qquad(73)$$

Using equations 71, 72, and 73, equation 70 may finally be written

$$\begin{aligned}2E_{tM}(\cos\sigma_0-\cos\sigma)=i_a[X_d'+X_q+\\ (X_d'-X_q)\cos 2\sigma]+i_b(X_d'-X_q)\\ \sin 2\sigma\end{aligned}\qquad(74)$$

Similarly, a relation for phase B may be written

$$\begin{aligned}2E_{tM}(\cos\sigma_0-\cos\sigma)=i_a(X_d'-X_q)\sin 2\sigma+\\ i_b[X_d'+X_q-(X_d'-X_q)\cos 2\sigma]\end{aligned}\qquad(75)$$

Equations 74 and 75 are two equations in i_a and i_b and may be solved simultaneously for these currents. These solutions are

$$\begin{aligned}i_a=\frac{E_{tM}(X_d'+X_q)}{2X_d'X_q}\cos\sigma_0-\frac{E_{tM}}{X_d'}\cos(\omega t+\sigma_0)-\\ \frac{E_{tM}(X_d'-X_q)}{2X_d'X_q}\cos(2\omega t+\sigma_0)\end{aligned}\qquad(76)$$

$$\begin{aligned}i_b=\frac{E_{tM}(X_d'+X_q)}{2X_d'X_q}\sin\sigma_0-\frac{E_{tM}}{X_d'}\sin(\omega t+\sigma_0)-\\ \frac{E_{tM}(X_d'-X_q)}{2X_d'X_q}\sin(2\omega t+\sigma_0)\end{aligned}\qquad(77)$$

The equation for current in the field may be obtained by substituting these values of i_a and i_b in equation 66. The equation for field current may first be put into better form as follows

$$\begin{aligned}i_f&=i_{f0}-\frac{\omega L_{afM}^2}{L_{ff}}\;\frac{i_{f0}}{\omega L_{afM}i_{f0}}(i_a\cos\sigma+i_b\sin\sigma)\\ &=i_{f0}-\frac{X_d-X_d'}{E_{tM}}i_{f0}(i_a\cos\sigma+i_f\sin\sigma)\end{aligned}\qquad(78)$$

Then, on substituting the armature currents and making use of trigonometric identities, equation 78 becomes

$$i_f=i_{f0}+\frac{X_d-X_d'}{X_d'}i_{f0}-\frac{X_d-X_d'}{X_d'}i_{f0}\cos\omega t\qquad(79)$$

Appendix III. Currents for a Line-to-Line Short Circuit

The starting point is with equations 67 and 68, the expressions for the general flux linkages of each phase winding. These may be simplified by the fact that $i_a=-i_b$. When the two flux-linkage equations are substituted in equation 18, Λ_{ab} is obtained as

$$\begin{aligned}\Lambda_{ab}=2L_{sa}i_a-2L_{sv}i_a\sin 2\sigma+\\ L_{afM}i_{f0}(\cos\sigma-\sin\sigma)-\\ (L_{afM}^2/L_{ff})(1-2\sin\sigma\cos\sigma)i_a\end{aligned}\qquad(80)$$

The relation for flux linkages at $t=0$ may be obtained from equation 80 by the fact that $i_a=0$. Thus

$$\Lambda_{ab0}=L_{afM}i_{f0}(\cos\sigma_0-\sin\sigma_0)\qquad(81)$$

After the short circuit is established, $\Lambda_{ab}=\Lambda_{ab0}$. Then setting equation 80 equal to equation 81 and making use of equations 9 and 10, the result for i_a is obtained as

$$i_a=-i_b=\frac{\sqrt{2}E_{tM}[\cos(\sigma_0+\pi/4)-\cos(\sigma+\pi/4)]}{X_d'+X_q-(X_d'-X_q)\sin 2\sigma}\qquad(82)$$

Equation 82 will be easier to manipulate if the following changes in angles are made

$$\psi=\sigma+\pi/4=\omega t+\sigma_0+\pi/4$$
$$\psi_0=\sigma_0+\pi/4$$

The field current may be obtained from equation 66 by substituting the values of i_a and i_b and making the change in angle. It is

$$\begin{aligned}i_f=i_{f0}+(X_d-X_d')\times\\ \frac{1+\cos 2\psi-2\cos\psi_0\cos\psi}{X_d'+X_q+(X_d'-X_q)\cos 2\psi}i_{f0}\end{aligned}\qquad(83)$$

Equation 82 with the change in angle, and equation 83 may be expanded as series by means of the following equations

$$\frac{1}{A+B\cos 2\psi}=\frac{1}{\sqrt{A^2-B^2}}\times\left(1+2\sum_{n=1}^{n=\infty}b^n\cos 2n\psi\right)$$

$$\frac{\cos\psi}{A+B\cos 2\psi}=\frac{1+b}{\sqrt{A^2-B^2}}\times\left(\cos\psi+\sum_{n=1}^{n=\infty}b^n\cos(2n+1)\psi\right)$$

$$\frac{\cos 2\psi}{A+B\cos 2\psi}=\frac{1}{\sqrt{A^2+B^2}}\times\left(b+\frac{1+b^2}{b}\sum_{n=1}^{n=\infty}b^n\cos 2n\psi\right)$$

$$\frac{1+\cos 2\psi}{A+B\cos 2\psi}=\frac{1+b}{\sqrt{A^2+B^2}}\times\left(1+\frac{1+b}{b}\sum_{n=1}^{n=\infty}b^n\cos 2n\psi\right)\qquad(84)$$

where

$$\begin{aligned}b=\frac{\sqrt{A-B}-\sqrt{A+B}}{\sqrt{A+B}+\sqrt{A-B}}+\frac{\sqrt{X_q}-\sqrt{X_d'}}{\sqrt{X_d'}+\sqrt{X_q}}\\ =\frac{\sqrt{X_d'X_q}-X_d'}{\sqrt{X_d'X_q}+X_d'}\end{aligned}\qquad(85)$$

for the equations under consideration. When equations 82 and 83 are expanded in accordance with equation 84, the results for i_a and i_f are

$$\begin{aligned}i_a=-i_b=-\frac{\sqrt{2}E_{tM}}{X_d'+\sqrt{X_d'X_q}}\times\\ \left(\cos\psi+\sum_{n=1}^{n=\infty}b^n\cos(2n+1)\psi\right)+\\ \frac{\sqrt{2}E_{tM}\cos\psi_0}{2\sqrt{X_d'X_q}}\left(1+2\sum_{n=1}^{n=\infty}b^n\cos 2n\psi\right)\end{aligned}\qquad(86)$$

$$\begin{aligned}i_f=i_{f0}+i_{f0}\frac{X_d-X_d'}{X_d'+\sqrt{X_d'X_q}}\times\\ \left[\frac{1+b}{b}\sum_{n=1}^{n=\infty}b^n\cos 2n\psi-2\cos\psi_0\times\right.\\ \left.\left(\cos\psi+\sum_{n=1}^{n=\infty}b^n\cos(2n+1)\psi\right)\right]\end{aligned}\qquad(87)$$

References

1. Synchronous Machines—I and II, R. E. Doherty, C. A. Nickle. *AIEE Transactions*. volume 45, June 1926, pages 912–47.

2. Synchronous Machines—IV, R. E. Doherty, C. A. Nickle. *AIEE Transactions*, volume 47, April 1928, pages 457–92.

3. Synchronous Machines—V, R. E. Doherty, C. A. Nickle. *AIEE Transactions*, volume 49, April 1930, pages 700–14.

4. Initial and Sustained Short-Circuits in Synchronous Machines, Vladimir Karapetoff. *AIEE Transactions*, volume 44, April 1925, page 403.

5. A SIMPLIFIED METHOD OF ANALYZING SHORT-CIRCUIT PROBLEMS, R. E. Doherty. *AIEE Transactions*, volume 42, June 1923, pages 841–49.

6. TWO-REACTION THEORY OF SYNCHRONOUS MACHINES—I, R. H. Park. *AIEE Transactions*, volume 48, July 1929, page 716.

7. TWO-REACTION THEORY OF SYNCHRONOUS MACHINES—II, R. H. Park. *AIEE Transactions*, volume 52, June 1933, page 352.

8. THE SHORT-CIRCUIT CHARACTERISTICS OF D-C GENERATORS, G. E. Frost. *Electrical Engineering* (*AIEE Transactions*) volume 65, June 1946, pages 394–402.

9. FUNDAMENTAL CONCEPTS OF SYNCHRONOUS MACHINE REACTANCES, B. R. Prentice. *AIEE Transactions*, volume 56 (supplement), 1937, pages 1–21.

10. SYMMETRICAL COMPONENTS (book), C. F. Wagner, R. D. Evans. McGraw-Hill Book Company, Inc., New York, N. Y., 1933, pages 332–36.

11. SYNCHRONOUS MACHINERY, W. A. Lewis. Illinois Institute of Technology, Chicago, Ill.

No Discussion

The Dynamoelectric Amplifier—Class A Operation

ROBERT M. SAUNDERS
MEMBER AIEE

Synopsis: Previous papers have described the basic operating principles of several dynamoelectric amplifiers and their behavior under d-c transient conditions. This paper compares the various dynamoelectric amplifiers to the electronic amplifier, and discusses the static and dynamic characteristics of both the single- and 2-stage dynamoelectric amplifiers from an a-c steady-state point of view. The dynamic characteristics are calculated from equivalent circuits and compared with test results in the form of frequency response and phase angle curves. Negative feed-back circuits and response curves also are presented and the effects of feedback pointed out. Methods of obtaining the equivalent circuit parameters from test are discussed briefly.

RECENTLY there have been several papers describing the various dynamoelectric amplifiers individually which discuss their characteristics when operating under transient conditions. It is the purpose of this paper to point out the similarity of the dynamoelectric amplifiers to each other as far as external effects are concerned, to show the similarity to electronic amplifiers (on which there is a wealth of literature), and to present equivalent circuits that can be used to predict satisfactorily the performance of these amplifiers under a-c steady-state conditions.

The American Standards Association[1] definition of an amplifier is "... a device for increasing the power associated with a phenomenon without appreciably altering its quality, through control by the amplifier input of a larger amount of power supplied by a local source to the amplifier output."

There are several rotating machines that can qualify as amplifiers under the above definition and those considered here are: the separately excited d-c generator, the Amplidyne, the Rototrol, and the Regulex. In each of these machines a large amount of output power can be controlled by a small control field current, the power being supplied in the form of mechanical power through the shaft.

Figure 1(A) shows the separately excited d-c generator with its circuit parameters indicated. The inductance and resistance of the control field are shown as L_c and R_c respectively. The total inductance of the armature circuit is shown as L_A and includes the armature, interpoles, and inductive effects of commutation. The resistance R_A represents all of the resistance in the armature circuit including the armature winding, interpoles, and brushes. If a direct voltage, E_c, is placed on the control field, a voltage E_G will be developed across the brushes. The relationship between E_c and E_G is indicated as the curve BA in Figure 2, this curve being the usual saturation curve for the machine. If the direct voltage now is replaced by the smaller sine wave, Figure 2, which requires swings of voltage over only the straight line portion of the curve, the variation of the generated voltage also will be sinusoidal in nature. However, if the amplitude of the sine wave of Figure 2 is increased so that operation ensues over a curved portion of the saturation curve, the output voltage will no longer be a true representation of the input and distortion now will be present. Hence, operation along the straight line portion of the curve results in distortionless amplification, usually referred to as class A amplification.

The operation of the d-c machine as a dynamoelectric amplifier is analogous in many respects to the electronic amplifier shown in Figure 1(B); yet there are several distinct differences:

1. The source of power is mechanical instead of electrical.
2. There is no direct current present in the output circuit.
3. Control is effected by a current instead of a voltage.
4. The control and output circuits contain inductance.

As a consequence of (1) and (2), there is no problem of blocking the direct current from the load in the dynamoelectric amplifier circuit, thus obviating the necessity for capacitor C, Figure 1(B), used in the electronic amplifier. It is this capacitor that limits the low frequency response of the latter; and its absence in the dynamoelectric amplifier permits the frequency response to extend to zero frequency or direct current. As a consequence of (3), the driving circuit must have sufficient power associated with it to excite the dynamoelectric amplifier even for class A operation, whereas the electronic amplifier normally requires essentially no power for similar operation. As a consequence of (4), the high-frequency response will be affected by the inductances present. The inductances, especially the control field inductance, are usually so high that the d-c machine is not suitable for use as an amplifier where the frequency response must be greater than approximately 0.5 cycle per second.

The Amplidyne, the Rototrol, and the Regulex are dynamoelectric machines in which the time constant of the control field has been materially reduced by various means so that a higher frequency response can be achieved. In this paper it is assumed that the basic principles of these machines are familiar to the reader.[2-6] For each of these machines, as well as for the d-c machine, a plot of E_c or I_c versus

---

Paper 49-295, recommended by the AIEE Rotating Machinery Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17-21, 1949. Manuscript submitted May 2, 1949; made available for printing September 13, 1949.

ROBERT M. SAUNDERS is with the Division of Electrical Engineering, University of California, Berkeley, Calif.

The author wishes to express his appreciation to Dr. Troy D. Graybeal, Assistant Professor of Electrical Engineering, University of California, for his many valuable suggestions contributed during the preparation of this paper.

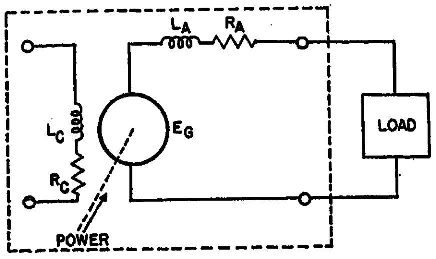


Figure 1A. Single-stage dynamoelectric amplifier with circuit parameters

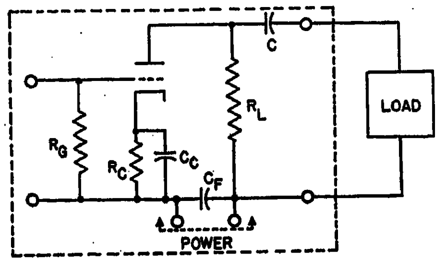


Figure 1B. Comparable single-stage resistance-coupled electronic amplifier with circuit parameters

E_G may be obtained for each stage, and for class A operation, the swings of control field current confined to the straight line portion of that curve. Likewise an equivalent circuit similar to that for the d-c machine may be employed for the purpose of obtaining the characteristics.

Static Characteristics

The static characteristics of an electronic amplifier are of interest since they determine the necessary supply voltage, circuit parameters, and the permissible operating range for linear operation. Similarly the static characteristics are needed for the dynamoelectric amplifier to determine the linear operating range and the induced voltage per ampere of field current for the various stages; this latter quantity is called the transresistance in this paper. It will be noted that the transresistance has a reciprocal analogy to the transconductance in the vacuum tube. There are several types of static characteristics. Those the author has found the most useful and the easiest to obtain are the control field current versus generated voltage for each stage, and the control field current versus over-all output voltage for constant resistance loads.

Figure 3 depicts a set of these over-all static characteristics for a 3/4-kw 250-volt Amplidyne. The open and short-circuit tests described by Graybeal[7] also have been found useful. These characteristics permit the calculation of the transresistances for the various stages and provide a check on the resistive components of the equivalent circuit. One also may calculate the power amplification, the voltage amplification, and the transconductance, but these quantities serve little use except to act as figures of merit when discussing amplifiers qualitatively.

Dynamic Characteristics

There are two methods of analyzing the dynamoelectric amplifier from a dynamic point of view: the transient method and the a-c steady-state method. This paper is concerned with the latter. If the transient analysis is preferred, the $j\omega$ terms used herein may be replaced with the operator, $p=d/dt$. In examining an amplifier from an a-c steady-state viewpoint, one usually is concerned with its amplification, frequency response characteristics, and zones of stability. To consider the dynamic characteristics an equivalent circuit is used in this paper, and the response of the equivalent circuit to various frequencies is explored. First, the single- and 2-stage amplifiers are discussed, and then the two types of negative feed-back amplifiers are considered. To illustrate the validity of the assumptions, the agreement with test values is indicated where such test data are available.

The following general assumptions apply to the dynamic analysis made in this paper:

1. Prime mover speed is constant.
2. Only straight line portion of saturation curve is used.
3. Residual effects are negligible.
4. Hysteresis effects are negligible.
5. Parameters are linear.

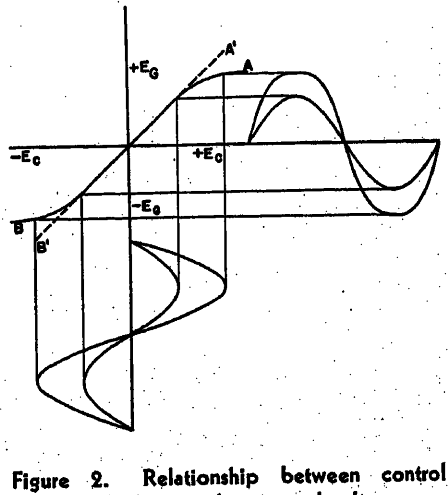


Figure 2. Relationship between control field voltage and generated voltage

Distortion is introduced as soon as the swings of control voltage extend over the straight line portion of the curve

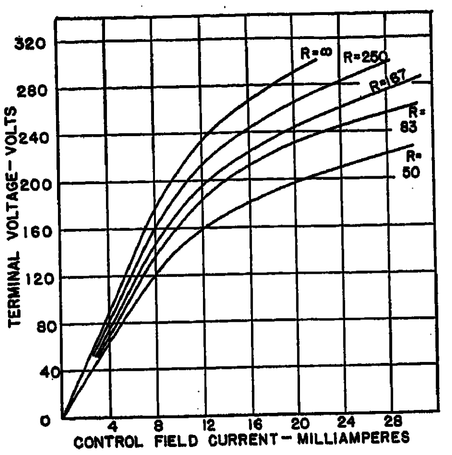


Figure 3. Over-all static characteristics of a 3/4-kw 250-volt Amplidyne

Resistance values are in ohms

First, a single-stage amplifier similar to that of Figure 1(A) will be considered. This circuit would be appropriate for the d-c generator or the single-stage Rototrol or Regulex units. For a constant alternating voltage applied to the control field, the voltage developed across the load is

$$E_L=\frac{E_GZ_L}{Z_L+Z_A}=\frac{NI_cZ_L}{Z_L+Z_A}=\frac{NE_cZ_L}{Z_c(Z_L+Z_A)} \tag{1}$$

where

$N=\left(\frac{\partial E_G}{\partial I_c}\right)_{I_A=0}$ = slope of E_G versus I_c curve

Z_L = load impedance
Z_A = armature circuit impedance
Z_c = control field self impedance
E_c = applied voltage to control field

The complex voltage amplification

$$A=\frac{E_L}{E_c}=\frac{NZ_L}{Z_c(Z_L+Z_A)} \tag{2}$$

The power amplification

$$B=\frac{P_L}{P_c}=\frac{|I_L|^2R_L}{|I_c|^2R_c}=\frac{|E_L|^2|Z_c|^2R_L}{|Z_L|^2|E_c|^2R_c}$$
$$=|A|^2\frac{|Z_c|^2R_L}{|Z_L|^2R_c} \tag{3}$$

Equation 3 is a very general one and applies to all dynamoelectric amplifiers irrespective of the numbers of stages. For any amplifier to serve as an oscillator the criterion is that one term in the denominator of equation 2 have a zero. Defining θ_L, θ_A, and θ_c as the phase angle of the load, armature, and control field impedances respectively, it is noted that in all cases $-\pi/2\leq\theta_L\leq\pi/2$; $0\leq\theta_A\leq\pi/2$; $0\leq\theta_c\leq\pi/2$. Hence $|Z_L|>0$ and $|Z_c|>0$ in all instances and therefore this amplifier is inherently stable, and cannot serve as an oscillator under the conditions imposed, namely, constant speed and linear constants.

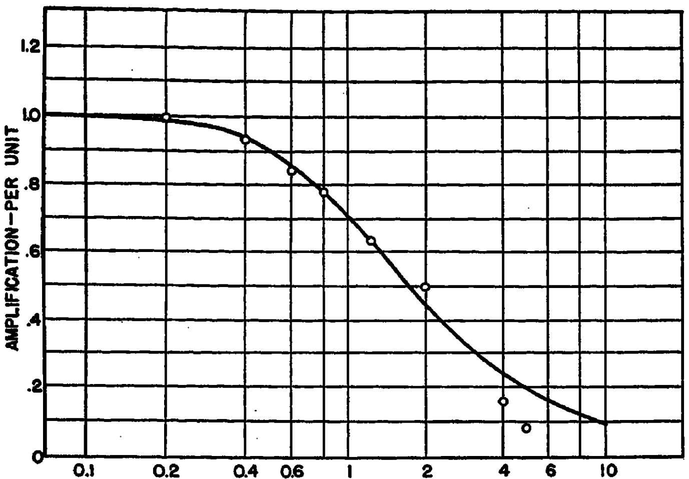


Figure 4. Frequency response characteristic typical of a single-stage dynamoelectric amplifier without feedback, in this case a 100-watt Rototrol

Test points are indicated by the circles

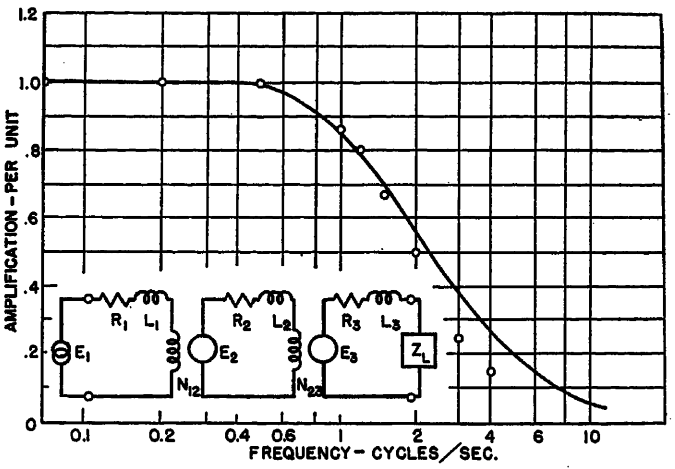


Figure 6. Frequency response characteristic typical of a 2-stage dynamoelectric amplifier without feedback, in this case a 3/4-kw 250-volt Amplidyne

The equivalent circuit used for the calculation of the curve is shown in the insert, and the test values indicated by the circles

The calculated frequency response characteristics of a 100-watt 125-volt single-stage Rototrol operating into a resistance load is shown in Figure 4 and the phase angle characteristic as curve *A*, Figure 5, with the test values indicated. For the single-stage amplifier it can be seen that there is very close agreement between the response determined by the equivalent circuit and the actual test values. The equivalent circuit for a 2-stage dynamoelectric amplifier is shown in the insert of Figure 6. This circuit would be suitable for two d-c generators in cascade, a critically compensated Amplidyne, or a critically compensated 2-stage Rototrol. For a constant alternating voltage applied to the control field, the voltage developed across the load is

$$E_L = E_1 \frac{N_{12}N_{23}Z_L}{Z_1Z_2(Z_3+Z_L)} \tag{4}$$

and the complex voltage amplification

$$A = \frac{N_{12}N_{23}Z_L}{Z_1Z_2(Z_3+Z_L)} \tag{5}$$

The power amplification expression is very similar to equation 3 except that the term Z_c is replaced by Z_1, and the *A* is, of course, that of equation 5.

$$B = |A|^2 \frac{|Z_1|^2 R_L}{|Z_L|^2 R_1} \tag{6}$$

A test for stability again is to examine the denominator for possible zeros. In this case: $0 \leqq \theta_1 \leqq \pi/2$; $0 \leqq \theta_2 \leqq \pi/2$; $0 \leqq \theta_3 \leqq \pi/2$; $-\pi/2 \leqq \theta_L \leqq \pi/2$; hence $|Z_1| > 0$, $Z_2| > 0$, $|(Z_3+Z_L)| > 0$ and no zeros can exist. The 2-stage amplifier is therefore inherently stable.

The calculated and test frequency response characteristics of a 3/4-kw 250-volt Amplidyne operating at no load ($Z_L = \infty$) are shown in Figure 6, and the phase

Figure 5. Phase angle characteristics of single- and 2-stage dynamoelectric amplifiers

Curve A—single-stage no-feedback 100-watt Rototrol

Curve B—2-stage no-feedback 3/4-kw 250-volt Amplidyne, test values indicated by circles

Curve C—2-stage, 10 per cent negative feedback, 3/4-kw 250-volt Amplidyne, test values indicated by squares

Curve D—2-stage, 20 per cent negative feedback, 3/4-kw 250-volt Amplidyne, test values indicated by triangles

Curve E—2-stage, 100 per cent negative feedback, 3/4-kw 250-volt Amplidyne, test values indicated by circles

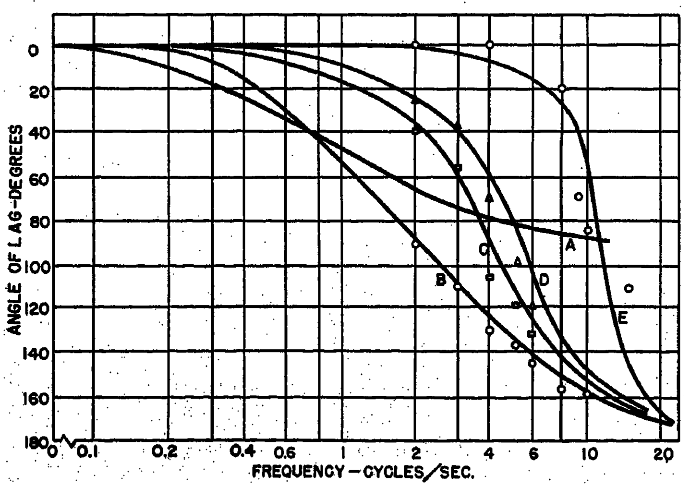
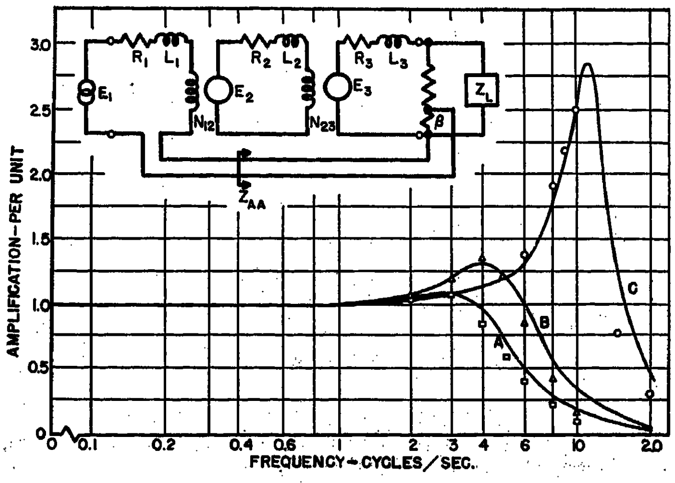


Figure 7. Frequency response characteristics of a 2-stage dynamoelectric amplifier for various degrees of negative feedback

Curve A—10 per cent feedback

Curve B—20 per cent feedback

Curve C—100 per cent feedback



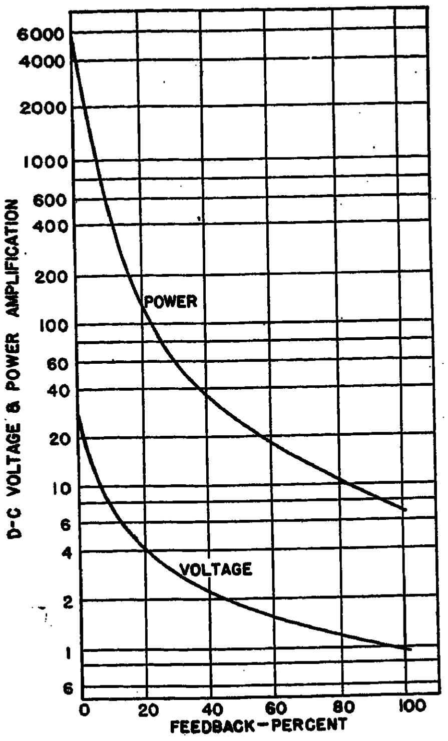


Figure 8. Variation of power and voltage amplification of a 3/4-kw 250-volt Amplidyne as a function of the amount of negative feedback

angle-frequency characteristic shown as curve *B*, Figure 5.

Feedback

The general advantages of the use of negative or inverse feedback in electronic amplifiers are well known and have been rather thoroughly explored by many observers. For a brief treatment of this subject and a bibliography the reader is referred to Terman[8] or for a more complete treatment to Bode.[9] The chief advantage of negative feedback in the dynamoelectric amplifier is that it improves the frequency response by reducing the over-all time constant by a factor of from six to ten times. The disadvantages are that the amplification is seriously reduced, and the possibility of unstable operation becomes greater.

For the purposes of this paper, negative feedback is defined as being present if at zero frequency (direct current), the over-all amplification of the amplifier is reduced from that without feedback.

The feedback may be introduced in the dynamoelectric amplifier by two methods. In the first method, called the external feed-back circuit, a fraction of the output voltage or current is placed in series with the input voltage or current as illustrated by the insert of Figure 7. The second method, called the internal feed-back circuit, involves the use of a separate control field for the purpose of injecting the feed-back signal. The second method has the advantage of keeping the input and output circuits electrically isolated, but has the disadvantage of being somewhat more awkward to handle from an analytical point of view. An example of this method is shown in Figure 11 where both current and voltage feedback are employed.

In the case of the Amplidyne and the 2-stage Rototrol, current feedback may be present as a result of the overcompensation or the undercompensation of the machine. A noncritically compensated unit will inject a magnetomotive force in the control circuit which will be proportional to the load current and will act in every way as if a separate field were used for this purpose. Of course, in many applications current feedback is intentionally employed, and the injection is made through the use of a separate control field winding. The overcompensated unit will display positive current feedback and the undercompensated unit will display negative current feed-back characteristics. Because the positive feedback may lead to oscillations, it is quite essential that the dynamoelectric amplifier be under or critically compensated for most applications.

The analysis of the external feed-back circuit is fairly straightforward and is developed in Appendix I for the case of voltage feedback. The voltage amplification for a 2-stage amplifier is

$$A=\frac{A_o}{1-\beta A_o} \tag{7}$$

where

$$A_o=\frac{N_{12}N_{23}Z_L}{Z_1Z_2(Z_3+Z_L)} \tag{8}$$

Equation 7 is the same type of expression as that used for electronic amplifiers.[8] Equation 6 gives the power amplification for this amplifier where the *A* is that defined by equation 7. As is shown in Appendix I, this amplifier may be unstable, but in the practical case the frequency and magnitude of the feed-back voltage required for sustained oscillation is well beyond the normal operating range. A series of calculated frequency response curves for a 3/4-kw 250-volt Amplidyne for various feed-back voltages are shown in Figure 7, and the phase characteristics are shown in Figure 5 with the test values being plotted as circles, triangles, and so forth. While these response curves are plotted in the customary fashion (that is, with 100 per cent for the zero-frequency point of all curves), it must be remembered that the power amplification is seriously impaired when feedback is used. A plot of the power amplification as a function of per cent negative feed-back voltage is shown in Figure 8. As was pointed out before, the frequency response is improved, and Figure 9 shows how the 70.7 per cent response can be extended by negative feedback. Hence one can extend the frequency response but it will be at the expense of the power amplification.

As was pointed out before, the analysis of the internal feed-back amplifier is considerably more involved than for the external feed-back case. The added complications are contributed by the mutual effects between the various field windings. The method of attack is very similar to the previous situations, that is, to write the voltage loop equations and to solve the resulting equations simultaneously. As an example, a 2-stage amplifier with internal feedback, Figure 11, is analyzed in Appendix II. It is theoretically possible to correlate equation 22 of Appendix II with the form of equation 7; however, β will no longer be a simple ratio, but will be a complex quantity. It is usually much easier to make a Nyquist plot to find when Δ_o vanishes to determine the point of instability.

Determination of Parameters

Thus far it has been assumed that all the necessary circuit parameters are known. The ensuing discussion suggests the means of finding the parameters. The parameters may be separated into four categories: the resistances (R_m); the self-inductances, (L_m); the mutual inductances (M_{mn}); and the speed transresistances (N_{mn}). The resistances of the field windings can be determined by the usual methods such as the bridge or the voltage drop methods. The armature resistance is a rather elusive quantity. At this stage the most reliable method

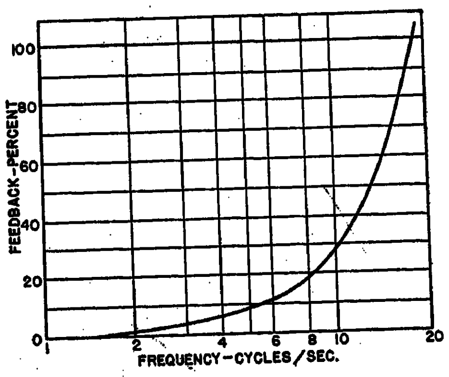


Figure 9. Variation of the 0.707 amplification point with frequency and feedback

seems to be to run the open and short-circuit tests described by Graybeal,[7] or to use data obtained from static tests. The self and mutual inductances can best be determined by means of 60-cycle impedance tests with the machine operating at normal speed somewhat as suggested by Bower.[10] Care must be exercised that the slot ripple voltage does not enter into the impedance calculations. If the slot ripple voltage is too large, it may be preferable to use static tests taking the average values for the various positions of the armature. The use of a rotating test procedure is considered superior since any short circuiting effects of the coils undergoing commutation then will be reflected in the inductances.

The determination of the transresistances has been described in the section on Static Characteristics.

Conclusions

1. The dynamoelectric amplifier has sufficient similarity to the electronic amplifier that the literature pertaining to the latter may be used to an advantage in predicting performance characteristics.
2. All the recent dynamoelectric amplifiers operating under class A conditions can be represented by an equivalent circuit containing resistances, self-inductances, mutual inductances, and transresistances.
3. The dynamic characteristics can be accurately predicted from a-c steady-state analysis using the equivalent circuits suggested before.
4. Negative feedback extends the frequency-response at the expense of amplification.
5. Either external or internal feedback may be employed.
6. The parameters can be determined from direct current and 60-cycle measurements with sufficient accuracy to use in the above equivalent circuits.

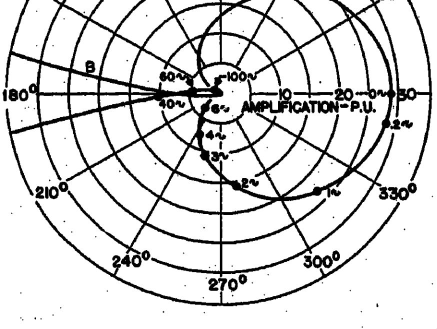


Figure 10. Nyquist plot of A_0 for a 3/4-kw 250-volt Amplidyne

Curve *A* employs the scale shown, and curve *B* is an enlarged portion of curve *A* as it crosses the 180 degree line. For values of curve *B*, multiply radial scale by 0.01

Appendix I

The analysis of an external feed-back circuit is based upon the insert diagram of Figure 7. If the impedance of the control field ($R_1+j\omega L_1$) is much greater than the impedance looking back at AA

$$E_L=\frac{N_{12}N_{23}Z_LE_1}{Z_1Z_2(Z_3+Z_L)\left(1-\beta\frac{N_{12}N_{23}Z_L}{Z_1Z_2(Z_3+Z_L)}\right)} \tag{9}$$

$$A=\frac{E_L}{E_1}=\frac{N_{12}N_{23}Z_L}{Z_1Z_2(Z_3+Z_L)\left(1-\beta\frac{N_{12}N_{23}Z_L}{Z_1Z_2(Z_3+Z_L)}\right)} \tag{10}$$

when

$\beta=0$

$$A_0=\frac{N_{12}N_{23}Z_L}{Z_1Z_2(Z_3+Z_L)} \tag{11}$$

so that

$$A=\frac{A_0}{1-\beta A_0} \tag{12}$$

The power amplification

$$B=\frac{|A|^2|Z_1|^2R_L}{R_1|Z_L|^2}=\frac{|A_0|^2|Z_1|^2R_L}{|(1-\beta A_0)|^2R_1|Z_L|^2} \tag{13}$$

The power amplification is reduced from that of the nonfeedback case since

$$\frac{|A_0|^2}{|(1-\beta A_0)|^2}<|A_0|^2 \text{ for } \beta<0 \tag{14}$$

The amplifier will be unstable and oscillate if

$$\beta A_0=1.0+j0 \tag{15}$$

The complex expression is used since A_0 is complex so that both the real and imaginary quantities must satisfy the explicit condition given. Since, for a given dynamoelectric amplifier, all the quantities comprising A_0 are known, a Nyquist plot of A_0 in the complex plane affords a convenient means of finding if the function βA_0 encloses the point 1, $j0$. The Nyquist plot for A_0 of a 3/4-kw 250-volt Amplidyne is shown in Figure 10. This plot shows an instability point for approximately 50 cycles but requires that β be a large negative number. The quantity A_0 is approximately 0.07 for instability so that β would have to be -142 to enclose 1, $j0$, a value well beyond the usually encountered. Hence, this particular amplifier will be very stable.

Appendix II

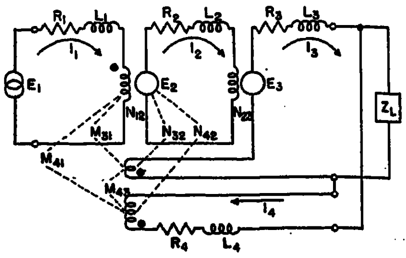


Figure 11. Circuit diagram for a dynamoelectric amplifier employing a separate control field for feedback

As an example of internal feedback, a dynamoelectric amplifier such as the Amplidyne or 2-stage Rototrol which is noncritically compensated (thus resulting in current feedback) and has a separate control field used for voltage feedback may be considered. Such an amplifier has mutual coupling between the fields and each field of its own accord will generate a voltage in the second stage. Assuming no mutual coupling between the second and first stages, the equivalent circuit for an amplifier of this type would be as in Figure 11. Writing voltage loop equations

$$E_L=(I_3-I_4)Z_L \tag{16}$$

$$E_1=I_1Z_1+jX_{31}I_3+jX_{41}I_4 \tag{17}$$

$$E_3=N_{23}I_2=\frac{N_{23}}{Z_2}(N_{12}I_1+N_{32}I_3+N_{42}I_4)$$
$$=I_3Z_3+E_L+jX_{32}I_1+jX_{42}I_4 \tag{18}$$

$$E_L=jX_{14}I_1+jX_{34}I_3+I_4Z_4 \tag{19}$$

The characteristic determinant is (after rearrangement)

$$\Delta_0=$$
$$\begin{vmatrix} -1, & \frac{N_{23}N_{12}}{Z_2}-jX_{13}, & \frac{N_{23}N_{32}}{Z_2}-Z_3, & \frac{N_{23}N_{42}}{Z_2}-jX_{43} \\ 0 & Z_1 & jX_{31} & jX_{41} \\ -1 & jX_{14} & jX_{34} & Z_4 \\ -1 & 0 & Z_L & -Z_L \end{vmatrix} \tag{20}$$

$$E_L=\frac{1}{\Delta_0}\times$$
$$\begin{vmatrix} E_1 & Z_1 & jX_{31} & jX_{41} \\ 0 & \frac{N_{23}N_{12}}{Z_2}-jX_{13}, & \frac{N_{23}N_{32}}{Z_2}-Z_3, & \frac{N_{23}N_{42}}{Z_2}-jX_{43} \\ 0 & jX_{14} & jX_{34} & Z_4 \\ 0 & 0 & Z_L & -Z_L \end{vmatrix} \tag{21}$$

$$A=\frac{E_L}{E_1}=\frac{1}{\Delta_0}\times$$
$$\begin{vmatrix} \frac{N_{23}N_{12}}{Z_2}-jX_{13}, & \frac{N_{23}N_{32}}{Z_2}-Z_3, & \frac{N_{23}N_{42}}{Z_2}-jX_{43} \\ jX_{14} & jX_{34} & Z_4 \\ 0 & Z_L & -Z_L \end{vmatrix} \tag{22}$$

The power amplification is again described by equation 6 where A is defined by equation 22. This amplifier will oscillate freely when $\Delta_0=0$.

References

1. American Standard Definitions of Electrical Terms. American Standards Association

(New York, N. Y.), 1942, paragraph 05.45.155.

2. Dynamoelectric Amplifier for Power Control, E. F. W. Alexanderson, M. A. Edwards, K. K. Bowman. *AIEE Transactions*, volume 59, 1940, pages 937–39.

3. The Design Characteristics of Amplidyne Generators, Alec Fisher. *AIEE Transactions*, volume 59, 1940, pages 939–944.

4. Rototrol—A Versatile Electric Regulator, W. H. Formahls. *Westinghouse Engineer* (East Pittsburgh, Pa.), May 1942, pages 51–54.

5. The Multistage Rototrol, M. M. Liwschitz. *AIEE Transactions*, volume 66, 1947, pages 564–68.

6. Two-Stage Rototrol for Low Energy Regulating Systems, A. W. Kimball. *AIEE Transactions*, volume 66, 1947, pages 1507–11.

7. Steady-State Theory of the Amplidyne Generator, T. D. Graybeal. *Electrical Engineering* (*AIEE Transactions*) volume 61, 1942, pages 750–56.

8. Radio Engineers' Handbook (book), F. E. Terman. McGraw-Hill Book Company, Inc., New York, N. Y., 1943, pages 395–406.

9. Network Analysis and Feedback Amplifier Design (book), H. W. Bode. D. Van Nostrand Company, New York, N. Y., 1945.

10. Fondamentals of the Amplidyne Generator, J. L. Bower. *AIEE Transactions*, volume 64, 1945, pages 873–81.

No Discussion

Design of Auxiliary Circuits of Single-Phase Induction Motors

M. S. THACKER
FELLOW AIEE

H. V. GOPALAKRISHNA
NONMEMBER AIEE

THE multiplicity of difficult industrial specifications for the starting performance of single-phase induction motors require equations to be developed in an ideal manner. In their AIEE paper of January 1944,[1] Lloyd and Karr have dealt with the design of starting windings of split-phase motors and plotted useful curves. And Veinott's AIEE paper of June 1944[2] presents calculation methods for the determination of the best starting winding and the best capacitor required to meet any specified requirement on the basis of the equation developed by Boothby[3] but with a slight modification. The methods outlined have merits; but a lengthy process of trial-and-error may lead to the desired result. The importance of accurate design methods to meet the increasing diversity and severity of the starting conditions especially in single-phase induction motors cannot be overemphasized.

In this paper, mathematical procedures are evolved to arrive at the best proportions of the auxiliary circuit constants for a given set of conditions on the basis of the equations developed by the method of symmetrical components in the paper entitled Suggested Improvements in the Performance Calculations of Single-Phase Induction Motors by the authors.[4]† Appendices III and IV indicate results by authors' method as compared with other methods. Tables I and II give the summary of results.

Table I contrasts the values obtained for a split-phase motor by the authors' method with those of Lloyd and Karr. According to the present investigation it is significant that for a given starting torque a smaller size of wire could be used for the starting winding; the magnitude of current in the auxiliary circuit as well as in the line also is lower.

Table II presents the design values for a capacitor-start motor and contrasts with Veinott's figures. The analysis of this paper yields a greater starting torque for a prescribed starting current in the line; or if the starting torque is the criterion in design a smaller size of capacitor could be employed with reduced line current.

It is believed that the methods presented in this paper will contribute to the most economical design of the auxiliary circuits of single-phase induction motors which are being manufactured in very large numbers.

Symbols and Factors

V_m = applied rms volts across main and start phases
I_m = main phase rms amperes
I_s = start phase rms amperes
$I_m{}^+$ = positive sequence component of I_m
$I_m{}^-$ = negative sequence component of I_m
$I_s{}^+$ = positive sequence component of I_s
$I_s{}^-$ = negative sequence component of I_s
$Z_m{}^+$ = total positive sequence impedance of M phase
$Z_m{}^-$ = total negative sequence impedance of M phase
$Z_s{}^+$ = total positive sequence impedance of S phase
$Z_s{}^-$ = total negative sequence impedance of S phase
X_{1m} = reactance of M phase primary winding
R_{1m} = resistance of M phase primary winding

$$a=\frac{\text{effective conductors of } S \text{ phase}}{\text{effective conductors of } M \text{ phase}}=a'\sqrt{\frac{X_s}{X_m}}$$

a^2R_{1s} = primary resistance of S phase
a^2X_{1s} = primary leakage reactance of S phase
R_c = capacitor resistance
X_c = capacitor reactance
R_2 = equivalent M phase secondary resistance referred to primary winding
X_2 = equivalent M phase secondary reactance referred to primary winding
$Z_2{}^{+1}$ = apparent secondary positive sequence impedance of M phase
$Z_2{}^{-1}$ = apparent secondary negative sequence impedance of M phase
$R_2{}^{+1}$ = apparent positive sequence M phase secondary resistance
$R_2{}^{-1}$ = apparent negative sequence M phase secondary resistance
$X_2{}^{+1}$ = apparent positive sequence M phase secondary reactance
$X_2{}^{-1}$ = apparent negative sequence M phase secondary reactance
$R_2{}''$ = standstill apparent secondary resistance
$X_2{}''$ = standstill apparent secondary reactance
$Z_2{}''$ = standstill apparent secondary impedance

$$b=\frac{R_\varphi{}^2+X_\varphi{}^2}{(R_2+R_\varphi)^2+(X_2+X_\varphi)^2}$$

R_φ = resistance representing core loss when connected in series with X_φ $=\dfrac{\text{core loss}}{i_m{}^2}$
X_φ = equivalent M phase mutual or magnetizing reactance
$X_0=X_\varphi+X_{1m}$ = reactance of the primary winding with the secondary open circuited
$i_m=\dfrac{V_m}{X_0}$ = primary current with the secondary open
R_m = real component of total impedance of M phase at standstill $=R_{1m}+R_2{}''$
X_m = j component of total impedance of M phase at standstill $=X_{1m}+X_2{}''$
$Z_m=R_m+jX_m$
$Z_m=\sqrt{R_m{}^2+X_m{}^2}$ = total impedance of the main winding at standstill $=K$
R_s = real component of total impedance of S phase at standstill $=a^2R_{1s}+a^2R_2{}''$
X_s = j component of total impedance of S

---

† This paper is scheduled for presentation at the AIEE Winter General Meeting, New York, N. Y., January 30–February 3, 1950, and should be available for distribution shortly thereafter.

Paper 49-296, recommended by the AIEE Rotating Machinery Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted August 23, 1948; made available for printing September 9, 1949.

M. S. Thacker and H. V. Gopalakrishna are both with the Indian Institute of Science, Bangalore, India.

Table I

Items	Maximum Torque Winding Lloyd and Karr Trial and Error Solutions					Authors' Method
Conductors, starting winding	1,056	1,056	1,056	850	850	1,091
Ratio, $1/a$	1.21	1.21	1.21	1.5	1.5	1.17
Resistance, starting winding	12.7	16.0	10.0	12.7	8.22	20.2
Starting winding current	6.53	5.55	7.55	7.4	10.13	4.6
Total starting amperes	14.4	13.3	15.45	13.96	17.9	12.35
Starting torque, ounce feet	16.80	16.10	16.15	19.2	20.6	15.0
Torque per ampere	1.165 (best solution)	1.208	1.042	1.286	1.15	12.15.

Table II

Items	Given: Line Current 24.8 Amperes		Given: Max. Start. Torque 82.5 ounce feet	
	Veinott's Maximum Torque/ampere Method	Authors' Maximum Torque Method	Veinott's Maximum Torque Method	Authors' Maximum Torque Method
Capacitance in microfarads	228	234	284	198
Total resistance of the auxiliary winding	8.5	4.63	8.5	6.6
Voltage across capacitor	118	115.9	104.2	138.1
Line current			27.1	23.03
Maximum starting torque ounce feet	78.5	111.7		
Maximum starting torque per ampere of line current	3.16	4.5	3.04	3.58

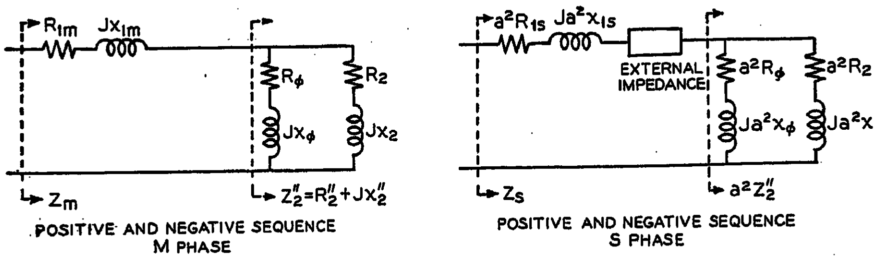


Figure 1. Equivalent circuits of a single-phase induction motor at standstill

External impedance: for capacitor motor $= R_c - jX_c$; for split-phase motor $=0$

phase at standstill

$=a^2X_{1s}+a^2X_2''$

$Z_s=R_s+jX_s$

$Z_s=\sqrt{R_s^2+X_s^2}$ = total impedance of the auxiliary winding at standstill

R_{sc} = real component of total impedance of the auxiliary winding with capacitor at standstill

$=R_c+R_s=R_c+a^2R_{1s}+a^2R_2''$

$X_{sc}=j$ component of total impedance of the auxiliary winding with capacitor at standstill

$=-X_c+X_s=-X_c+a^2X_{1s}+a^2X_2''$

$Z_{sc}=R_{sc}+jX_{sc}$

$Z_{sc}=\sqrt{R_{sc}^2+X_{sc}^2}$ = total impedance of the auxiliary winding with capacitor at standstill

θ = time angle, I_m and I_s of split-phase motors

φ = time angle, I_m and I_s of capacitor-start motors

The equivalent circuits of a single phase induction motor at standstill conditions are represented by Figure 1. These circuits and the following equations from 1 to 7 are derived by substituting $s=1$ in the corresponding equivalent circuits of Figure 3 and equations (numbered with*) of the paper entitled Suggested Improvements in the Performance Calculations of Single-Phase Induction Motors by the authors.[4]

Starting Performance

Under starting conditions the apparent secondary resistance of M phase at standstill is

$$R_2''=R_2^{+1}=R_2^{-1}=\frac{(R_2R_\varphi-X_2X_\varphi)(R_2+R_\varphi)+(R_2X_\varphi+X_2R_\varphi)(X_2+X_\varphi)}{(R_2+R_\varphi)^2+(X_2+X_\varphi)^2} \quad (1) \quad (10)^*(13)^*$$

and the apparent secondary reactance of M phase at standstill is

$$X_2''=X_2^{+1}=X_2^{-1}=\frac{(R_2X_\varphi+X_2R_\varphi)(R_2+R_\varphi)-(R_2R_\varphi-X_2X_\varphi)(X_2+X_\varphi)}{(R_2+R_\varphi)^2+(X_2+X_\varphi)^2} \quad (2) \quad (11)^*(14)^*$$

The total impedance of the M phase at standstill is

$$Z_m=Z_m^+=Z_m^-=(R_{1m}+R_2'')+j(X_{1m}+X_2'')=R_m+jX_m \quad (3)(15)^*(16)^*$$

The total impedance of the S phase at standstill is

$$Z_{sc}=Z_s^+=Z_s^-=(R_c+a^2R_{1s}+a^2R_2'')+j(-X_c+a^2X_{1s}+a^2X_2'') \quad (4)$$

$$=R_{sc}+jX_{sc} \quad (17)^*(18)^*$$

The starting torque in synchronous watts is obtained from equation 35*, so that

$$T_{st}=2bR_2[|I_m^+|^2-|I_m^-|^2] \quad (5)$$

where

$$b=\frac{R_\varphi^2+X_\varphi^2}{(R_2+R_\varphi)^2+(X_2+X_\varphi)^2}$$

The positive and negative sequence currents of the main winding are found from equations 23* and 24* in conjunction with equations 3 and 4. Thus

$$I_m^+=\frac{V_m}{2}\,\frac{(R_{sc}+aX_m)+j(X_{sc}-aR_m)}{(R_mR_{sc}-X_mX_{sc})+j(R_mX_{sc}+X_mR_{sc})} \quad (6)$$

$$I_m^-=\frac{V_m}{2}\times\frac{(R_{sc}-aX_m)+j(X_{sc}+aR_m)}{(R_mR_{sc}-X_mS_{sc})+j(R_mX_{sc}+X_mR_{sc})} \quad (7)$$

giving

$$|I_m^+|^2-|I_m^-|^2=aV_m^2\,\frac{R_{sc}X_m-X_{sc}R_m}{Z_m^2Z_{sc}^2}$$

Equation 5 for the starting torque of capacitor start motors now may be written as

$$T_{st}=\frac{2abR_2V_m^2(R_{sc}X_m-X_{sc}R_m)}{Z_m^2Z_{sc}^2} \quad (8)$$

In Appendix I the starting conditions for a capacitor motor are calculated and compared with the double revolving field and cross field methods.

If core loss is neglected

$$T_{st}=\frac{2aR_2''V_m^2(R_{sc}X_m-X_{sc}R_m)}{Z_m^2Z_{sc}^2} \quad (9)$$

This equation is identical with the equation 103 of Veinott's paper[2] (see also Appendix II) which is a modification of the original equation given by Boothby[3] and also with equation 14 of Morrill's[12] paper.

For split-phase motors $R_c=0$ and $X_c=0$. Deleting the subscript c in equation 9, the starting torque for split-phase motors

$$T_{st}=\frac{2abR_2V_m^2(R_sX_m-X_sR_m)}{Z_m^2Z_s^2} \quad (10)$$

Neglecting core loss

$$T_{st}=\frac{2aR_2''V_m^2(R_sX_m-X_sR_m)}{Z_m^2Z_s^2} \quad (11)$$

The locked-rotor current in the line

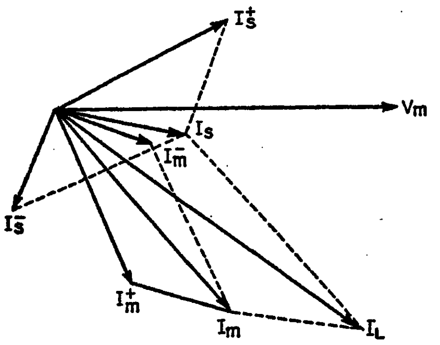


Figure 2. Vector relationships at instant of starting in a split-phase motor

being the vector sum of the main and start phase currents, Figures 2 and 3, is

$$I_L=I_m+I_s=I_m{}^++I_m{}^-+I_s{}^++I_s{}^-$$
$$=I_m{}^++I_m{}^-+\frac{j}{a}I_m{}^+-\frac{j}{a}I_m{}^-$$
$$=V_m\frac{(R_{sc}+R_m)+j(X_{sc}+X_m)}{(R_mR_{sc}-X_mX_{sc})+j(R_mX_{sc}+X_mR_{sc})}$$

from equations 6 and 7

Therefore for capacitor motors

$$|I_L|=V_m\frac{\sqrt{(R_{sc}+R_m)^2+(X_{sc}+X_m)^2}}{Z_mZ_{sc}} \quad (12)$$

and for split-phase motors

$$|I_L|=V_m\frac{\sqrt{(R_s+R_m)^2+(X_s+X_m)^2}}{Z_mZ_s} \quad (13)$$

Equations 12 and 13 are convenient expressions for calculating the line current.

The starting torque per ampere of line current is obtained by dividing equation 8 by equation 12 for capacitor motors so that

$$T_{st}/I_L=\frac{2abR_2V_m(R_{sc}X_m-X_{sc}R_m)}{Z_mZ_{sc}\sqrt{(R_{sc}+R_m)^2+(X_{sc}+X_m)^2}} \quad (14)$$

Neglecting core loss

$$T_{st}/I_L=\frac{2aR_2''V_m(R_{sc}X_m-X_{sc}R_m)}{Z_mZ_{sc}\sqrt{(R_{sc}+R_m)^2+(X_{sc}+X_m)^2}} \quad (15)$$

The starting torque per ampere of line current for split-phase motors is obtained by dividing equation 10 by equation 13 so that

$$T_{st}/I_L=\frac{2abR_2V_m(R_sX_m-X_sR_m)}{Z_mZ_s\sqrt{(R_s+R_m)^2+(X_s+X_m)^2}} \quad (16)$$

Neglecting core loss

$$T_{st}/I_L=\frac{2aR_2''V_m(R_sX_m-X_sR_m)}{Z_mZ_s\sqrt{(R_s+R_m)^2+(X_s+X_m)^2}} \quad (17)$$

The voltage across thc eapacitor E_c

$$=I_s\sqrt{R_c{}^2+X_c{}^2}=\frac{V_m}{Z_{sc}}\sqrt{R_c{}^2+X_c{}^2} \quad (18)$$

Optimum Auxiliary Circuit Constants

The main winding is designed by efficiency and breakdown torque considerations and the problem now is to find the best possible companion starting winding to produce the maximum starting torque, and the maximum starting torque per ampere of line current for single-phase induction motors.

MAXIMUM STARTING TORQUE WINDING FOR SPLIT-PHASE MOTORS

The winding ratio a may be considered as equal to

$$a'\sqrt{\frac{X_s}{X_m}}$$

where a' is a constant.

Equation 10 may be written as

$$T_{st}=\frac{2bR_2V_m{}^2}{Z_m{}^2}a\frac{R_sX_m-X_sR_m}{R_s{}^2+X_s{}^2}$$
$$=\frac{2a'bR_2V_m{}^2\sqrt{X_s}}{Z_m{}^2\sqrt{X_m}}\left[\frac{R_sX_m-X_sR_m}{R_s{}^2+X_s{}^2}\right]$$
$$=\text{constant }\sqrt{X_s}\frac{R_sX_m-X_sR_m}{R_s{}^2+X_s{}^2} \quad (19)$$

The standstill resistance R_m and reactance X_m are known for a given main winding. The standstill resistance R_s and reactance X_s of the starting winding will have to be specified in terms of the constants of the main winding to produce maximum torque. The magnitude of the current in the starting winding may be arbitrarily fixed up thereby fixing the total impedance $Z_s=\rho$ of the starting winding and its position varied. The problem, therefore, reduces to investigating the maximum value of

$$T_{st}\propto\alpha\sqrt{X_s}\frac{R_sX_m-X_sR_m}{R_s{}^2+X_s{}^2} \quad (20)$$

T_{st} being positive, R_s and X_s being variables, with the condition

$$R_s{}^2+X_s{}^2=\rho^2$$

being constant.
Now from equation 19

$$T_{st}{}^2=\text{ constant }X_s\frac{(R_sX_m-X_sR_m)^2}{(R_s{}^2+X_s^2)^2=\rho^4}$$

Let

$$R_m{}^2+X_m{}^2=Z_m{}^2=K^2$$

Putting

$$R_sX_m-X_sR_m=K\rho\sin\theta \quad (21)$$

and

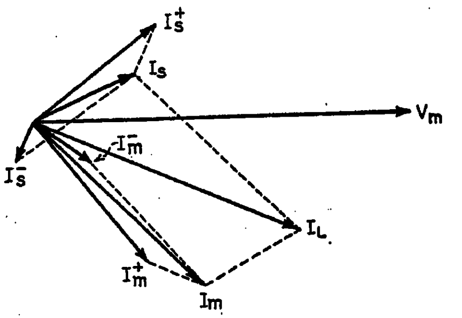


Figure 3. Vector relationships at instant of starting in a capacitor motor

$$X_sX_m+R_sR_m=K\rho\cos\theta \quad (22)$$

and solving for R_s and X_s from these, the following equations are obtained

$$R_s=\frac{\rho}{K}(X_m\sin\theta+R_m\cos\theta) \quad (23)$$

$$X_s=\frac{\rho}{K}(X_m\cos\theta-R_m\sin\theta) \quad (24)$$

The problem modifies to finding the maximum value of

$$(X_m\cos\theta-R_m\sin\theta)\sin^2\theta$$

Taking logarithms and differentiating and equating to zero gives

$$\frac{\varphi'(\theta)}{\varphi(\theta)}=\frac{2\cos\theta}{\sin\theta}-\frac{R_m\cos\theta+X_m\sin\theta}{X_m\cos\theta-R_m\sin\theta}=0$$

Or

$$2(X_m-R_m\tan\theta)-\tan\theta(R_m+X_m\tan\theta)=\theta$$

that is

$$\tan^2\theta+3\alpha\tan\theta-2=0$$

where

$$\alpha=\frac{R_m}{X_m}$$

that is

$$\tan\theta=\frac{-3\alpha+\sqrt{9\alpha^2+8}}{2} \quad (25)$$

It is, therefore, clear that for all maximum torque windings the phase displacement θ, Figure 4, between the main and auxiliary circuit currents is fixed; it is independent of the constants of the auxiliary winding. For the value of θ lying between zero and $\pi/2$ both X_s and $R_sX_m-X_sR_m$ will be positive and the values of R_s and X_s are determined from equations 23 and 24, which completely specify the auxiliary winding that gives the maximum starting torque for a given main winding. ρ could be determined easily to give the desired total locked-rotor current thus:

Referring to Figure 4, the total locked-rotor current in the line is

$$I_L=\sqrt{I_m{}^2+I_s{}^2+2I_mI_s\cos\theta} \quad (26)$$

The current in the starting winding is therefore

$$I_s = -I_m \cos\theta + \sqrt{I_L^2 - I_m^2 \sin^2\theta}$$

The expression for the total impedance of the starting winding thus becomes

$$\rho = Z_s = \frac{V_m}{I_s} = \frac{V_m}{-I_m \cos\theta + \sqrt{I_L^2 - I_m^2 \sin^2\theta}} \qquad (27)$$

therefore

$$T_{st\ \max} = \frac{2a'bR_2 V_m^2 \sin\theta}{Z_m^{3/2}} \times \sqrt{\frac{X_m \cos\theta - R_m \sin\theta}{\rho X_m}} \qquad (28)$$

It may be noticed that for a given starting torque θ is definite and hence R_s and X_s.

If core loss is neglected

$$T_{st\ \max} = \frac{2a'R_2'' V_m^2 \sin\theta}{Z_m^{3/2}} \times \sqrt{\frac{X_m \cos\theta - R_m \sin\theta}{\rho X_m}} \qquad (29)$$

The procedure for determining the starting winding constants for a given main winding to meet a specified starting torque, and a specified starting current, is as follows:

For a Specified Starting Torque (See Appendix III):

1. $I_m = (V_m)/(Z_m = K)$
2. Calculate θ from equation 25.
3. Substitute the value of θ in equation 28 and get the value of ρ.
4. Determine R_s and X_s from equations 23 and 24.
5. $I_s = (V_m)/(Z_s = \rho)$
6. Calculate the value of I_L from equation 26 and check with the limit of total locked-rotor current prescribed by rules.

For a Specified Starting Current:

1. $I_m = (V_m)/(Z_m = K)$
2. Calculate θ from equation 25.
3. Substitute the value of θ in equation 27 and get the value of ρ.
4. Determine R_s and X_s from equations 23 and 24.
5. Substitute the above values in equation 28 and check with the minimum value of starting torque prescribed by rules.

CONDITIONS FOR MAXIMUM STARTING TORQUE PER AMPERE OF LINE CURRENT FOR SPLIT-PHASE MOTORS

The expression for the starting torque per ampere of line current may be written in the form

$$T_{st}/I_L = \text{constant} \times \sqrt{X_s} \sqrt{\frac{R_sX_m - X_sR_m}{(R_s^2+X_s^2)(\overline{R_s+R_m}^2 + \overline{X_s+X_m}^2)}} \qquad (30)$$

R_s and X_s being the variables. As in the previous problem the following relations hold good

$$R_s^2 + X_s^2 = \rho^2; \quad R_m^2 + X_m^2 = K^2$$
$$R_sX_m - X_sR_m = K\rho \sin\theta$$
$$X_sX_m + R_sR_m = K\rho \cos\theta$$

Solving for R_s and X_s yields

$$KR_s = \rho(X_m \sin\theta + R_m \cos\theta) \qquad (31)$$

$$KX_s = \rho(X_m \cos\theta - R_m \sin\theta) \qquad (32)$$

and

$$(R_m + R_s)^2 + (X_m + X_s)^2 = K^2 + \rho^2 + 2K\rho \cos\theta \qquad (33)$$

The problem reduces to that of finding the maximum, when ρ and θ vary, of

$$(\rho,\theta) = \frac{\sin\theta (X_m \cos\theta - R_m \sin\theta)^{1/2} \rho^{1/2}}{(K^2 + \rho^2 + 2K\rho \cos\theta)^{1/2}} \qquad (34)$$

On differentiating $\log f(\rho,\theta)$ with respect to ρ and θ and equating the expressions to zero gives

$$\frac{1}{2\rho} - \frac{2(\rho + K \cos\theta)}{2(\rho^2 + K^2 + 2K\rho \cos\theta)} = 0 \qquad (35)$$

and

$$\frac{2\cos\theta}{\sin\theta} - \frac{X_m \sin\theta + R_m \cos\theta}{X_m \cos\theta - R_m \sin\theta} + \frac{2K\rho \sin\theta}{\rho^2 + K^2 + 2K\rho \cos\theta} = 0 \qquad (36)$$

Simplifying equation 35

$$\rho^2 + K^2 + 2K\rho \cos\theta - 2\rho^2 - 2K\rho \cos\theta = 0$$

or

$$\rho = K$$

that is

$$R_m^2 + X_m^2 = R_s^2 + X_s^2 \qquad (37)$$

Substituting $\rho = K$ in equation 36 and simplifying results in

$$2\cot\theta - \frac{X_m \tan\theta + R_m}{X_m - R_m \tan\theta} + \frac{\sin\theta}{1 + \cos\theta} = 0$$

that is,

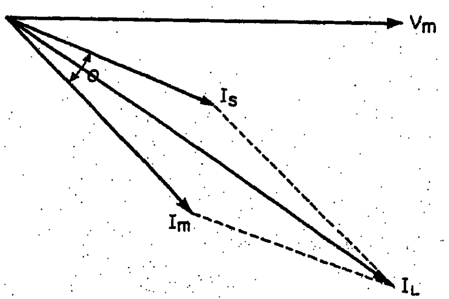


Figure 4. Maximum starting-torque diagram for a split-phase motor. θ is determined by the main winding constants—equation 25

$$\frac{2}{\tan\theta} - \frac{X_m \tan\theta + R_m}{X_m - R_m \tan\theta} + \tan\frac{\theta}{2} = 0 \qquad (38)$$

Put $t = \tan\theta/2$, then $\tan\theta = (2t)/(1-t^2)$. Therefore equation 38 can be written as

$$\frac{1-t^2}{t} + t - \frac{2tX_m + R_m(1-t^2)}{X_m(1-t^2) - 2R_mt} = 0$$

Or

$$-2t^2X_m - R_mt + R_mt^3 + X_m - t^2X_m - 2R_mt = 0$$

Or

$$R_mt^3 - 3X_mt^2 - 3R_mt + X_m = 0$$

Let

$$\frac{R_m}{X_m} = \alpha$$

as before then $\tan\theta/2 = t$ satisfies the equation

$$\varphi(t) = t^3 - \frac{3}{\alpha} t^2 - 3t + \frac{1}{\alpha} = 0 \qquad (39)$$

As it has two changes of sign it has at most two positive roots, and

$$\varphi(0) = \frac{1}{\alpha} > 0$$

$$\varphi(1) = 1 - \frac{3}{\alpha} - 3 + \frac{1}{\alpha} = -2\left(1 + \frac{1}{\alpha}\right) < 0$$

Hence it has exactly one root in (0,1), another in (1, ∞), and another negative root. For the value of $t = \tan\theta/2$ in (0,1), T_{st}/I_L will be maximum. From the form of $f(\rho,\theta)$ for $\rho = K$ it is clear that this gives the maximum value of T_{st}/I_L and for this value X_s is really positive. The solution of equation 39 is obtained by writing in the form

$$\left(t - \frac{1}{\alpha}\right)^3 - \frac{3t}{\alpha^2} - 3t + \frac{1+\alpha^2}{\alpha^3} = 0$$

Or

$$\left(t - \frac{1}{\alpha}\right)^3 - \frac{3(1+\alpha^2)}{\alpha^2}\left(t - \frac{1}{\alpha}\right) - \frac{2(1+\alpha^2)}{\alpha^3} = 0 \qquad (40)$$

Put

$$\left(t - \frac{1}{\alpha}\right) = \beta \cos\frac{\psi}{3}$$

Then equation 40 assumes the form

$$\beta^3 \cos^3\frac{\psi}{3} - \frac{3(1+\alpha^2)\beta}{\alpha^2} \cos\frac{\psi}{3} - \frac{2(1+\alpha^2)}{\alpha^3} = 0 \qquad (41)$$

Multiplying by $4/\beta^3$

$$4\cos^3\frac{\psi}{3} - \frac{12(1+\alpha^2)}{\alpha^2\beta^2} \cos\frac{\psi}{3} - \frac{8(1+\alpha^2)}{\alpha^3\beta^3} = 0 \qquad (42)$$

Let β be such that

$$\frac{4(1+\alpha^2)}{\alpha^2\beta^2} = 1 \text{ or } \beta = \frac{2\sqrt{1+\alpha^2}}{\alpha}$$

Then equation 42 becomes

$$4\cos^3\frac{\psi}{3}-3\cos\frac{\psi}{3}-\frac{8(1+\alpha^2)\alpha^3}{\alpha^3 8(1+\alpha^2)^{3/2}}=0$$

or

$$\cos\psi=\frac{1}{\sqrt{1+\alpha^2}}=\frac{X_m}{\sqrt{R_m^2+X_m^2}} \tag{43}$$

Obviously

$$\psi=\tan^{-1}\alpha=\tan^{-1}\frac{R_m}{X_m}$$

ρ is calculated from

$$\tan\frac{\theta}{2}=t=\frac{1}{\alpha}+\beta\cos\frac{\psi}{3}=\frac{1}{\alpha}+\frac{2\sqrt{1+\alpha^2}\cos\frac{\psi}{3}}{\alpha} \tag{44}$$

Substitute instead of $\psi/3$, $\psi/3+120$ degrees or $\psi/3+240$ degrees, so that $\tan\theta/2$ lies in (0,1). The values of R_s and X_s are obtained by substituting θ in equations 31 and 32 and since $\rho=K$

$$R_s=X_m\sin\theta+R_m\cos\theta \tag{45}$$

$$X_s=X_m\cos\theta-R_m\sin\theta \tag{46}$$

The expression for the maximum starting torque per ampere of line current becomes

$$(T_{st}/I_L)_{\max}=\frac{a'bR_2V_m\sin\theta}{Z_m}\times\sqrt{\frac{2(X_m\cos^{*}\theta-R_m\sin\theta)}{X_m(1+\cos\theta)}} \tag{47}$$

If core loss is neglected

$$(T_{st}/I_L)_{\max}=\frac{a'R_2''V_m\sin\theta}{Z_m}\times\sqrt{\frac{2(X_m\cos\theta-R_m\sin\theta)}{X_m(1+\cos\theta)}} \tag{48}$$

Substituting the values of R_s and X_s from equations 45 and 46 in equation 19 for the expression of the starting torque, the following relation is obtained

$$T_{st}=\frac{2a'bR_2V_m^2\sin\theta}{Z_m^2}\times\sqrt{\frac{X_m\cos\theta-R_m\sin\theta}{X_m}} \tag{49}$$

Neglecting core loss

$$T_{st}=\frac{2a'R_2''V_m^2\sin\theta}{Z_m^2}\times\sqrt{\frac{X_m\cos\theta-R_m\sin\theta}{X_m}} \tag{50}$$

CONDITIONS FOR MAXIMUM STARTING TORQUE OF CAPACITORS START MOTORS

Depending on the voltage the capacitor is able to withstand a certain number of turns and distribution of the starting winding is assumed fixing the reactance X_s. The current in the starting winding may be arbitrarily fixed up thereby fixing the locked-rotor impedance of the starting winding, $Z_{sc}=\rho_1$.

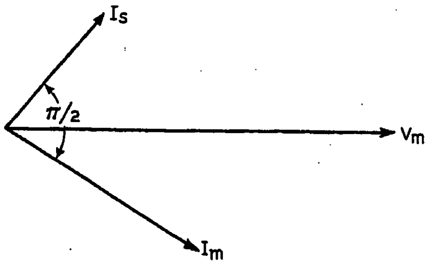


Figure 5. Maximum starting-torque diagram for a capacitor-start motor

The expression for the starting torque may be written as

$$T_{st}=\text{constant}\ \frac{R_{sc}X_m-X_{sc}R_m}{R_{sc}^2+X_{sc}^2}$$

$$=\text{constant}\times\frac{(R_c+R_s)X_m-(-X_c+X_s)R_m}{(R_c+R_s)^2+(-X_c+X_s)^2} \tag{51}$$

Taking

$$(R_c+R_s)X_m-R_m(-X_c+X_s)=K\rho_1\sin\varphi \tag{52}$$

$$(-X_c+X_s)X_m+R_m(R_c+R_s)=K\rho_1\cos\varphi \tag{53}$$

where

$$R_m^2+X_m^2=Z_m^2=K^2$$

The problem now reduces to that of finding the maximum value of

$$K\rho_1\sin\varphi \tag{54}$$

The maximum obviously occurs when $\varphi=\pi/2$, Figure 5. Substituting this in equations 52 and 53

$$(R_c+R_s)X_m-R_m(-X_c+X_s)=K\rho_1 \tag{55}$$

$$(-X_c+X_s)X_m+R_m(R_c+R_s)=0 \tag{56}$$

yielding

$$X_c=X_s+\frac{\rho_1}{K}R_m=\frac{10^6}{2\pi fc} \tag{57}$$

where

c = microfarads
f = frequency

and

$$R_s=\frac{\rho_1}{K}X_m-R_c \tag{58}$$

giving the total resistance of the starting winding; the capacitor resistance R_c is treated as a known quantity.

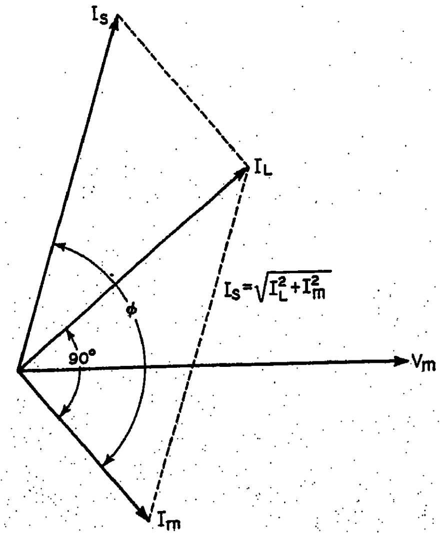


Figure 6. Maximum starting-torque per ampere diagram for a capacitor-start motor

The expression for the maximum starting torque may be written as

$$T_{st\,\max}=\frac{2abR_2V_m^2}{Z_m\rho_1} \tag{59}$$

Neglecting core loss

$$T_{st\,\max}=\frac{2aR_2''V_m^2}{Z_m\rho_1} \tag{60}$$

Then the starting torque per ampere of line current is

$$T_{st\,\max}/I_L=\frac{2abR_2V_m}{\sqrt{Z_m^2+\rho_1^2}} \tag{61}$$

Neglecting core loss

$$T_{st\,\max}/I_L=\frac{2aR_2''V_m}{\sqrt{Z_m^2+\rho_1^2}} \tag{62}$$

For a given locked-rotor current in the line I_L, the current in the start phase is

$$I_s=\sqrt{I_L^2-I_m^2} \tag{63}$$

so that

$$\rho_1=Z_{sc}=\frac{V_m}{\sqrt{I_L^2-I_m^2}} \tag{64}$$

For a given maximum starting torque ρ_1 is definite. Equations 57 and 58 will give the required X_c and R_s.

The procedure for determining the starting winding constants of capacitor start motors (See Appendix IV), X_s being assumed on considerations already indicated, is as follows:

For a Given Line Current I_L:

1. Calculate

$$I_m=\frac{V_m}{Z_m},\ I_s=\sqrt{I_L^2-I_m^2}\ \text{and}\ \rho_1=\frac{V_m}{I_s}$$

2. Find X_c and R_s from equations 57 and 58.

3. $C_{\text{mfd}}=\dfrac{10^6}{2\pi fX_c}$

4. $E_c=I_s\sqrt{R_c^2+X_c^2}$

5. Find the maximum starting torque from equations 59 or 60 and the starting

torque per ampere of line current from equations 61 or 62.

For a Given Maximum Starting Torque $T_{st\,\max}$:

Calculate ρ_1 from equation 59 or 60. The other items are calculated as in the previous case.

CONDITIONS FOR MAXIMUM STARTING TORQUE PER AMPERE OF LINE CURRENT FOR CAPACITOR START MOTORS

The expression for the starting torque per ampere of line current is

$$T_{st}/I_L=\text{constant}\times\frac{R_{sc}X_m-X_{sc}R_m}{Z_{sc}\sqrt{(R_{sc}+R_m)^2+(X_{sc}+X_m)^2}}$$

$$=\text{constant}\times\frac{(R_c+R_s)X_m-(-X_c+X_s)R_m}{\sqrt{\begin{matrix}[(R_c+R_s)^2+(-X_c+X_s)^2]\\ [(R_c+R_s+R_m)^2+(-X_c+X_s+X_m)^2]\end{matrix}}} \tag{65}$$

As in the previous problem the following relations may be written

$$R_m^2+X_m^2=Z_m^2=K^2$$

$$(R_c+R_s)^2+(-X_c+X_s)^2=\rho_1^2$$

also

$$(R_c+R_s)X_m-(-X_c+X_s)R_m=K\rho_1\sin\varphi$$

$$(-X_c+X_s)X_m+(R_c+R_s)R_m=K\rho_1\cos\varphi$$

Solving for (R_c+R_s) and $(-X_c+X_s)$ the following are obtained

$$(R_c+R_s)=\frac{\rho_1}{K}(X_m\sin\varphi+R_m\cos\varphi) \tag{66}$$

$$(-X_c+X_s)=\frac{\rho_1}{K}(X_m\cos\varphi-R_m\sin\varphi) \tag{67}$$

and

$$(R_c+R_s+R_m)^2+(-X_c+X_s+X_m)^2=K^2+\rho_1^2+2K\rho_1\cos\varphi \tag{68}$$

The problem reduces to that of finding the maximum value when φ varies, of the following expression

$$\xi(\rho_1,\varphi)=\frac{\sin\varphi}{\sqrt{\rho_1^2+K^2+2K\rho_1\cos\varphi}} \tag{69}$$

where $0<\varphi<\pi$.

Differentiating log $\xi(\rho_1,\varphi)$ with respect to ρ_1 and φ and equating the expressions to zero gives

$$-\frac{2(\rho_1+K\cos\varphi)}{2(\rho_1^2+K^2+2K\rho_1\cos\varphi)}=0 \tag{70}$$

and

$$\frac{\cos\varphi}{\sin\varphi}+\frac{2K\rho_1\sin\varphi}{2(\rho_1^2+K^2+2K\rho_1\cos\varphi)}=0 \tag{71}$$

Simplifying equation 70 results in

$$\cos\varphi=-\frac{\rho_1}{K} \tag{72}$$

This value satisfies the equation 71 and from the form of $\xi(\rho_1,\varphi)$ it is evident that this gives the maximum value. Equations 66 and 67 yield

$$R_s=\frac{\rho_1}{K^2}[X_m\sqrt{K^2-\rho_1^2}-\rho_1R_m]-R_c \tag{73}$$

$$X_c=\frac{\rho_1}{K^2}[R_m\sqrt{K^2-\rho_1^2}+\rho_1X_m]+X_s \tag{74}$$

These two equations completely define the auxiliary circuit.

From the foregoing, it follows that the expression for the maximum starting torque per ampere of line current may be put down as

$$(T_{st}/I_L)_{\max}=\frac{2abR_2V_m}{Z_m} \tag{75}$$

Neglecting core loss

$$(T_{st}/I_L)_{\max}=\frac{2aR_2''V_m}{Z_m} \tag{76}$$

The starting torque is given by

$$T_{st}=\frac{2abR_2V_m^2\sqrt{Z_m^2-\rho_1^2}}{Z_m^2\rho_1} \tag{77}$$

Neglecting core loss

$$T_{st}=\frac{2aR_2''V_m^2\sqrt{Z_m^2-\rho_1^2}}{Z_m^2\rho_1} \tag{78}$$

For a given locked-rotor current in the line I_L, the current in the start phase is expressed by the relation

$$I_s=\sqrt{I_L^2+I_m^2} \tag{79}$$

so that

$$\rho_1=Z_{sc}=\frac{V_m}{\sqrt{I_L^2+I_m^2}} \tag{80}$$

The time angle between the line current and the main phase current is 90 degrees, the starting conditions being represented by Figure 6.

Appendix I

The starting performance calculations on a capacitor motor for the same motor for which constants are given in Morrill's paper[12] with some additional items as given by Puchstein and Lloyd.[9] The values for this 4-pole 1/4-horsepower 110-volt motor are

$R_{1m}=2.02$; $a^2R_{1S}=1.39\times5.12=7.13$; $R_2=4.12$; R_c(at start)$=3$; X_c(at start)$=14.5$; $X_{1m}=2.79$; $a^2X_{1s}=1.39\times2.31=3.22$; $X_2=2.12$; $a=1.18$; $a^2=1.39$; $X_\varphi=66.8$; iron losses$=24$ watts

Calculations

$X_o=69.59$; $i_m=1.58$; $R_\varphi=9.6$

Equations 1 and 2 give
$R_2''=3.84$ and $X_2''=2.25$

From equations 3 and 4

$$Z_m=5.86+j5.04\quad I_m=\frac{V_m}{Z_m}=14.23\ \overline{\diagdown 40^\circ 42'}$$

$$Z_{sc}=15.47-j8.15\quad I_s=\frac{V_m}{Z_{sc}}=6.29\ \underline{/27^\circ 49'}$$

Substituting in equation 8 $T_{st}=749$ synchronous watts, see Table III.

Appendix II

Example of Starting Torque Calculations

Machine data are taken from the example given by Veinott[2] for a capacitor start motor, 115 volts, 4 poles 60 cycles. In the notations of this paper they become

$R_{1m}=1.47$; $a^2R_2''=3.67$; $X_c=11.62$; $X_m=3.72$; $a^2R_{1s}=4.83$; $a=1.172$; $Z_m=5.57$; $R_c=0.98$; $R_2''=2.67$; $a^2(X_{1s}+X_2'')=X_s=5.14$

Then

$R_{sc}=9.43$; $X_{sc}=-6.48$; $R_m=4.14$

Equation $9\times\frac{112.7}{\text{synchronous rpm }1{,}800}$ gives

$T_{st}=78.7$ ounce feet (same as Veinott's result)

Equation $15\times\frac{112.7}{\text{synchronous rpm }1{,}800}$ gives

$Tst/I_L=3.15$ ounce feet (same as Veinott's result)

Appendix III

Example of Maximum Starting Torque Calculations

Machine data are taken from the example given by Lloyd and Karr[1] for a split-phase induction motor.

A starting winding is to be designed for a motor rated 1/8th horsepower, 60 cycles, 1,150 rpm, 115 volts. The following con-

Table III. Starting Conditions

Method	I_m		I_s		T_{st} in Synchronous Watts
Double revolving field (Morrill)	14.2	$\overline{\diagdown 40^\circ 50'}$	6.30	$\underline{/27^\circ 55'}$	755
Cross field (Puchstein and Lloyd)	14.2	$\overline{\diagdown 40^\circ 0'}$	6.25	$\underline{/27^\circ 50'}$	759
Symmetrical components	14.23	$\overline{\diagdown 40^\circ 42'}$	6.29	$\underline{/27^\circ 49'}$	749

stants are known. In the symbols of this paper they become

$R_m=9.26$; $X_m=10.7$; $R_2''=4.85$

Series turns in main winding = 1,248; winding factor for main winding = 0.856; and winding factor for the auxiliary winding = 0.837. The starting torque is to be 15 ounce-feet and the starting current limited to 15 amperes.
Then

$$Z_m=14.15 \qquad I_m=8.13$$
$$\theta=30°58'$$

Assume that the ratio of transformation obtained from the effective turns is exactly the same as the ratio obtained from the leakage reactances, that is, $a'=1$. Multiplying the right-hand side of equation 29 by 112.7/1,200 to convert synchronous watts into ounce feet and inserting the foregoing values in the equation yields $\rho_1=25.0$.

Equations 23 and 24 give $R_s=23.74$ and $X_s=7.80$, also

$$\frac{1}{a}=\sqrt{\frac{X_m}{X_s}}=1.17.$$

The resistance of the starting winding = $R_s-a^2R_2''=20.2$; $I_s=4.6$. From equation 26 $I_L=12.35$.

Therefore,

$$\text{starting torque per ampere}=\frac{15}{12.35}=1.215$$

ounce feet

$$\text{Starting winding conductors}=\frac{0.856\times 1{,}248}{0.837\times 1.17}$$

= 1,091.

Appendix IV

Determination of the Optimum Auxiliary Circuit Constants of a Capacitor Start Motor

Machine data are taken out from the example given in Appendix II.

Maximum Starting Torque Winding

For a given line current $I_L=24.8$; $I_m=20.6$; $I_s=13.8$; $\rho_1=8.33$; $X_c=11.33$; $R_s=4.63$; $C_{mfd}=234$; $E_c=115.9$.

Equation $60\times\frac{112.7}{1{,}800}$ gives $T_{st\max}=111.7$ ounce feet

$T_{st\ \max}/I_L=4.5$ ounce feet

For a given maximum starting torque $T_{st\ \max}=82.5$ ounce feet.

$$\rho_1=\frac{2aR_2''V_m^2}{Z_mT_{st\ \max}}\times\frac{112.7}{1{,}800}=11.28$$

and $X_c=13.52$; $R_s=6.60$; $C_{mfd}=198$; $I_s=10.19$; $E_c=138$; $I_m=20.6$.
Therefore

$$I_L=\sqrt{I_m^2+I_s^2}=23.03$$

$$T_{st\ \max}/I_L=\frac{82.5}{23.03}=3.58 \text{ ounce feet}$$

References

1. Design of Starting Windings for Split-Phase Motors, T. C. Lloyd, J. H. Karr. *Electrical Engineering* (*AIEE Transactions*) volume 63, January 1944, pages 9–13.

2. Starting Windings for Single-Phase Induction Motors, C. G. Veinott. *Electrical Engineering* (*AIEE Transactions*) volume 63, June 1944, pages 288–94.

3. Discussions, C. R. Boothby. *AIEE Transactions*, volume 48, 1929, pages 629–30.

4. Suggested Improvements in the Performance Calculations of Single-Phase Induction Motors, M. S. Thacker, H. V. Gopalakrishna. *AIEE Transactions*, volume 69, 1950 (*Proceedings* 50-1).

5. Single-Phase Induction Motors, W. J. Branson. *AIEE Transactions*, volume 31, 1912, part II, pages 1749–87.

6. The Cross-Field Theory of A-C Machines, H. R. West. *AIEE Journal*, February 1926, pages 160–66.

7. Performance Calculations on Induction Motors, C. G. Veinott. *AIEE Transactions*, volume 51, September 1932, pages 743–54.

8. Single-Phase Induction-Motor Performance, A. F. Puchstein, T. C. Lloyd. *Electrical Engineering* (*AIEE Transactions*) volume 56, October 1937, pages 1277–84.

9. The Cross-Field Theory of the Capacitor Motor, A. F. Puchstein, T. C. Lloyd. *Electrical Engineering* (*AIEE Transactions*) volume 60, February 1941, pages 58–62.

10. The Design of Fractional Horsepower Induction Motors, N. F. T. Saunders. *Journal*, Institution of Electrical Engineers (London, England) volume 85, number 502, August 1939.

11. Single-Phase Induction-Motor Performance Calculations, F. S. Himebrook. *Electrical Engineering* (*AIEE Transactions*) volume 60, February 1941, pages 55–58.

12. The Revolving Field Theory of the Capacitor Motor, W. J. Morrill. *AIEE Transactions*, volume 48, April 1929, pages 614–29.

13. Symmetrical Components as Applied to the Single-Phase Induction Motor, F. W. Suhr. *Electrical Engineering* (*AIEE Transactions*) volume 64, September 1945, pages 651–66.

14. Segregation of Losses in Single-Phase Induction Motors, C. G. Veinott. *Electrical Engineering* (*AIEE Transactions*) volume 54, December 1935, pages 1302–06.

15. Performance Calculations on Capacitor Motors, P. H. Trickey. *Electrical Engineering* (*AIEE Transactions*) volume 60, February 1941, pages 73–76.

16. Design of Capacitor Motors for Balanced Operation, P. H. Trickey. *AIEE Transactions*, volume 51, September 1932, pages 780–84.

17. The Equal-Volt-Ampere Method of Designing Capacitor Motors, P. H. Trickey. *Electrical Engineering* (*AIEE Transactions*) volume 60, November 1941, pages 990–92.

18. Current Loci for the Capacitor Motor, T. C. McFarland. *Electrical Engineering* (*AIEE Transactions*) volume 61, March 1942, pages 152–56.

19. The Fundamental Theory of the Capacitor Motor, H. C. Specht. *AIEE Transactions*, volume 48, April 1929, pages 607–13.

20. The Condenser Motor, B. F. Bailey. *AIEE Transactions*, volume 48, April 1929, pages 596–606.

21. Method of Symmetrical Co-ordinates Applied to the Solution of Polyphase Networks, C. L. Fortescue. *AIEE Transactions*, volume 37, 1918, part II, pages 1027–1140.

22. Analysis of Unsymmetrical Machines, W. V. Lyon, Charles Kingsley. *Electrical Engineering* (*AIEE Transactions*) volume 55, May 1936, pages 471–76.

23. The Apparent-Impedance Method of Calculating Single-Phase Motor Performance, W. J. Morrill. *Electrical Engineering* (*AIEE Transactions*) volume 60, December 1941, pages 1037–41.

24. Principles of Alternating-Current Machinery (book), Ralph R. Lawrence. McGraw-Hill Book Company Inc., New York, N. Y., 1940.

25. Theory of Alternating-Current Machinery (book), A. S. Langsdorf. McGraw-Hill Book Company, Inc., New York, N. Y., 1937.

26. Fractional Horsepower Electric Motors (book), C. G. Veinott. McGraw-Hill Book Company Inc., New York, N. Y., 1948.

27. Electric Power Circuits—Theory and Applications (book), O. G. C. Dahl. McGraw-Hill Book Company, Inc., New York, N. Y., first edition, pages 80–89 and 138–44, 1938.

28. Symmetrical Components as Applied to the Analysis of the Unbalanced Electrical Circuits (book), C. F. Wagner, R. D. Evans. McGraw-Hill Book Company, Inc., New York N. Y., 1933.

No Discussion

Measuring Commutation with an Indicating Instrument

R. T. LUNDY
ASSOCIATE AIEE

BLACK bands[1] have been used for years to compare the commutating ability of d-c machines and to set the brushes in the proper place. A black band, as used here, is defined as the number of amperes by which the current through the commutating winding can be changed without producing sparking at the brush faces. This band width is usually taken at 25, 50, 75, 100, 125, and 150 per cent of rated load. This is plotted as shown in Figure 1, the black band being the shaded portion.

When developing new brushes it is necessary to have a fairly fast and accurate way of comparing the commutating ability of the various brushes. The conventional black band was first used for this purpose. It was thought, however, that changing the load to obtain these bands disturbed the commutator film, making it difficult to duplicate results. Consequently, it was decided to make these comparative tests at full load only, which saves time as well as giving more consistent results. Of course this gives only the band width at full load, not a complete band as shown in Figure 1. The complete band can be taken if required. Even when holding the load constant, results varied considerably with different observers and with different light conditions. Other variables which affect commutation are atmospheric conditions (especially humidity), contaminated atmospheres, brush fit, end play of shaft, and commutator surface condition. In our work we attempted to eliminate as many of these variables as possible.

Studies were first made with a cathode-ray oscilloscope and then with an electronic voltmeter and a radio noise meter. Not all electronic voltmeters are suitable for this service. Two makes were tried that showed very little difference in reading between black commutation and bad sparking. The trouble with these voltmeters was, evidently, their lack of sensitivity at high frequencies. The meter giving the best results has a frequency error of less than 1 per cent between 20 cycles and 50 megacycles. This instrument which now has been used almost daily for two years, is a peak reading voltmeter calibrated in terms of the rms value of a pure sine wave. The voltage indicated is 0.707 of the positive peak of the applied voltage. There are five ranges making it easy to select a suitable scale for the voltage being read. Since the voltmeter is connected across the positive and negative studs it has to stand the full direct voltage of the generator. The instrument as received is limited to 700 direct volts if the negative lead is connected to the high terminal, and to 300 volts if the positive lead is connected to the high terminal. The voltage is limited by the rating of the 0.006-microfarad input capacitor which blocks out the direct current. This voltage rating can be increased by using an external capacitor of the same size with no effect other than raising the lower frequency limit from 20 cycles to about 40 cycles. Specific instructions should be obtained for the instrument when used on direct voltages above scale reading. Tests were made with the shielded probe of the electronic voltmeter connected directly on a brush stud. This was repeated with the shielded probe in the meter box and with unshielded connections between the voltmeter terminals and the machine. Since little or no differences were observed in the results the latter and simpler method of connection is now used.

Most of our evaluation of commutating ability of various brush grades has been done on a type *CD87* motor rated at 230 volts, 575 to 2,300 rpm, 10 to 15 horsepower , 39/58 amperes full load current. Our tests were all run at 1,800 rpm and approximately 30-volts output. This machine is directly connected and driven as a generator by a similar motor, both being mounted on a rigid steel frame. A thrust bearing was installed to eliminate end play. When first operated this equipment was located in a large room, but we soon found that the commutation was affected by changes in atmospheric conditions. The whole set was then moved inside of our altitude chamber[2] where we could hold the temperature at 85 degrees Fahrenheit and 35 per cent relative humidity. The steel shell of the altitude chamber also makes a good shield to eliminate interference when using the radio noise meter and the electronic voltmeter. The shield is not essential, however, as we have obtained good vacuum-tube voltmeter bands on a transportation traction motor outside the chamber.

Before starting a test with a new grade of brush the film was removed from the commutator with a commutator dresser stone and the new test started on a raw

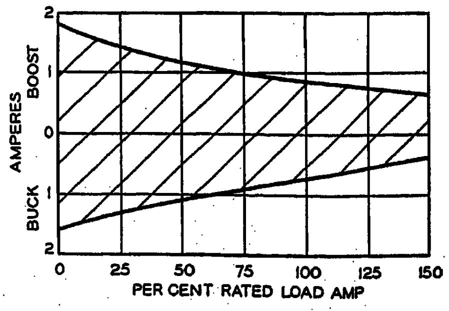


Figure 1. Representative black band

Paper 49-31, recommended by the AIEE Rotating Machinery Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Winter General Meeting, New York, N. Y., January 31–February 4, 1949, and represented at the Fall General Meeting, Cincinnati, Ohio, October 17–21, 1949. Manuscript submitted November 16, 1948; made available for printing December 16, 1948.

R. T. LUNDY is with the General Electric Company, Schenectady, N. Y.

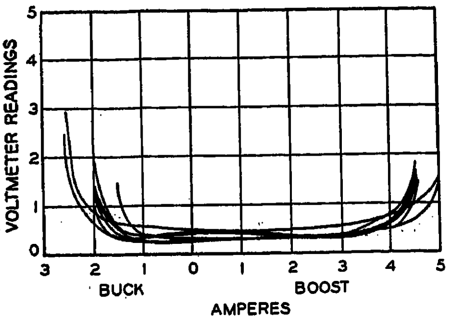


Figure 2. Representative voltmeter band for brush number 6, a very good commutating brush

commutator. Brushes were carefully sanded to fit the commutator and finally seated with soft white seater stone. Hourly readings were taken with the electronic voltmeter and then plotted against amperes buck and boost. Amperes buck or boost were changed in half-ampere steps, starting at the middle of the band and going in the buck direction until a voltmeter reading of over 1.0 volt was obtained, then returning to the middle of the band and increasing the boost current to the same limit. From this family of curves, see Figures 2 and 3, taken each day, an average band width at one volt was obtained. The comparison was made at one volt on this machine because the curve had definitely turned up and sparking was evident at this point. Carrying the buck and boost current much further caused excessive sparking which damaged the film and then required considerable time to return to normal. Table I is a summary of test results obtained on five different electrographitic brush grades. Brushes numbers *1* and *5* were useful commercial grades but did not have especially good commutating ability. Brushes numbers *2* and *3* were commercial grades recognized as better than average commutating brushes, while numbers *4* and *6* were experimental brushes. Brush number *6* shows exceptionally good commutating qualities. Visible sparking occurred near the bend in the curve of Figure 2 where the first increase in voltage was read, or at the half-ampere step of buck or boost current immediately preceding it. This has been checked many times while taking the voltmeter bands.

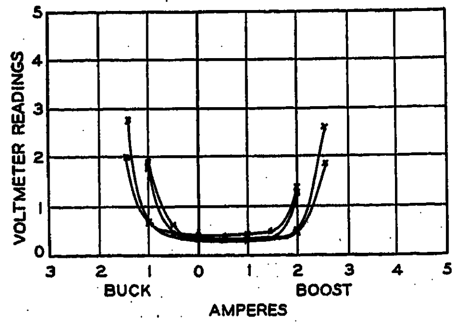


Figure 3. Representative voltmeter band for brush number 1, a poor commutating brush

A cathode-ray oscilloscope, connected between one brush and a pencil probe about one-half commutator bar from the trailing edge of the brush, produces the standing voltage wave shown in Figure 4. The high peaks occur at a frequency of approximately 1,260 per second, which is the calculated slot frequency. The minor peaks between are probably due to the three coils per slot in this machine. The sharp points at the tops and bottoms of the wave are caused by sparks when the brush leaves a commutator bar before the current is reduced to zero. The frequency of this spark is high. When the oscilloscope is connected across the brush studs, instead of from one brush to the pencil probe, the pattern is less sharply defined due to the influence of all eight brushes, but the slot frequency is still evident.

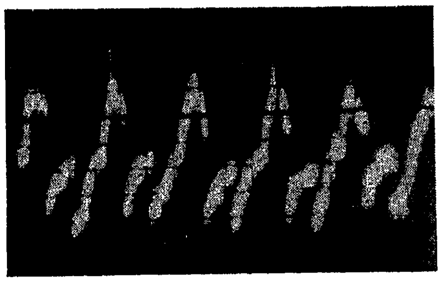


Figure 4. The wave obtained on an oscilloscope with leads connected between one brush and a pencil probe about one-half commutator bar from the trailing edge of the brush

Following this work an investigation of radio noise generation was made in coöperation with C. W. Frick. Readings were taken with a Ferris model *32* radio noise meter connected across the brushes at the following frequencies: 0.16, 0.2, 0.35, 0.6, 1, 1.5, 2, 3, 5, 8, 11, 15, and 20 megacycles. These readings show a maximum disturbance at 0.35 megacycle, which explains the necessity of using an electronic voltmeter sensitive to high frequencies. For comparison of results with those obtained with the electronic voltmeter the values plotted are the summation of the readings taken at the different frequencies, converted to an arbitrary scale. This comparison is shown in Figure 5. It is seen readily that these curves are similar in shape and, as indicated, sparking started near the bend in the curves. Some precautions must be taken when using the indicating instrument as a commutation meter. Erroneous results may be obtained if the generator supplying the buck and boost current sparks, as the disturbance will follow the wires to the voltmeter. Also, if the machine is loaded by "pump back" or on a line with other generators, sparking of the other machines may be read on the meter. We have used a resistance load bank for our tests to eliminate interference. At one time our generator was separately excited by an electronic current regulator to hold constant load. The current regulator rectified alternating current, supplying pulsating current to the generator field. These pulsations were transmitted to the armature and raised the electronic voltmeter readings over the full width of the band. If it is

Table I. Summary of Commutation Tests Using Electronic Voltmeter

Brush	Date of Test	Amperes, Buck	Amperes, Boost	Band Width
1	1-21-47	1.2	2.0	3.2
1	1-27-47	1.2	2.0	3.2
2	12-16-46	1.6	2.6	4.2
2	1-24-47	1.3	2.8	4.1
3	12-12-46	1.3	2.1	3.4
3	12-19-46	1.4	2.4	3.8
4	12-26-46	1.7	2.8	4.5
4	12-30-46	1.7	2.7	4.4
4	1-28-47	1.2	2.8	4.0
5	12-24-46	—0.3	1.8	1.5
5	12-23-46	—0.3	1.7	1.4
5	12-20-46	—0.3	1.9	1.6
6	2-11-47	1.9	4.2	6.1
6	2-13-47	1.8	4.5	6.3

Note:
Full load current of 58 amperes, room temperature 85 degrees Fahrenheit, 35 per cent relative humidity.

Average band width corresponding to a reading of one volt on the voltmeter.

These were 14-hour tests starting with a raw commutator each day.

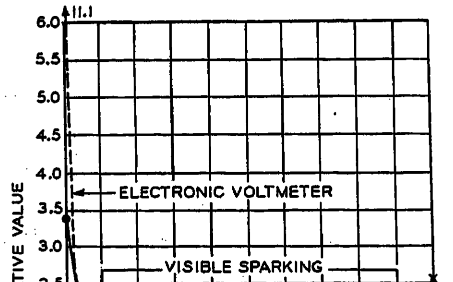


Figure 5. Comparison of radio noise meter readings and electronic voltmeter readings (relative values)

necessary to make a test on a generator running in parallel with other machines, it should be possible to block out the high frequency from the other machines by using a filter. Nevertheless, there are several reasons for our preferring the electronic voltmeter for measuring commutation instead of observing the visual sparking. One advantage is the elimination of the variations in readings due to different observers. This is important as there are considerable differences in the ability of the human eye to detect the first sparks. The local lighting conditions also effect the ability of the eye to see the small sparks. Another important point is that the electronic voltmeter shows sparking on all the brushes with one reading since it is connected directly across the brushes, while with visual observation it is necessary to look at all the brushes and on many machines this is physically impossible.

This more accurate method of comparing commutation makes it possible to study the effect of atmospheric conditions on brushes and machine performance. Besides being useful in evaluating brush performance the electronic voltmeter should be useful on production tests in setting the brushes in the neutral position, for checking pole shims, and to be sure each machine is within design limits. The requirement of preventing any sudden changes in current, especially at the trailing edge of the brush, has been fairly well taken care of in some machines by the machine designers. In many others current is still flowing between the commutator bar and the brush when contact is broken. This produces a spark, or at least a higher reading of the electronic voltmeter, and if bad enough causes etched commutator bars as well as short brush life and excessive commutator wear. These machines rely on the commutating ability of brushes for their proper operation. The electronic voltmeter is a useful tool in the development and evaluation of brushes with improved commutating ability.

References

1. THE "BLACK BAND" METHOD OF COMMUTATION OBSERVATION, T. W. Schroeder, J. C. Aydelott. *AIEE Transactions*, volume 60, 1941, pages 446–51.

2. NEW TEST CHAMBERS FOR AIRCRAFT ELECTRIC APPARATUS WITH PARTICULAR REFERENCE TO CARBON BRUSHES, E. R. Summers, J. F. Settle. *AIEE Transactions*, volume 63, 1944, pages 1205–12.

No Discussion

Report of the Board of Directors

THE BOARD OF DIRECTORS of the American Institute of Electrical Engineers presents to the membership its 65th annual report, covering the fiscal year ending April 30, 1949. It contains a brief summary of the principal activities of the Institute during the year, a general balance sheet showing the financial condition of the Institute at the close of the fiscal year, a statement of cash receipts and disbursements, and a schedule of securities owned. Additional information regarding the activities appeared in issues of *Electrical Engineering*.

BOARD OF DIRECTORS' MEETINGS

Five meetings of the Board of Directors were held during the year, two in New York, N. Y., one in Mexico, Federal District, Mexico, one in Milwaukee, Wis., and one in Dallas, Tex.

Information regarding many of the more important matters which were considered by the Board of Directors appeared in various issues of *Electrical Engineering*.

INSTITUTE VISITS BY PRESIDENT LEE

Alabama

Southern District Meeting, Birmingham

Arizona

Arizona Section and University of Arizona Branch, Phoenix

California

Stanford University Branch, Stanford; University of California Branch, Berkeley; University of Santa Clara Branch officers, Santa Clara; San Francisco Section; California Institute of Technology Branch, Pasadena; University of Southern California Branch, Los Angeles; Los Angeles Section; San Diego Section

Canada

Vancouver Section and University of British Columbia Branch officers; Toronto Section; Montreal Section; St. Maurice Valley Subsection, Shawinigan Falls; Ottawa Subsection; Niagara International Section, Niagara Falls, N. Y.

Colorado

Denver Section and University of Wyoming, Colorado Agricultural and Mechanical College, University of Denver, and University of Colorado Branches; University of Colorado Branch, Boulder

District of Columbia

Middle Eastern District Meeting, 'Washington; Joint AIEE-Institute of Radio Engineers-National Bureau of Standards Conference on High-Frequency Measurements, Washington

Florida

South Florida Subsection, Miami; North Florida Subsection, Gainesville; University of Florida AIEE-IRE Joint Student Branch, Gainesville

Illinois

Chicago Section, with Counselors and Chairmen of Branches at Illinois Institute of Technology and Northwestern University; dedication of new Electrical Engineering Building, University of Illinois, Urbana

Indiana

Fort Wayne Section; University of Notre Dame Branch, Notre Dame; South Bend Section

Maryland

Maryland Section and Johns Hopkins University Branch, Baltimore

Massachusetts

Pittsfield Section; Summer General Meeting, Swampscott

Michigan

Michigan Section, Detroit; joint meeting of University of Detroit and Wayne University Branches, Detroit; Michigan State College Branch, East Lansing; University of Michigan Branch, Ann Arbor; luncheon for Counselors of these Branches, Detroit

Missouri

St. Louis Section; luncheon meeting of St. Louis Section Executive Committee with Counselors and Chairmen of University of Missouri, Washington University, and Missouri School of Mines Branches, St. Louis; Washington University Branch, St. Louis; Kansas City Section and University of Kansas and Kansas State College Branches, Kansas City

Montana

Montana Section, Butte

New York

Joint meeting of Student Branches in New York City District, New York, with ten Branches represented; New York Section; Winter General Meeting, New York; Schenectady Section; Union College and Rensselaer Polytechnic Institute Branches, Schenectady; Syracuse Section; University of Syracuse Branch; Rochester Section; Ithaca Section with Binghamton Area Subsection; District 1 Executive Committee, Schenectady

North Carolina

North Carolina Section and University of North Carolina and Duke University Branches, Raleigh

Ohio

Toledo Section; Dayton Section; Columbus Section and Ohio State University Branch; Cleveland Section, Case Institute of Technology Branch, and Fenn College Branch, Cleveland; Mansfield Section; Akron and Canton Sections joint meeting, Akron; University of Akron Branch; Cincinnati Section and University of Cincinnati Branch

Oregon

Portland Section; Oregon State College professors of electrical engineering, Corvallis

Pennsylvania

University of Pennsylvania and Drexel Institute of Technology Branches, Philadelphia; Philadelphia Section; Lafayette College Branch, Easton; Lehigh University Branch, Bethlehem; Lehigh Valley Section, Reading; Pittsburgh Section and Carnegie Institute of Technology, University of Pittsburgh, West Virginia University, and Pennsylvania State College Branches, Pittsburgh; Erie Section; I.S.A. Convention joint with AIEE Committee on Instruments and Measurements, Philadelphia; Johnstown Subsection

Tennessee

Memphis Section

Texas

South West District Meeting, Dallas

Utah

Utah Section and University of Utah Branch, Salt Lake City

Vermont

North Eastern District Conference on Student Activities, Burlington

Virginia

Virginia Section with Western Virginia Subsection and Virginia Polytechnic Institute and Virginia Military Institute Branches and professors representing the University of Virginia Branch, Roanoke

Washington

Richland Section; Seattle Section; Pacific General Meeting, Spokane

West Virginia

West Virginia Section, Charleston

Wisconsin

Midwest General Meeting, Milwaukee

ANNUAL MEETING

The 65th annual business meeting of the Institute was held in Mexico, Federal District, Mexico, June 23, 1948. The annual report of the Board of Directors for the fiscal year which ended April 30, 1948, was presented in abstract by Secretary H. H. Henline, who also presented reports of the Committee of Tellers on the vote of the membership on constitutional amendments, and on the election of officers whose terms were to begin on August 1, 1948. In the absence of Treasurer W. I. Slichter, his report on Institute finances was presented by Secretary Henline. President-Elect Everett S. Lee was introduced, and responded with a brief address. Institute prizes for technical papers were presented.

The Lamme Medal for 1947 was presented to Past-President A. M. MacCutcheon, retired Vice-President of the Reliance Electric and Engineering Company, Cleveland, Ohio. The president's address was delivered by Blake D. Hull.

GENERAL MEETINGS

Four general meetings were held during the year, and a brief report on each follows.

Summer General Meeting. The 64th Summer General Meeting was held in Mexico, Federal District, Mexico, June 21–25, 1948, with a registration of 1,074. The program included the special opening inauguration ceremony presided over by President Miguel Aleman of the United States of Mexico, 19 technical sessions, 2 administrative conferences, and the official annual business meeting of the Institute. Social and entertainment features included a banquet Monday noon sponsored by the Secretary of National Economy and by the Federal Commission of Electricity, at which about 1,300 persons were present by invitation; a reception and luncheon in honor of the Board of Directors; a banquet and grand ball; a symphonic concert; exhibition of Mexican dances; ladies' events; and closing ceremonies and reception.

Pacific General Meeting. The Pacific General Meeting was held in Spokane, Wash., August 24–27, 1948, with a registration of 375. There were a general session, seven technical sessions, two Student sessions, a meeting of the District 9 Executive Committee, a luncheon conference on Student activities, and an informal meeting of Section officers of Districts 8 and 9. Members and guests attended a luncheon meeting of the Spokane Chamber of Commerce on Tuesday, addressed by President Lee, and a joint luncheon with the Associated Engineers of Spokane was held on Wednesday. Other events were inspection trips, a golf tournament, ladies' entertainment, president's reception and dance, and a banquet.

Midwest General Meeting. The second annual Midwest General Meeting was held in Milwaukee, Wis., October 18–22, 1948. The registration was 1,507. The program included an opening general session, addressed by Mayor Frank P. Zeidler of Milwaukee, and President Everett S. Lee, and 34 technical sessions and conferences, with 125 papers of wide variety in the fields of power, industry, communication, and basic sciences. There were about 30 committee meetings and luncheons. Many members and guests attended a luncheon in the Allis-Chalmers clubhouse Tuesday. Other events included inspection trips, a stag-smoker with entertainment, a banquet and dance, an extensive ladies' program, and a "Gemuetlichkeit."

Winter General Meeting. The Winter General Meeting was held in the Hotel Statler, New York, N. Y., January 31–February 4, 1949, with a registration of 2,896, and a record program of 55 technical sessions and conferences, and presentation of 233 papers. In a general session, the Edison Medal for 1948 was presented to Dr. Morris E. Leeds, Chairman of the Board, Leeds and Northrup Company, Philadelphia; an address on "The Professional Estate" was delivered by Charles E. Wilson, President of the General Electric Company; and President Everett S. Lee gave an address on "Our Institute." Approximately 80 meetings of technical and administrative committees were held. In addition, there were inspection trips, a smoker, a dinner-dance, and ladies' entertainment events.

DISTRICT MEETINGS

Middle Eastern District Meeting. The largest meeting ever held by the Middle Eastern District was held in Washington, D. C., October 5–7, 1948. The registration was 715. In 20 technical sessions, 70 technical program and District papers were presented. In a general session, addresses were given by President Lee, and Dr. F. B. Silsbee, Chief of the Division of Optics and Electricity of the National Bureau of Standards. Dr. Silsbee was introduced by Past-President William McClellan, honorary Chairman of the Meeting Committee. The program included inspection trips, a smoker, a dinner-dance, and events for the ladies.

Southern District Meeting. A Southern District Meeting was held in Birmingham, Ala., November 3–5, 1948, with a registration of 334. The program included an opening session with addresses by the Honorable Cooper Green, President o City Commission of Birmingham, F. B. Weiss, Chairman of the Meeting Committee, President Everett S. Lee, and Vice-President J. H. Berry, who were introduced by H. J. Scholz, Chairman of the Alabama Section. In five technical sessions, many papers of particular interest in the South were presented. There were inspection trips, two banquets, a District Executive Committee meeting, and ladies' events.

South West District Meeting. A highly successful meeting was held in Dallas, Tex., April 19–21, 1949, with a registration of 783, including slightly more than 300 students. The features included two general sessions, one with addresses by President Lee and Dr. Jack T. Wilson, and the other with five addresses on "The Electrical Engineers' Contribution to the Growth of the Southwest," a meeting of the Board of Directors, seven technical sessions with 27 papers, two student sessions, two luncheons, a smoker and buffet supper, a banquet and dance, inspection trips, and ladies' events.

SPECIAL TECHNICAL CONFERENCES

A special technical conference is a relatively new type of meeting, which is held independent of general and District meetings. It may be defined as a program of panel discussions, technical papers, or exhibits under the auspices of a national technical committee in co-operation with an AIEE local Section. The purpose of these meetings is to expand technical committee activities on individual specialized subjects and to serve the interest of members and nonmembers on that subject. The scope of the conference is meant to be national in interest, technical in character, and concentrated as to subject matter toward one particular industry or objective. It also is intended to further the program of expanded Institute activities by taking the Institute to members, and at the same time to stress its potential value in advancing the AIEE into new fields by demonstrating in a forceful manner to engineers inside and outside the membership that the Institute does have a place for all, and is definitely interested in providing ways and means for discussing and ironing out their technical problems.

Operating under the foregoing principles, the following conferences were held during the past fiscal year.

Conference on Electric Welding was held in the Rackham Memorial Building, Detroit, Mich., December 6–8, 1948, sponsored by the Committee on Electric Welding, in co-operation with the AIEE Michigan Section, the Detroit Section of American Welding Society, and the Industrial Electrical Engineers Society of Detroit. The conference provided an opportunity for the presentation of papers and discussions among an audience of persons primarily interested in arc and resistance welding. The program was planned to cover many timely and vital topics of interest to manufacturers, designers, users, and the utilities which supply the power. Six sessions provided for the presentation of 31 papers. The total attendance at the conference was 436; advance registration was 178. Orders for proceedings at the conference were 235. A special inspection trip was planned to the Ford Motor Company's River Rouge Plant, and was attended by 194 persons. During the 3-day conference, 336 persons attended the special luncheons.

Conference on Electronic Instrumentation in Medicine and Nucleonics was held in the Engineering Societies Building, New York, N. Y., November 29–December 1, 1948, sponsored jointly by committees of AIEE and the Institute of Radio Engineers. Committees included the Subcommittee on Nucleonic Instrumentation of the Committee on Nucleonics and the Subcommittee on Electronic Aids for Medicine of the Committee on Electronics; also the Committee on Nuclear Studies of the Institute of Radio Engineers. These three groups found an area of common interest, and combined their efforts to conduct an interesting and informative conference. The program was planned to interest the several groups of technical specialists: one day for papers of interest to medical people, one for engineers and physicists, and another combined the interests of both medical and technical people. Six sessions provided for the presentation of 26 papers. The total attendance at this conference was 547 persons; advance registration 250. An interesting exhibit of nuclear radiation instruments, which was provided by the Atomic Energy Commission, accompanied this conference.

Conference on High-Frequency Measurements was held in Washington, D. C., January 10–12, 1949, sponsored jointly by the AIEE, the Institute of Radio Engineers, and the National Bureau of Standards. It brought together those interested in high-frequency measurements and measuring apparatus. The program was planned to include surveys of various branches of the art and the presentation of the latest developments in measuring technique. Four sessions provided for the presentation of 25 technical conference papers. The total attendance at this conference was 584; advance registration more than 200.

In addition to the technical sessions, inspection trips were arranged to the Naval Research Laboratory, the Naval Ordnance Laboratory, and the National Bureau of Standards. At a special luncheon at the Roger Smith Hotel on January 11, the guests were addressed by President Everett S. Lee, Dr. Stuart L. Bailey, President of IRE, and Dr. E. U. Condon, Director of the Bureau of Standards, each expressing the belief that many benefits to all and encouragement for future progress would be derived from the exchange of technical knowledge at these conferences.

Conference on Industrial Application of Electron Tubes was held at the Statler Hotel, Buffalo, N. Y., April 11 and 12, 1949. This conference was sponsored by the Subcommittee on Electron Tubes of the Committee on Electronics, and the Subcommittee on Electronic Control of the Committee on Industrial Control, jointly with the Niagara Frontier Section. The conference provided an opportunity to discuss the application of electron tubes in the industrial field, with presentation and discussion encouraged among the audience of people primarily interested in the application and operation of tubes in this field. A program was planned to cover experiences of manufacturers, designers, and users. Four sessions provided for the presentation of 20 papers. The total attendance was 229; with an advance registration of 152. Orders for proceedings at the conference were 118.

A special inspection trip was planned to Westinghouse Electric Corporation's Motor and Control Plant in Buffalo. A portion of the control division of this plant is devoted to the manufacture of electronic control equipment. The Monday evening dinner featured an address on the control aspects of atomic power by Dr. Karl H. Kingdon of the General Electric Company.

Conference on Electrical Engineering Problems in the Rubber and Plastics Industries was held at the Hotel Portage, Akron, Ohio, April 26, 1949, sponsored by the Subcommittee on Rubber and Plastics of the Committee on General Industry Applications, in co-operation with the Akron Section. This conference provided an opportunity for the presentation of papers and discussions of electrical problems in the rubber and plastics industry. Two sessions provided for the presentation of ten papers, with full discussion at the end of each session. The total attendance at this conference was 191; with an advance registration of 168. Orders for proceedings at the conference were 99.

Because of the lack of time at the one-day conference, the committee was unable to schedule any special events except the noonday luncheon. For next year's conference, the committee is hoping to extend the time to two days in order to cover more fully the problems at hand. This was the second conference conducted by the committee on this subject.

General Committees

FINANCE COMMITTEE

The fiscal year ending on April 30, 1949, as was indicated by preliminary figures, shows an operating deficit of about $18,000. This shortage, together with temporary needs for cash during the latter part of the year, has been met by withdrawals from the reserve capital fund. These transfers will be repaid early in the new fiscal year. Certain remedial factors are now in operation, which, it is hoped, will minimize or obviate such transfers in the near future. They were reflected in the adoption in October 1948 of a balanced budget for the year October 1, 1948, to September 30, 1949; and performance under this budget so far has been approximately in line with its provisions. Principal among these remedial factors are

1. Substantially increased advertising rates in *Electrical Engineering*.
2. Establishment of registration fees for general and District meetings.
3. Price scales for Institute publications designed to make them approximately self-supporting.
4. Wholehearted co-operation by those committees responsible for sizable expenditures in the effort to confine their expenditures within limits set by the budget.

In spite of these encouraging signs, the financial future of the Institute is by no means a clear picture, and the Special Committee on Institute Dues appointed a year ago by President Hull is continuing to function. In response to one of its recommendations, the principal spending committees have submitted 5-year forecasts of their financial requirements, which now are being studied by the Finance Committee with the objective of preparing a 5-year budget. The special committee then will use this and other information to assist it in recommending a policy concerning the dues situation. In the meantime, the existence of the reserve capital fund is the "shock absorber" which enables our activities to continue unimpaired and to be enlarged where necessary to meet the demands of the ever-enlarging electrical industry.

COMMITTEE ON PLANNING AND CO-ORDINATION

The committee held four meetings during the fiscal year, in August and October, 1948, February and April, 1949. It is expected that a meeting will be held during the Summer General Meeting.

Meetings. Consideration and approval was given to a number of Institute general and District meetings scheduled to be held during 1950, 1951, and 1952, at various locations. It has been found desirable to schedule these at least two, and preferably three, years in advance in order to obtain suitable hotel accommodations.

Registration Fees. The matter of registration fees to be charged at general and District meetings has been reviewed at various times. A fixed schedule of fees was adopted in 1948, whereby, at general meetings members pay a registration fee of $3 and nonmembers $5, while at District meetings members pay $2 and nonmembers $3. Families of members and Student members are not charged any registration fee. The purpose of these fees is to assist in defraying the expenses of the technical activities of the meetings. Recently the Committee on Planning and Co-ordination reviewed this situation to determine what fees, if any, should be paid by guest speakers, nonmembers presenting papers, applicants, and so on. The committee recommended, and the Board of Directors approved, the following supplemental schedule:

Guest speaker—No fee.

Nonmember presenting paper—Nonmember fee.

Applicant for membership—Member fee.

Attendance at meetings jointly sponsored with other societies—Fees are to be agreed upon beforehand with the other society, either *a* each society charging its members according to its own rules, or *b* all in attendance being charged in accordance with AIEE rules; means of identification to be agreed upon.

Attendance at social functions only—No fee.

From all reports, the charging of registration fees during the past year has not aroused any substantial adverse criticism, and the money so collected has been decidedly beneficial in assisting to maintain a balanced budget.

Technical Activities. The Technical Activities Subcommittee has well served as the correlating agency of the several co-ordinating committees, and has handled a number of specific projects, including the preparation of "Administrative Scope for Technical Co-ordinating Committees" and a manual for technical conferences.

Several organization changes have been arranged for in the co-ordinating and technical committee setups. Several important subcommittees have been elevated to full committee status, and this has proved advantageous. The co-ordination group on Communication and Science has been enlarged and split into two co-ordinating groups, Communication, and Science and Electronics. The Communication Co-ordinating Group will have seven technical committees, and the Science and Electronics Co-ordinating Group will have eight technical committees.

The scopes of several new technical committees now are being prepared.

Professional Group. The Professional Activities Subcommittee has provided a scope for the Bylaws for the Committee on Education, which recently was transferred to the list of general committees and placed under the Professional Group Co-ordinating Committee.

Legislation and Tax Status. The committee has given a great deal of consideration to the tax status of the AIEE as it might be affected by activities in the field of legislation pertaining to engineers. The New York State Tax Department recently ruled the Institute to be exempt from New York State Unemployment Tax.

Uniform Grades of Membership. The committee has endorsed the statement of uniform grades of membership as adopted by the ECPD committee on Professional Recognition, and this has been approved by the Board of Directors.

Volta Memorial Fund. A specific procedure for handling this memorial fund was drafted, and it is expected this will be submitted to the Board of Directors for approval at the June meeting.

Prize Awards. Action has been initiated for a detailed review and analysis of the Institute's procedure for handling prize awards.

Technical Conferences. The policy to be followed in planning and conducting technical conferences has been discussed in detail, and the manual which is being prepared on this subject is expected to facilitate and encourage holding of such conferences, which have proved to be highly successful.

PUBLICATION COMMITTEE

The publication policy, which became effective January 1, 1947, has been continued in operation. This policy provides for three publications: *Electrical Engineering*, *Transactions*, and *Proceedings*, together with advance photographic copies of papers regularly approved for presentation at meetings. Each of these publications was described briefly in an article entitled: 'AIEE Publication Services Reviewed for Membership" (*EE, Apr '49, pp 348-9*), with a detailed statement telling how each publication might be procured. The number of pages of published material is reported in Table I for the calendar year 1948, so that it readily may be compared with the amount of material published during previous years.

Electrical Engineering. With the growth of the Institute in numbers of members and Student members, the circulation of *Electrical Engineering* has increased steadily from an average of 40,375 copies for the preceding

Table I. Numbers of Pages of Published Material

Year	Electrical Engineering: Technical Articles	Electrical Engineering: News	Electrical Engineering: Transactions Sections	Transactions Only: Technical Papers	Transactions Only: Discussions	Totals
1941	337	281	600	542	268	2,028
1944	246	214	738	493	231	1,922
1945	255	225	736	121	143	1,480
1946	285	315	832	254	120	1,806
1947	806	474		1,463*	153*	2,896
1948	832	395		1,815†	172†	3,214*

* Preprinted in 227 sections of AIEE *Proceedings*, except for a 17-page technical paper in *Electrical Engineering*.

† Preprinted in 247 sections of AIEE *Proceedings* requiring a total of 2,090 pages.

Table II. Classification of Articles in Electrical Engineering in Seven Broad Fields

	May	June	July	Aug.	Sept.	Oct.	Nov.	Dec.	Jan.	Feb.	Mar.	Apr.	Total
General interest			2	2							3		7
Educational						2		2	2	1		1	8
Scientific	3.5*	4	6	2	4	3	2	3	2		0.5*	2	32
Communication	0.5*	2	1	1					1	3	3	1	12.5
Power	2.5*	2	1	1	4	3	5	3		3		1	25.5
Industry	3.5*	1		2		2	1		4	1	0.5*	1	16
General applications	1.0	1			1	1		1	2	3	3	1	14
	11	10	10	8	9	11	8	9	11	11	10	7	115.0

* Half values assigned to articles which fall in two fields.

year, as compared with 47,800 copies for the current year, with an all-time high of 52,100 copies for the April issue. The increase in circulation, combined with a ten per cent increase in the cost of printing and an advance in paper prices has been partly offset by reducing the yearly content by 135 pages, and using a paper of slightly less weight. In Table II, is presented a classification of articles in *Electrical Engineering* in seven broad fields. By judicious selection of technical papers of broader interest, and by giving preference to the papers which are shorter (six pages or less), the table shows that about the same number of articles have been published in the last half of the year, under the reduced page budget, as was published during the first half of the year. On inquiry in various sections of the country, it has been reported that the 135 pages have not been missed.

In the selection of articles for each issue, an attempt has been made to carry one or two articles in each of the six broad fields shown in Table II. The increase in the numbers of communication, educational, and general appliance engineering articles in recent months has met with favor. As time goes on, the totals for each of these six broad fields will be more nearly proportional to the number of members in these fields of endeavor.

Advertising. The amount of advertising received has held up very well, and it is expected that the gross revenue will total approximately the $180,000 estimated at the beginning of the appropriation year. The $33^1/_3$ per cent increase in rates, which became effective February 1, 1948, still has not been in effect for a sufficient time to derive the full gross revenue for the duration of the appropriation year. In view of the large circulation, and, after consideration of the circulation and rates of eight similar magazines, the committee thought the rates for *Electrical Engineering* were disproportionate. However, on recommendation of the advertising director, the committee agreed that now was not an appropriate time to raise the advertising rates further.

Transactions. Volume 67 of the 1948 *Transactions* has been made available in two parts—part I, which consists of 862 pages, contains the papers which were presented during the first half of the year, and it was made available last December. Part II, which consists of 952 pages, includes the papers which were presented during the last half of the year, the Board of Directors' report, and a complete index, was made available in May. In this way, half of the papers have been made available six months sooner, and the bulk of the volume has been reduced. On recommendation to the Board of Directors, the Student subscription rate to the *Transactions* has been raised to $5, the same as for members.

AIEE Proceedings. As an interim service to members who are specialists, and Student members only, single copies of the 1948 papers correlated with discussion, if any, have been made available in 247 sections to those filling out order forms published every few months in *Electrical Engineering.* Ten per cent of the papers are free to members and additional papers are available at ten cents each. In the case of Student members, there is no free allowance. A total of 6,295 orders for *Proceedings* were received during 1948. These orders average $8^1/_2$ papers per order form, and represent a distribution of 53,500 copies. Most of the copies ordered are free copies, and the total receipts were $644. By publishing a reference on the cover of the April issue to the location of the *Proceedings* order form in the advertising section, 54 per cent more orders have been received during the first ten days than were received during the same interval for the December order form. At this date, it is not known whether the procedure has merely advanced the time for ordering the papers, or there has been a substantial increase in the number of orders.

Review of Publication Policy. The present publication policy is being reviewed, a number of suggestions and constructive criticisms have been received from members and officers, and all of them are being given careful study. Among them, two important suggestions are listed:

(*a*). To abandon the *Proceedings* entirely and issue quarterly *Transactions* with the papers correlated in six subject divisions: communication, electronics and science, power systems, power equipment, general applications, and industry. This plan involves providing one or two such *Transactions* each quarter to each member without charge, and with a nominal price for any others. According to a preliminary estimate, one free division would cost approximately $3,700 less than the present cost of *Proceedings* and *Transactions.* Two divisions free would cost approximately $22,000 more.

(*b*). As an alternate to *a:* To publish more technical material in full in *Electrical Engineering* to the extent of the additional expense in *a.*

Special Publications. During the year, the following four special publications have been made available, and others are in prospect. These special publications fill a specialized need—they are published entirely on a self-supporting basis, and made available to members at cost, and in general at double the cost to nonmembers, through announcements published in *Electrical Engineering* from time to time.

1. Telemetering, Supervisory Control, and Associated Circuits (September 1948).
2. Electrical Engineering Problems in the Rubber and Plastics Industries.
3. Electron Tubes for Instrumentation and Industrial Use.
4. Elements of Nucleonics for Engineers.

Preprints. Technical papers approved for presentation at meetings have been made available in advance by a photolithographic process. A few of the papers not considered of permanent-record value have been made available as miscellaneous papers on an advance-copy-only basis. For the purposes of discussion, the free allowance to authors has been increased to ten copies, and the allowance to committees has been liberalized to 50 copies to the sponsoring committee. Other copies are sold to members at a price of 30 cents each, and to nonmembers at a price of 60 cents each. In general the system has been working satisfactorily, and it is believed to be more nearly on a self-supporting basis.

SECTIONS COMMITTEE

Section Activities. Section activities have continued to expand at accelerated postwar levels. A new Section has been formed at Miami, Fla., and several new Sections are in the process of formation. New Subsections have been formed in various Sections.

Booklet—"The Electrical Engineer." A subcommittee of the Sections Committee and of the Committee on Student Branches has revised the booklet, "The Electrical Engineer," and presented it to the headquarters staff for reprinting.

Prize Award Rules. A subcommittee of the Sections Committee has prepared a revision of the Institute Prize Paper Rules for final consideration at the June meeting of the Section Delegates. After approval, it will be presented to the Committee on Planning and Co-ordinating and the Committee on Award of Institute Prizes for final approval.

Meetings of the Sections Committee. A meeting of the Sections Committee was held during the Winter General Meeting. Three meetings are scheduled for Section delegates during the Summer General Meeting. These will include the Delegates' Get-together on Monday evening, June 20; a meeting to initiate an aggressive publicity program by the Sections on Tuesday morning, June 21; and a Section Delegates' Conference on Tuesday afternoon, June 21.

Co-operation With Other Institute Committees. The Sections Committee has actively co-operated with several Institute general committees, including Transfers, Membership, Student Branches, and the Technical Activities Subcommittee of the Committee on Planning and Co-ordination. At the request of the Technical Activities Subcommittee, a revised draft of the proposed policy on conduct of technical conferences was prepared and submitted.

Section Finances. A subcommittee of the Sections Committee under the chairmanship of G. W. Bower, Vice-President of District 2, is conducting a survey and study of the problem of Section finances for the next 5-year period. This study is being made in accordance with the action of the Board of Directors during the Winter General Meet-

Table III. Section and Branch Statistics

	For Fiscal Year Ending April 30					
	1944	1945	1946	1947	1948	1949
Sections						
Number of Sections	73	73	75	75	81	84
Number of meetings held	738	884	1,210	1,333	1,340	1,561
Total attendance	83,120	96,346	113,531	155,649	109,637	128,025
Branches						
Number of Branches	125	125	125	126	127	129
Number of meetings held	755	547	716	1,018	1,172	1,350
Total attendance	24,768	17,132	22,844	40,669	77,040	103,828

ing, wherein a recommendation of the Special Committee on Institute Dues requested a survey of Institute finances for the coming 5-year period.

COMMITTEE ON RESEARCH

The activity of the committee consisted of the sponsorship of a symposium at the Winter General Meeting on the subject "New Tools for Research and What They Promise for Electrical Engineering." Several papers were presented, all of which were well received. Attendance was approximately 200. Plans for a similar session on a different subject are being formulated for the 1950 Winter General Meeting.

STANDARDS COMMITTEE

During the year, the administration of the Standards Committee was unfortunately interrupted by the untimely death of its Chairman, Dr. Reginald L. Jones, who had for many years past given tirelessly of his time and of his tremendously capable capacity.

The Secretary, H. E. Farrer, who served the committee exceptionally well for more than 25 years, was retired at the end of the calendar year 1948 under the Institute's retirement system, and has been succeeded in this assignment by J. J. Anderson, Jr.

In the field of Standards or Codes developed or revised, the past year saw the completion of the following: Industrial Control (revised); Marine Rules (further revision); Master Test Code for Temperature Measurement in Electric Apparatus; Graphical Welding Symbols; Mercury Arc Rectifiers (revision); Letter Symbols for Electrical Quantities; Transformers, Regulators and Reactors (revision); Steam Turbine Generators (revision); Speed Governing of Prime Movers (revision).

It has been determined finally between ASA and co-operating bodies that the size of published Standards will be changed to $8^1/_2$ by 11 inches, and for the AIEE this was put into effect during the year. New binders for the $8^1/_2$ by 11 size now have been provided, and the Institute's entire existing stock of Standards on hand have been repunched so they will fit both old and new binders.

A new Standards co-ordinating committee, designated as number 8, to be known as the Committee on Co-ordination of Insulation, has been formed under the chairmanship of J. H. Foote. This committee includes the entire AIEE delegation to the Joint Committee on Insulation between AIEE, Edison Electric Institute, and the National Electrical Manufacturers Association. The scope of this activity encompasses insulation levels, insulation strengths, apparatus design, and similar technical aspects.

The C40 Sectional Committee on Storage Batteries, one of the important ASA activities for which AIEE is sponsor, is being reactivated pursuant to getting at the revision of the storage battery Standards.

In the international field, a very successful conference session was conducted in Mexico City during the Summer General Meeting. Papers were presented outlining the standardization procedures employed in the United States, the use of Standards as tools by industry, emphasis on safety codes, and papers indicating the application of Standards to two specific types of apparatus. The entire top echelon of the Mexican Department de Normas participated in the conference, and were very cordial in conducting interested visitors through their facilities and in discussing together standards problems. Senõr Rafael Illescas Frisbie, Director General de Normas, visited this country soon after the meeting, and it is believed a real forward step in understanding and co-operation was accomplished.

Further in the international field, the AIEE Committee on Marine Transportation was asked to assume international standards work in the marine field. Representatives thereof met in London.

One knotty problem entered into during the year and carrying over is that of temperature limits for switchgear in enclosures as controlled by cable insulation. This problem was assigned to a special subcommittee under the chairmanship of J. E. Clem.

UNITED STATES NATIONAL COMMITTEE OF THE INTERNATIONAL ELECTROTECHNICAL COMMISSION

Meetings of the following advisory committees of the International Electrotechnical Commission were held: A.C. 6: Lamp Caps and Holders, March 1948, The Hague; A.C. 28: Co-ordination of Insulation, June 1948, Paris; A.C. 18: Electrical Installations on Shipboard, July 1948, London; A.C. 30: Extra-High Voltages, July 1948, Paris; A.C. 31: Flameproof Enclosures, July 1948, London; A.C. 32: Fuses, July 1948, London. In Stockholm, Sweden, October, 1948—Council; Committee of Action; A.C. 6: Lamp Caps and Holders; A.C. 9: Electric Traction; A.C. 12: Radio Communication; A.C. 23: Electrical Accessories; A.C. 33: Capacitors.

The advisory committees have under way considerable technical work and much progress has been made. None of this work has resulted as yet in the submission of proposals for international agreement. Due to the lapse in the work during World War II, it is to be expected that some time will elapse before standard proposals can be put into shape for international adoption.

The meetings of the council and the Committee of Action were devoted to the administrative and financial affairs of the commission. Due to the transfer of the central office of the IEC from London to Geneva, Switzerland, during the previous year, adjustments were necessary. The offices in Geneva are now completely equipped and in a position adequately to service the work of the IEC. At Stockholm, work was actively begun on a revision of the Statutes, Rules of Procedure, and Directives for the Advisory Committees. The statutes were last revised in September 1913, and in the intervening years had become partially out of date. It is hoped that the revision of these fundamental documents of the IEC will be completed in the near future.

To date, a meeting of A.C. 14: Transformers, has been held in London, March 1949. The following meetings have been scheduled for the coming year: A.C. 32: Fuses, May 1949, Paris; and at Stresa, Italy, June 1949: A.C. 1: International Electrotechnical Vocabulary; A.C. 8: Standard Voltages and Current Ratings, High-Voltage Insulators; A.C. 10: Insulating Oils; A.C. 17: Switchgear; A.C. 22: Electronic Devices; Council; Committee of Action.

TECHNICAL PROGRAM COMMITTEE

Technical Programs. The technical activities of the Institute are at the highest level in history as a result of the efforts of 31 technical committees, the largest number ever to serve the Institute. The Winter General Meeting program was enlarged to provide for the presentation of 233 papers, 145 of which were conference papers. Both of these figures are new meeting records.

In Table IV, the number of papers presented at each of the seven meetings which were held during the year are given, together with the attendance. This table would show a greater increase in the yearly number of papers presented but for the fact that only half as many papers as usual were presented at the Summer General Meeting. One of the reasons for the smaller number of papers at the Mexico City meeting was the great amount of time required to provide preprints in both English and Spanish. The Mexican members were responsible for translating and reproducing the preprints in Spanish. The local members were most appreciative of having the papers available in their native language and, of course, the results were made available to a much larger number of engineers. The large number of sessions, conferences, and papers would indicate that the Institute is on its way toward a greatly expanded program of technical activities. This increase may be attributed to growth in membership and the increase in the number of technical committees from 21 to 31. In addition the Institute is served also by six joint subcommittees.

Planning for Programs. The recent creation of seven new technical committees has increased the number of these committees serving the Institute to a total of 38, which in itself will continue to make available to the Institute an increased number of technical meetings and papers. The committee

Table IV. Number of Papers Presented During the Year Ending April 30, 1949*

Meeting	District	Advance Copy Only	Conference Papers	Transactions	Total	Attendance
Summer, Mexico City		26		31	57	1,074
Pacific Coast, Spokane		2	11	19	32	375
Middle Eastern, Washington	33	8		29	70	715
Midwest, Milwaukee		10	57	58	125	1,507
Southern, Birmingham	17			1	18	334
Winter, New York		14	145	74	233	2,896
South West, Dallas	20	4		3	27	783
	70	64	213	215	562	7,684

* Repeated presentations not included.

now is reviewing papers for the Summer General Meeting, the meeting having been designed to fit in with the meeting of the American Society for Engineering Education to be held concurrently in Troy, N. Y. This kind of co-ordinated activity with other technical organizations also has added to the general activity in the electrical engineering field; each technical committee is attempting to work jointly, in so far as possible, with other technical bodies whose work affects it. In the arrangements of programs, an attempt has been made to cover all new developments in the field of electrical engineering and the allied arts. The geographical location of meetings also has been taken into consideration, and papers have been presented which were of particular interest in certain regions.

Analysis of Subject Matter. With special reference to the 1948 Summer General Meeting, the technical program was planned to present the latest trends and developments in a number of specialized fields of electrical engineering that were of particular interest to Mexican engineers. Among these were papers on paper and textile mills, petroleum and steel production applications, hydroelectric power and irrigation and mining operations. At the Pacific General Meeting, the emphasis was on transmission and distribution, while at the Middle Eastern District Meeting the larger number of papers were on subjects in the fields of air and marine transportation. A field relatively new to electrical engineers was covered at the Midwest General Meeting in a conference on ore beneficiation. Also presented, in the industrial field, was a symposium on speed regulating systems for sectional paper machine drives and an all-day conference on automatic contouring for machine tools. At the Winter General Meeting, a widely diversified program was highlighted by a conference on energy sources and symposiums on gas turbines for power generation and new tools for research.

Future Meetings. The program for the Summer General Meeting has been completed, and those for the Pacific General Meeting and the Fall General Meeting are well under way.

BOARD OF EXAMINERS

Statistics relating to the number of cases handled by the Board of Examiners during the year 1948–49 are given in Table V. The current total represents an increase of 60 per cent over 1947–48. These figures are exclusive of those covering "enrolled Students," which rose from 5,894 to 9,265, or about 58 per cent. The increase in the current total is principally in the applications for admission to the grade of Associate, this increase being 85 per cent.

Due to the increase in the membership of the Board of Examiners to 30, it has been possible to handle this larger volume of work with only one meeting over those regularly scheduled.

The large increase in Associate admissions and Student enrollments is principally due to the greatly increased enrollment in schools under the G. I. Bill of Rights. The Associate increase has come from those whose schooling was interrupted by the war and who have now finished the two or three remaining years. The Student enrollment has come largely from those who have taken advantage of the G. I. Bill of Rights to start their electrical engineering education.

There is need to re-emphasize the statement in last year's report to the effect that it is still necessary to ask frequently for more detailed statements of record and additional supporting references. A mere statement of dates and titles of positions held is not sufficient to enable the Board of Examiners to arrive at a decision on eligibility.

The Board of Examiners regrets the loss, through retirement, of its former Secretary, H. E. Farrer. His services to the Board through the years have been invaluable.

COMMITTEE ON CONSTITUTION AND BYLAWS

Submitted by the committee on June 2, 1948, and adopted by the Board of Directors on June 24, 1948, were the following recommendations:

Bylaws. A new section defining the scope of the Committee on Institute Publicity was added and became Section 78 in the 1948 "Year Book." The name was changed in October to Committee on Public Relations. Section 83 was amended to give the Standards Committee increased authority in the adoption of Standards.

A new section regarding the establishment of Subsections (or Divisions) was added, and became Section 41 in the 1948 "Year Book."

A new section relating to the disfranchisement of a Section of the Institute was added, and became Section 44 in the 1948 "Year Book."

Submitted by the committee on September 24, 1948, and adopted by the Board of Directors on October 19, 1948, were the following recommendations:

Bylaws. Section 105 (Section 107 in 1948 "Year Book") was amended to omit any reference to a limiting price for *Transactions.*

Sections 67, 69, 71, 72, 75, 81, 84, and the new section on Committee on Transfers (became Sections 66, 77, 82, 69, 74, 81, 80, and 86 in 1948 "Year Book") were amended to add the sentence, "At least one member of the committee shall be a member of the Board of Directors."

Submitted by the committee on January 20, 1949, and adopted by the Board of Directors on February 3, 1949, were the following recommendations:

Bylaws. A new section defining the scope of the Members-for-Life-Fund Committee was added, and will be placed between Sections 73 and 74, as printed in the 1948 "Year Book."

Section 31 was amended to clarify the matter of voting privileges of alternates to District Executive Committee meetings.

Submitted by the committee on March 29, 1949, and adopted by the Board of Directors on April 20, 1949, were the following recommendations:

Bylaws. Section 55 was amended to allow Student members to purchase current *Transactions* at the same price extended to members. A new section defining the scope of the Committee on Education was added, and will be placed between Sections 70 and 71, as printed in the 1948 "Year Book."

HEADQUARTERS COMMITTEE

The principal activities of the committee were the supervision of the cleaning of all rugs in the Directors' room, Members Room, and the hallways; shampooing of all upholstered furniture in these rooms; re-upholstering of three overstuffed chairs in the Directors' room, and repainting of all venetian blinds in both rooms; and the submission to United Engineering Trustees of a suggestion that AIEE participate in proposed changes in the first floor lobby of the Engineering Societies Building, and contribute proportionately to the cost of such changes on the basis that: AIEE retain its own Board room for meetings of Directors and other large groups, and AIEE make use of smaller rooms as necessary for meetings of small groups.

The committee has under consideration the matter of polishing the table in the Board of Directors room, and provision of a sectionalized glass top. Some further upholstering may be necessary during the coming year, and the possibility of new lighting fixtures in the Directors' room will be considered.

COMMITTEE ON PUBLIC RELATIONS

The Committee on Public Relations, formerly known as the Committee on In-

Table V. Applications for Admission and Transfer, 1948–1949

Applications for Admission		
Recommended for grade of Associate	4,061	
Re-elected to the grade of Associate	98	
Not recommended	3	
		4,162
Recommended for grade of Member	308	
Re-elected to the grade of Member	10	
Not recommended	73	
		391
Recommended for grade of Fellow	6	
Not recommended	2	
		8
Applications for Transfer		
Recommended for grade of Member	485	
Not recommended	38	
		523
Recommended for grade of Fellow	118	
Not recommended	4	
		122
Students		
Recommended for enrollment as Students		9,265
Total		14,471

stitute Publicity, has been guided in its activities during the past season by a statement of policy adopted by the Board of Directors during the summer of 1948. This statement reads in part as follows:

> It is the policy of the Institute that public knowledge and understanding of the Institute's activities and public confidence in its members and their work be as widespread as possible....
>
> To accomplish these purposes and to promote the prestige of membership in the Institute, an active publicity program is necessary. Such a program should be integrated and continuous. It should reflect the Institute's position of responsible leadership in the scientific and engineering world and as the authoritative source of information regarding broad advances in the art of electrical engineering

In former years the committee had as its principal duty the publicizing of the Winter General Meeting. To implement the Board's statement of policy, a program was undertaken which promises to widen greatly the public's appreciation of Institute activities. This program is based largely on active co-operation with Section officers throughout the United States.

Publicity for the Winter General Meeting again was handled by professional counsel, Raymond C. Mayer, who has served effectively in the past, provided press releases and services which resulted in broad news coverage. The Committee on Public Relations assisted as far as possible in the publicity activities of the local committee covering the South West District Meeting, and will assist in connection with the Summer General Meeting.

The program of "integrated and continuous" publicity has enlisted the lively support of the Sections Committee. As an experimental part of the program, a publicity kit of instructions, suggestions, and samples of written material has been distributed to the Sections for trial and comment. It is expected that experience with this material during the next year will lead to the exploration and improvement of means for co-operation between the Committee on Public Relations and the AIEE leaders throughout the country. A half-day session of the Sections Committee at Swampscott, Mass., will be devoted to the discussion of the kit and other phases of Institute public relations.

Plans to provide more active publicity for general and District meetings are also under way. A great deal remains to be done to carry out the Board's policy, but a good beginning has been made.

COMMITTEE ON SAFETY

The Committee on Safety has been active, both in holding a conference and meetings of importance to the Institute, and in launching projects for promoting a better appreciation of safety and for implementing safety into practice.

A project of outstanding importance which has been considered intensively throughout the year by the committee and by a special subcommittee is that relating to research on resuscitation from electric shock. Analysis of reports of electric shock accidents shows that about a third of the victims made unconscious, but given artificial respiration with the promptness that should promise success, fail to recover for causes generally unknown. The doubtful and unknown elements in this situation long have blocked any substantial improvement in the resuscitation ratio of shock victims. In this committee's considered opinion, this block will continue until removed by research.

A research program for this purpose, therefore, has been carefully drawn up by members of the committee, including engineering and medical authorities on electric shock. The three Schools of Engineering, Hygiene, and Medicine of the Johns Hopkins University have offered to carry out this research jointly if the necessary financial support can be secured. The program has the endorsement of the Accident Prevention Committee of the Edison Electric Institute. The carrying out of this research not only should provide information on reasons for present failures, but also explore promising improvements in methods of resuscitation. The Board of Directors of the Institute has endorsed this proposed research, and hopes the electrical industry can provide the necessary financial support.

During the year, the steering committee held numerous meetings for the purpose of preparing an authoritative pamphlet which would present the hazards of low-voltage electric shocks, how electrocution occurs, and the safety features of electrical codes in prescribing proper wiring and vigilant maintenance as means of mitigating the hazards of low-voltage shocks. The preparation of such a pamphlet which would be worthy of the dignity of the Institute was proposed at the 1948 Winter General Meeting as a means of aiding in the dissemination of information on electrical hazards and safety practices. Although its context is still in the formative period, the pamphlet is being pressed for early completion.

A comprehensive bibliography on safety is under preparation to supplement a prior edition which covered safety conferences up to 1942, and it is planned that it will be available during the next administrative year.

The committee has co-operated with the Sections and Student Branches Committees with the objective of promoting continuing interest in safety throughout the AIEE membership by the nationwide activities of these committees.

Various committee members gave addresses on pertinent phases of safety before AIEE Sections, chapters of the National Safety Council, meetings of the American Society of Safety Engineers, industrial accident prevention groups, electrical association meetings, and other technical groups. W. R. Smith, C. F. Dalziel, and W. B. Kouwenhoven have given addresses on different aspects of low-voltage shock hazards, and Robin Beach on electrostatic fire and explosion hazards in industry. Other members have been active in promoting safety within their respective organizations.

Several members of the committee were appointed to serve as liaison representatives with correlated committees of the Institute and with national organizations, such as, the American Society of Safety Engineers, Edison Electric Institute, National Fire Prevention Association, and others.

W. R. Smith, representing AIEE on the National Fire Waste Council, attended the annual meeting in Washington. The chairman, Robin Beach, represented the Institute on The President's Conference on Industrial Safety—being a member of the steering committee and of the Subcommittee on Machine Safeguarding. During the year, he attended three meetings of this conference in Washington. W. B. Kouwenhoven and W. T. Rogers, of the Committee on Safety, also attended the general conference meetings.

During the 1949 Winter General Meeting the Committee on Safety held a well-attended Conference on Control of Electrical Hazards in Rural Areas. The program included a treatise by Professor C. F. Dalziel on American and European practice in the manufacture and operation of electric fences and on the hazards of improvised or improperly designed systems. The papers on low-voltage shocks to cattle and to personnel on farm properties by W. B. Buchanan and J. H. Waghorne of the Hydro-Electric Power Commission of Ontario were reports of several years' investigations into this phase of the electrical hazards. A. H. Schirmer supplemented these papers on shock hazards arising from improper grounding by graphically presenting the code requirements for their elimination. These papers are being prepared for *Transactions*, as they summarize the findings from a long-range study, the collection of extensive field data, and recommendations for safe practices which possess outstanding value on this subject.

COMMITTEE ON TRANSFERS

The present Committee on Transfers has endeavored to continue the efforts of its predecessor committees in complying with the requirements of the bylaws calling for encouragement of the Sections in their local transfers work, co-operation with the Sections committees, acting as a medium of exchange of ideas, and working toward the preparation and maintenance of an operations guide for the local committees. Encouragement of and co-operation with the local Sections has been accomplished through answering all inquiries addressed to the committee and through issuance of a letter and questionnaire to all Section committees on transfers.

The committee met at the Winter General Meeting, at which time a clarification of the principles, policies, and objectives of grade transfer work was developed. These may be stated as follows:

1. Each member of the Institute should be in the highest grade for which he is qualified.
2. The impetus for transfer should come from without. No member should be overlooked. The initiative for advancing a man in grade preferably should come from his fellow engineers (although the prerogative to take the initiative himself should remain unimpaired).
3. The initiating and follow-through action for transfer best can be undertaken by each Section through the medium of a hard working standing Committee on Transfers.
4. Each Section committee on transfers should be, in effect, a "Junior Board of Examiners" undertaking the careful preliminary screening of each and all members of the Section.

Also on February 1, 1949, an informal report on transfers work was presented at a meeting of the Sections Committee.

The importance and status of Section committees on transfers cannot be overemphasized. There seems to be general agreement with the idea of Section committees on transfers having the status and importance of being in effect "Junior Boards of Examiners."

D. E. Moat, Chairman of the Committee on Transfers for the past two committee-years, perhaps has stated this conception best in his report presented at the Summer General Meeting in 1948:

> The primary determination of when a member is qualified for a higher grade should be in the hands of the local committee on transfers. The procedure of trial application to the Board of Examiners should be abandoned except in borderline cases when for some peculiar reasons the local committee cannot decide.
>
> The Board of Examiners, of course, should be the last word, charged with the maintenance of the standards and quality of membership, but the local committee should be, in effect, a "Junior Board of Examiners" which nine times out of ten could come up with the recognized correct answer.
>
> The whole problem is one of definition placed in the hands of recognized people in each Section and which people should so clearly understand the requirements that they can fairly judge, approve, or disapprove, the qualifications of an applicant.

As an example, the Committee on Transfers of a Section having a membership of approximately 800 submitted 132 cases for transfer to the Board of Examiners over a two year period, of which only one case was disapproved.

A guide or manual of recommended procedure for Section transfer work is regarded as one of the most important objectives of the committee. Such a manual can be prepared best as the result of polling the Sections as to how past transfer suggestions have been received and acted on, and how the various Sections conduct their transfers work. From this information, the best material can be selected and combined into a master plan or manual of procedure. Toward this end, a questionnaire was sent to the Sections under date of March 18, 1949. In addition to questions as to committee procedures and suggestions, questions were included for the purpose of revealing the present extent and nature of Sections transfer activities throughout the Institute.

Other projects in which the Committee on Transfers is active are as follows:

1. Assistance in publicizing advantages of the higher grades.
2. Assistance in working toward improvement of the AIEE system of grades.
3. Improvement of proposal and application transfer forms.

Replies to the questionnaire are being currently received, and it is hoped that returns will be adequate to permit analysis and preparation of a proposed manual of procedure for presentation at the scheduled meeting of the committee at the Summer General Meeting.

In the letter of transmittal accompanying the questionnaire sent to the Sections on March 18, 1949, the following statements were included by way of encouragement to the Section committees on transfers:

> The importance of Section work on transfers cannot be overestimated. We, on the national committee on transfers, have come to regard a Section committee on transfers as a "Junior Board of Examiners," a "Court of Honor," sitting in accurate appraisal of a man's professional record in an endeavor to determine whether or not he merits the honor of advancement to a higher grade as a badge of recognition conferred on him by his professional associates. It is to be emphasized, however, that such a committee performs a screening function only, and in no way:
>
> 1. Replaces or changes the present constitutional procedure whereby the final decision as to whether or not an applicant qualifies rests with the national Board of Examiners.
> 2. Replaces the present right of individual members to take the initiative for their own transfer.

The committee's reaction from contact with the Sections over the past year is that interest in transfers work is very much on the increase throughout the Institute. The committee will continue to endeavor to move ahead in this very important work.

MEMBERSHIP COMMITTEE

Membership in the Institute increased by 2,391 members in the year ending April 30, 1949. This was the largest growth on record, amounting to 373 more than the previous maximum of 2,018 in 1946. Total membership reached 30,791. Table VI of membership statistics shows the nature of the changes in membership during the year.

Applications from new members shows the basis for the new records being set in Institute growth. Applications from Student members for Associate grade reached a new high of 2,286, compared with 1,481 in 1948. This trend of increased applications from students reflects the large enrollment in colleges, growth in Student membership, which reached a total of 19,428 as of April 30, 1949, and the active solicitation of students for election to Associate grade. See Table VII.

This trend of growth from the student group is still on the increase due to the fact that the peak graduation of electrical engineering students in the war-swollen cycle will not be reached before 1950 or 1951.

The accelerated activity of the membership committees among the college graduates tends to reduce the number of eligible members in other categories. This is reflected in the decrease in applications from all others in this fiscal year, as shown in Table VII.

Members in arrears for dues for the fiscal year ended April 30, 1949, reached a total of 1,376, compared with 1,288 last year. This represented a delinquency of 0.448 per cent, compared with 0.453 per cent a year ago, showing slight improvement. It is also encouraging to note that only 685 were dropped during the year, compared with 693 in the previous period.

Activity of the District vice-chairmen was outstanding during the year. From two to four letters were sent by each vice-chairman

Table VII. Number of Applications Received From Student Members and From All Others

Year Ending April 30	Students	All Others	Total
1949	2,286	2,192	4,478
1948	1,481	2,272	3,753
1947	938	2,331	3,269
1946	308	2,453	2,761
1945	249	2,179	2,428
1944	466	1,908	2,374
1943	783	1,431	2,214

Table VIII. Number of Student Members as of April 30

Year	New Applications	Renewals	Total
1949	9,967	9,461	19,428
1948	7,876	6,041	13,917
1947	5,092	3,929	9,021
1946	2,574	2,513	5,087
1945	2,326	2,287	4,613
1944	2,242	2,656	4,898
1943	2,512	3,200	5,712

Table IX. Record of AIEE Membership

Year	Total May 1	Year	Total May 1	Year	Total May 1
1884	71	1906	3,870	1928	18,265
1885	209	1907	4,521	1929	18,133
1886	250	1908	5,674	1930	18,003
1887	314	1909	6,400	1931	18,334
1889	333	1910	6,681	1932	18,003
1890	427	1911	7,117	1933	17,010
1891	541	1912	7,459	1934	15,230
1892	615	1913	7,654	1935	14,269
1893	673	1914	7,876	1936	14,600
1894	800	1915	8,054	1937	15,308
1895	944	1916	8,202	1938	16,078
1896	1,035	1917	8,710	1939	16,605
1897	1,073	1918	9,282	1940	17,213
1898	1,098	1919	10,352	1941	17,886
1899	1,133	1920	11,345	1942	18,944
1900	1,183	1921	13,215	1943	20,161
1901	1,260	1922	14,263	1944	21,407
1902	1,549	1923	15,298	1945	23,072
1903	2,229	1924	16,455	1946	25,090
1904	3,027	1925	17,319	1947	26,470
1905	3,460	1926	18,158	1948	28,408
		1927	18,344	1949	30,791

Table VI. Membership Statistics for Fiscal Year Ending April 30, 1949

	Honorary Members	Fellows	Members	6-Year Associates	Associates	Subtotals	Totals
Membership April 30, 1948	8	1,088	8,117	8,106	11,081		28,400
Additions							
New members qualified		5	249		3,009	3,263	
Former members re-instated or re-elected		4	96	64	134	298	
Subtotals		9	345	64	3,143	3,561	
Transfers		115	514	1,296		1,925	
Totals		124	859	1,360	3,143	5,486	
Deductions							
Died		36	71	68	33	208	
Resigned		2	46	110	119	277	
Dropped		2	75	232	376	685	
Subtotals		40	192	410	528	1,170	
Transfers			115	419	1,391	1,925	
Totals		40	307	829	1,919	3,095	
Net Changes		84	552	531	1,224	2,391	
Membership April 30, 1949	8	1,172	8,669	8,637	12,305		30,791

Table X. Deaths of AIEE Members Reported in Electrical Engineering

Name	Date of Election	Date of Death	Grade at Death	Obituary Notice in Electrical Engineering
Adams, Lee F.	A '09	Oct. 14, '48	F	Dec. '48, p. 1217
Adamson, Ralph T.	A '44	Mar. 19, '48	A	June '48, p. 613
Archibold, William K.	A '94	Oct. 27, '48	M	Dec. '48, p. 1219
Atwater, De Nyse W.	A '34	Apr. 13, '48	A	June '48, p. 613
Bachman, Elwood	M '40	Dec. 14, '48	M	Mar. '49, p. 271
Baker, Carey W.	M '34	July 5, '48	M	Sept. '48, p. 920
Bancroft, Charles F.	A '95	Oct. 25, '48	F	Dec. '48, p. 1219
Barrett, Sampson K.	A '11	Dec. 23, '48	M	Mar. '49, p. 269
Bassett, Allen	A '23	Nov. 19, '47	A	May '48, p. 503
Becker, Alvin G.	A '47	Oct. 18, '48	A	Jan. '49, p. 79
Bergrun, Ted	A '44	Apr. 1, '48	A	June '48, p. 613
Bijl van der, Hendrik J.	M '17	Dec. 2, '48	M	Feb. '49, p. 177
Bonine, Charles E.	A '03	Oct. 25, '48	M	Dec. '48, p. 1218
Booth, William T.	A '03	Apr. 11, '48	M	Aug. '48, p. 791
Borden, Stephen W.	M '16	May 13, '48	M	Aug. '48, p. 791
Bowman, Sr., James C.	A '40	June 30, '48	M	Sept. '48, p. 920
Brainerd, Frank K.	A '08	Dec. 6, '48	F	Feb. '49, p. 177
Buell, Henry H.	A '11	Jan. 12, '48	M	May '48, p. 503
Burkholder, Charles I.	A '03	Mar. 13, '48	F	June '48, p. 612
Cadwallader, James A.	A '15	Dec. 20, '48	F	Mar. '49, p. 270
Carr, John O.	A '22	Aug. 24, '48	A	Dec. '48, p. 1219
Champlin, Franklin J.	A '20		A	Nov. '48, p. 1115
Chipperfield, John W.	A '22	Mar. 16, '48	M	June '48, p. 613
Colpitts, Edwin H.	A '11	Mar. 6, '49	F	May '49, p. 460
Colvin, Clare W.	A '12	Oct. 9, '48	M	Jan. '49, p. 79
Davis, Benjamin	A '39	Oct. 8, '48	A	Dec. '48, p. 1219
Day, George C.	A '45	Nov. 13, '48	A	Mar. '49, p. 271
Diamond, Harry	F '47	June 21, '48	F	Aug. '48, p. 790
Dubs, Ford H.	A '27		A	Jan. '49, p. 79
Durland, D. C.	A '08	Oct. 19, '48	A	Dec. '48, p. 1219
Elson-Dew, William	A '08	Dec. 16, '47	F	June '48, p. 613
Flood, Jr., Henry	A '10	June 17, '48	F	Aug. '48, p. 790
Forman, Alexander H.	A '16	June 30, '48	F	Sept. '48, p. 920
Frankenfield, Budd	A '97	Feb. 1, '48	M	June '48, p. 612
Garretson, Harry D.	A '07	Feb. 12, '49	ML	Apr. '49, p. 366
George, Frederick R.	A '13	June 1, '48	ML	Sept. '48, p. 919
Glasso, John G.	A '03		F	Aug. '48, p. 791
Gordon, Leslie B.	A '25	Apr. 8, '48	M	June '48, p. 613
Gorsuch, William S.	A '07	June 13, '48	M	Sept. '48, p. 919
Gowan, Arthur J.	A '23	Aug. 17, '48	A	Dec. '48, p. 1219
Graf, Robert J.	A '11	Jan. 2, '49	A	Mar. '49, p. 271
Haar, Selby	A '07	Sept. 5, '48	M	Jan. '49, p. 79
Haller, Cecil E.	A '43	July 6, '48	A	Dec. '48, p. 1218
Hamlin, Edwin W.	A '35	Apr. 27, '48	M	July '48, p. 714
Hancock, Edward A.	M '38	Feb. 26, '48	M	May '48, p. 503
Herring, Edgar J. C.	A '29	Oct. 28, '48	M	Mar. '49, p. 271
Herron, James H.	M '35	Mar. 29, '48	F	Aug. '48, p. 790
Hill, E. Rowland	A '99	Aug. 25, '48	F	Dec. '48, p. 1217
Hill, Frederick W. L.	M '24	Jan. 12, '49	F	Mar. '49, p. 271
Horley, R. E. G.	A '21		A	Dec. '48, p. 1219
Hoseason, Donald B.	A '22	July 16, '48	M	Oct. '48, p. 1021
Humphrey, Clifford W.	A '03	July 6, '48	F	Nov. '48, p. 1115
Jackson, John P.	A '92	Apr. 2, '48	F	June '48, p. 612
James, Tudor C.	A '18	July 3, '48	M	Oct. '48, p. 1021
Jones, Archer K.	A '16	Sept. 6, '48	A	Dec. '48, p. 1219
Jones, Reginald L.	A '11	Jan. 14, '49	F	Mar. '49, p. 270
Kelsall, George A.	A '10	Jan. 4, '49	A	Mar. '49, p. 270
Kenyon, Otis A.	A '06	Feb. 3, '49	M	Apr. '49, p. 365
Knight, George L.	A '11	Mar. 27, '48	F	June '48, p. 612
Knouse, Walter E.	A '27	Dec. 4, '48	A	Feb. '49, p. 177
Lately, Harry N.	A '04	July 11, '48	F	Sept. '48, p. 920
Law, Raymond K.	A '47	Feb. 23, '49	A	Apr. '49, p. 365
Lawson, John E.	A '22	July 23, '48	A	Oct. '48, p. 1022
Lear, John E.	A '03	Aug. 17, '48	M	Nov. '48, p. 1116
Lee, Lloyd L.	A '46	Aug. 14, '48	A	Nov. '48, p. 1115
Linebaugh, Jesse J.	A '06	Aug. 2, '48	A	Nov. '48, p. 1116
Littler, Raymond G.	M '44	May 1948	M	Oct. '48, p. 1022
Long, Thomas H.	M '44	Jan. 18, '49	M	Apr. '49, p. 366
Mackenzie, Norman	A '43	Dec. 13, '48	A	Mar. '49, p. 270
Mahoney, Francis W.	A '09	Aug. 21, '48	M	Dec. '48, p. 1219
Manion, William J.	A '33	Feb. 18, '48	A	May '48, p. 503
Mapes, Leland R.	M '29	May 1, '48	F	Sept. '48, p. 920
Marti, Othmar K.	A '21	Feb. 15, '49	F	Apr. '49, p. 365
Martin, Allan F.	A '36	Mar. 2, '48	A	May '48, p. 503
Mason, Frank H.	A '10	Dec. 17, '48	A	Mar. '49, p. 270
Maurer, Keith L.	M '30	Feb. 23, '49	M	Apr. '49, p. 365
Meyers, Robert S.	A '37	Dec. 12, '48	M	Mar. '49, p. 270
Miller, Eugene H.	M '25	Mar. 12, '48	M	June '48, p. 612
Mills, John	A '11	June 14, '48	F	Aug. '48, p. 790
Milnor, Joseph W.	A '13	Mar. 30, '49	F	May '49, p. 460
Moloney, Thomas O.	A '12	Jan. 30, '49	F	Apr. '49, p. 366
Moritz, Charles J. H.	A '02	June 27, '48	F	Oct. '48, p. 1022
Moser, Matthew X.	A '25	Feb. 3, '49	M	Apr. '49, p. 366
MacFadden, Samuel P.	A '19	May 23, '48	M	Sept. '48, p. 920
MacGregor, Willard H.	A '97	Dec. 19, '48	M	Feb. '49, p. 177
MacMuray, Charles F.	A '13	Aug. 24, '48	A	Nov. '48, p. 1116
McClure, Earl L.	A '37	Sept. 2, '48	M	Dec. '48, p. 1218
McKearin, James P.	A '11	Apr. 24, '48	F	Aug. '48, p. 791
McKinney, Henry H.	M '47	Apr. 13, '48	M	Aug. '48, p. 791
McLean, William	M '42	Dec. 17, '48	M	Mar. '49, p. 271
Needham, Robert J.	M '23	Oct. 25, '48	M	Mar. '49, p. 270
Nill, Edwin P.	A '41	May 20, '48	A	Aug. '48, p. 791
Orwell, Albert E.	A '44		A	Mar. '49, p. 271
Peterson, Arthur P.	A '32	July 16, '48	M	Nov. '48, p. 1115
Post, Charles R.	A '39	Feb. 18, '48	A	June '48, p. 613
Powell, David A.	M '45	Dec. 19, '48	M	Mar. '49, p. 270
Powell, James A.	M '31	June 9, '48	M	Aug. '48, p. 790
Pratt, William H.	A '02	May 29, '48	F	Sept. '48, p. 919
Price, Bernard	A '10	July 9, '48	A	Dec. '48, p. 217
Pufler, William L.	A '93	Mar. 8, '48	F	June '48, p. 614
Rawkins, Herbert L.	A '30	Feb. 17, '48	M	May '48, p. 503
Reid, William E.	M '36	Jan. 23, '48	M	June '48, p. 613
Rentschler, Harvey C.	M '40	Mar. 23, '49	M	May '49, p. 460
Riggs, Arthur F.	M '16	Oct. 30, '48	F	Feb. '49, p. 177
Rockwell, Robert L.	A '13	Feb. 11, '48	M	May '48, p. 503
Rockwood, Jr., George H.	A '26		M	July '48, p. 715
Rushmore, Samuel W.	A '00	Aug. 16, '48	A	Dec. '48, p. 1218
Sah, Adam Pen-Tung	A '35	Feb. 14, '49	F	Apr. '49, p. 366
Samuels, Maurice M.	F '24	Jan. 29, '49	F	Mar. '49, p. 271
Shire, Leo E.	A '27	July 31, '48	A	Nov. '48, p. 1116
Sloan, Kenneth H.	A '38	Dec. 31, '48	M	Apr. '49, p. 365
Smeaton, James H.	A '07	Oct. 30, '48	A	Jan. '49, p. 79
Smith, Howard E.	A '44	Apr. 25, '48	A	June '48, p. 612
Smith, Jr., Walter F.	A '23	Feb. 9, '49	M	Apr. '49, p. 366
Snow, Wilber C.	A '20	May 20, '48	M	Dec. '48, p. 1218
Spencer, Charles G.	M '22	Aug. 16, '48	M	Nov. '48, p. 1116
Stayner, Charles M.	A '20	Nov. 19, '48	M	Mar. '49, p. 270
Sterne, William C.	A '23	Nov. 28, '48	M	Feb. '49, p. 177
Thompson, Jesse L.	M '40	Jan. 13, '49	M	Apr. '49, p. 365
Twyford, George T.	A '21	Dec. 6, '48	A	Mar. '49, p. 271
Unger, Arthur M.	A '40	Sept. 1948	A	Dec. '48, p. 1218
Warren, Aldred K.	A '95	Nov. 20, '48	M	Jan. '49, p. 78
Watts, Frank W.	A '24		F	May '48, p. 503
Werwath, Oscar	A '19	Mar. 20, '48	M	June '48, p. 613
White, Peter	A '37	Dec. 12, '48	A	Feb. '49, p. 177
White, William M.	M '21	Feb. 9, '49	M	Apr. '49, p. 366
Wilson, Charles C.	M '43	Aug. 12, '48	M	Oct. '48, p. 1021
Wilson, Jr., Joseph C.	A '44	July 14, '48	A	Sept. '48, p. 920
Wilson, William	M '23	May 6, '48	M	July '48, p. 715
Wood, Albert C.	A '10	Mar. 13, '49	M	May '49, p. 460
Work, William R.	A '08	Oct. 3, '48	F	Dec. '48, p. 1218

to the Section membership chairmen suggesting ways of increasing membership and committee activities. In addition to this, the vice-chairmen made many personal visits to Sections encouraging membership work. All Districts showed a gain in membership applications except District 8. The greatest gain was 57 per cent by District 9, with District 7 close behind. District 7 showed the greatest activity per member, having applications totaling 21.1 per cent of the membership at the beginning of the period.

Individual membership committees are of course primarily responsible for these gains. The committees had more members on the average and were particularly active in handling personal contacts with prospects. The greatest difficulty is still in the delay in organizing committees. Few Section membership committees begin active solicitation before the first of the year, which leaves only four months of concentrated effort.

The new program for soliciting Associate applications from Student members before departure from college was inaugurated this year. It is expected that this will stimulate membership and aid the Membership Committees. This program was developed through the efforts of the Membership and Student Branches Committees.

Two active subcommittees are working on membership problems. J. C. Woods, Vice-Chairman of the Membership Committee, is acting as chairman of a joint subcommittee with the Committee on Student Branches to study Student membership in the Branches. It is expected that this group will recommend methods of increasing Student membership and standardizing practices among the various universities and colleges.

Walter C. Bloomquist, Vice-Chairman, District 1, is acting as chairman of a subcommittee to prepare a proposal for improving the function of interesting prospective members in the Institute. A revision of the "Membership Information" bulletin has been proposed.

The key to a successful membership promotion program still rests with the individual initiative of the Section membership chairmen. Earlier selection of committees, use of larger committees, and more activity in finding and soliciting prospects are the three objectives to which each must strive. These committee chairmen have done an outstanding job this year but much ground is still to be gained in the year ahead.

Professional Group Committees

COMMITTEE ON CODE OF PRINCIPLES OF PROFESSIONAL CONDUCT

The committee has dealt with only two matters of importance, beyond incidental correspondence upon minor matters not requiring further attention. These were a new Statement or Code of Principles of Professional Conduct, and action of the Southwestern Power Authority, United States Department of the Interior, in soliciting competitive bids for engineering service.

In regard to the first matter, work held over from last year has continued upon the formulation of a formal Statement of Principles of Professional Conduct which will combine the newer Engineers Council for Professional Development canons, or ethical principles, with certain additional rules of practice which have had a long and honorable history in the Institute code.

The second matter involved a case where a Federal bureau advertised for competitive proposals for professional electrical engineering services. That matter was closed with our detailed report to the Board of Directors. It appeared that the bureau officials needed outside engineering service, but were advised that under existing law and regulations they had no other way of securing it. This committee found no violation of the new canons or the old code, but believed that the Institute should not approve or encourage such practice, and that necessity for it should be removed by legislative or executive action.

COMMITTEE ON EDUCATION

Four sessions have been individually or jointly sponsored at general meetings as follows.

1. At the Summer General Meeting, papers by two Mexican and three American authors dealt with education in Mexico, and with philosophical side of engineering education.
2. At the Midwest General Meeting, a conference was sponsored jointly with the Committee on Student Branches to promote interest among engineering students and faculties in electric power engineering. This activity was undertaken because the committees thought that students have been preparing for careers in electronics and communication in numbers proportionately much greater than employment opportunities warrant. Four papers, on careers in power engineering, unsolved problems, and better ways of teaching in the field were given to a large audience whose interest was exhibited by a spirited discussion.
3. At the Winter General Meeting, one conference was held on the theme "Industry's Active Part in Education." Four papers were presented, two from industry, two from educational institutions. The papers dealt with co-operation between colleges and industry at the undergraduate and graduate levels and with the development of the young engineer after entering industry. Another conference was sponsored jointly with the Subcommittee on electronic Education of the Committee on Electronics on the theme "Education in Power Electronics." Five papers were presented showing how the subject of power electronics could be employed to great advantage as a medium of instruction.

A session is planned for the Summer General Meeting on the accreditation of electrical engineering curricula. It is thought that the value of Engineers Council for Professional Development accreditation will be enhanced by a wider understanding of its objectives and procedures. At later meetings, it is planned to present and discuss other activities of the ECPD within the field of electrical engineering education. With these subjects and others, the committee looks forward to being able to sponsor interesting sessions at three of the general meetings each year.

At the invitation of the Engineers Council for Professional Development, the committee has undertaken a revision of Section IV, Electrical Engineering, of the ECPD Selected Bibliography of Engineering Subjects. The efforts of many members of the committee who have given generously of their time have brought this work very near to its completion.

In addition to these activities, the committee has considered and is currently considering other questions: the report of the President's Commission on Higher Education, industrial experience for foreign students, co-operation with Sections of the Institute in educational programs, and the acquainting of students with the facts of professional registration and licensing, the last jointly with the Committee on Registration of Engineers.

The committee thinks it can be most effective if it deals with problems that require the co-ordinated efforts of the engineering teacher, the practicing engineer, and industrial and governmental organizations. Thus, it is recommended that in appointing new personnel, there be more nearly equal representation from industry and colleges than at present.

COMMITTEE ON REGISTRATION OF ENGINEERS

The committee has continued its activities in connection with efforts to advise AIEE membership regarding the advantages of registration as a professional engineer and also as to the procedures for registration in the several states. Particular emphasis has been placed upon acquainting the young engineer, both while a senior in college and during his early years of professional activity, with professional registration.

Meetings of the committee were at the Midwest General Meeting and at the Winter General Meeting. Another meeting of the committee is scheduled for the Summer General Meeting, at which time special consideration will be given to suggested changes in the Model Law.

The committee has asked Professor R. W. Sorensen and Professor A. H. Lovell to prepare a paper entitled "Registered Professional Engineers." This paper has been prepared and presented for publication in *Electrical Engineering*, with the expectation of distributing several thousand copies to the Student Branches and the Sections of the Institute, calling their attention to professional registration, its advantages, and its procedures.

The committee has addressed a letter to each of the Student Branches, suggesting that they arrange a meeting at which the Sorensen-Lovell paper could be discussed, and at which time the Student membership could be given full information regarding registration. A letter also has been addressed to the officers of each Section of the Institute, directing their attention to the Sorensen-Lovell paper, and suggesting that they give all possible assistance to the Student Branches; also that some time be devoted at a Section meeting to acquaint the general membership with registration facts.

The individual Sections also have been asked to give assistance wherever possible to the State Boards of Examination, particularly in helping them to prepare suitable questions for the examinations covering the field of electrical engineering. Encouragement wherever possible has been given to the Sections to establish refresher courses to prepare their membership for the written and oral examinations.

At the suggestion of the committee, a question regarding the status of registration was included on the "Year Book" card circulated by AIEE headquarters to the membership. It is hoped that the returns will provide additional information regarding the extent of registration among our membership.

COMMITTEE ON STUDENT BRANCHES

During the past year, the Committee on Student Branches held three meetings, each at a general meeting of the Institute. At these meetings, there were unusually important agenda items which are summarized in the following.

At the committee meeting in Mexico City, a means of revising and expediting the timetable and processes of admission of Student members to Associate grade was recommended to the Board of Directors. This was the result of a study made jointly with the Membership Committee.

The committee met at the Midwest General Meeting. Pursuant to remarks made by Past President Hull and endorsed by President Lee, the committee set up a subcommittee to work jointly with the Institute of Radio Engineers to work out recommended bylaws for the establishment of Joint Student Branches that would be acceptable to both Institutes. The committee endorsed the action of the previous committee relative to using ECPD accreditation as a necessary qualification in the establishment of Student Branches in schools within the United States.

The third meeting was at the Winter General Meeting. At this time, the Subcommittee on Joint Student Branches reported in full and presented a model set of bylaws that had been worked out with IRE representatives. The committee endorsed this report, and referred it to the Secretary of the Institute for Board of Directors' consideration. This is an important step allowing many schools now having Branches to operate in a clearcut fashion jointly with many IRE branches now in existence.

At the New York meeting, a new joint

subcommittee was established with the Membership Committee and the Sections Committee. This group will organize and undertake surveys of Student Branch membership as a function of maximum potential membership at each school. It is investigating methods used by Counselors in promoting a larger percentage of admissions of Student Members as Associates, and will inquire concerning active steps taken by Branches to insure closer co-ordination with AIEE Sections located in the same geographical area.

The committee approved the revision of "The Electrical Engineer," which was accomplished jointly with the Sections Committee, and this revision was referred to headquarters for publication.

The Committee on Student Branches jointly sponsored with the Committee on Education a session at the Midwest General Meeting covering career opportunities, teaching methods, and growth in the electrical engineering power field. This session was very well received, and all of the four papers, each by an outstanding author, have been since published in *Electrical Engineering*.

During the past year, the committee has had referred to it seven requests for permission to establish new Student Branches. After complete consideration in each case, the committee recommended four for favorable consideration of the Board of Directors, and to date three charters have been approved. These Branches are located at the following schools:

Louisiana Polytechnic Institute

Fenn College

Instituto Tecnologico y De Estudios Superiores De Monterrey

Technical Committees

Co-ordinating Committees

POWER, INDUSTRY, COMMUNICATION AND SCIENCE, GENERAL APPLICATIONS

Organizational Changes. The technical committees have continued to function under four co-ordinating groups as established by the Board of Directors on April 23, 1947. It has been found that the co-ordinating committee meetings have been very valuable to the work in each group, and they have, to a considerable extent, facilitated the efforts of the individual technical committees. Problems relating to organization of technical committees received careful study by the co-ordinators, culminating in the following:

1. The Committee on Electronic Power Converters was transferred from the Power Group to the Communication and Science Group. This action was taken to reduce the number of technical committees within the Power Group from 12 to 11, also placing the committee in the group where it could carry out its work more effectively. At the same time, this change tended to equalize the number of committees within the co-ordinating groups. The creation of additional co-ordinating groups with the numbers of committees similarly limited will insure more effective operation under the co-ordinating group plan of organization.

2. A fifth co-ordinating group was created by splitting the Communication and Science Group into two groups, one for Communication and the other for Science and Electronics. This gives more prominence to Communication and Electronics, and elevates these subjects to positions commensurate with their importance in the field of electrical engineering.

3. The Metallic Rectifier Subcommittee has been advanced to full technical committee status. Upon recommendation of the Chairman of the Committee on Chemical, Electrochemical, and Electrothermal Applications of the Industry Group, this committee has been transferred to the Science and Electronics Group. It is believed that this arrangement will provide desirable liaison between the several technical committees of relatively common interests, since metallic rectifiers appear to have much in common with electronics and basic sciences.

4. The growing importance of servomechanism applications would seem to indicate the need of a full technical committee on this subject, which currently is being handled by subcommittees of several technical committees. The matter has been under study, which will be continued during the ensuing year.

5. In accordance with a resolution by the Committee on Planning and Co-ordination to the effect

> "That it is the sense of the Committee on Planning and Co-ordination that the number of committees (11) in the Power Group may be too large for effective operation, and, therefore, the Power Group Co-ordinating Committee should give consideration to the desirability of dividing it into two groups,"

a letter ballot canvass was conducted among the members of the Power Co-ordinating Committee on the proposal to split the Power Group into two groups, one for Power Equipment, and the other for Power Systems. A majority favored the proposal, with a strong minority in opposition thereto. The matter remains unsettled, pending further study and consideration.

Committee Scopes. Experience during the year has demonstrated successful operation under the scope previously prepared for each technical committee. Accordingly, the policy of having scopes available for all committees at Institute headquarters was extended to include all subcommittees, working, and project committees.

Technical Conferences. The number of technical conferences on specialized subjects scheduled during this year has been greatly increased, and this development promises to become one of the more important Institute activities. A written statement on the policy with respect to technical conferences has been completed in the form of a manual for the guidance of technical committee chairmen who may plan to sponsor such conferences. The co-operation and contribution of J. C. Strasbourger, Chairman of the Sections Committee, toward completion of this project is gratefully acknowledged. Plans for joint sponsorship with IRE of annual technical conferences on semiconductors have been completed, with recommendation for approval of this undertaking pending.

Committee Publicity. With the April 1949 issue of *Electrical Engineering*, a new department was created titled "Committee Notes," the object being to provide publicity on technical committee activities, which it is hoped will be interesting and beneficial to the Institute members.

Power Group

COMMITTEE ON CARRIER CURRENT

The Committee on Carrier Current held three meetings. In addition to the committee meetings, a full technical session was sponsored at the Midwest General Meeting, and a combined technical and conference-type session at the Winter General Meeting. At these meetings, a total of four *Transactions*-type papers, three committee reports, and four conference-type papers were presented.

The following projects are being followed actively, or have been completed during the past year, as indicated by the individual listings in the following:

1. A project committee is preparing a general-interest paper covering the status of carrier current in power system operation.

2. A survey was made and a report presented in Milwaukee outlining the subject of methods of measurements at carrier frequency. Project completed.

3. A working group is collecting data on the carrier current characteristics of transmission lines and systems.

4. A working group is collecting information on the carrier current characteristics of power equipment.

5. A subcommittee has been appointed to prepare information that would permit writing an application guide for carrier-current equipment. A working group prepared and presented a report on coupling capacitors and traps for rural line carrier telephone applications. Work completed.

6. A working group prepared and presented a report on the use of microwave equipment for relaying, telemetering, and supervisory control. This study of the application of microwave equipment for power system uses will continue.

7. A working group has been appointed to obtain data as to the operating experience with carrier current channels with the intention of publishing a committee report indicating the expected reliability of such channels.

8. A subcommittee has been appointed to study the requirements for tubes for carrier current applications and to report from time to time on the status of this subject.

COMMITTEE ON INSULATED CONDUCTORS

This report covers the work of the committee, which until 1947 functioned on a limited basis as the Cable Subcommittee of the Committee on Transmission and Distribution.

Committee Meetings. Committee meetings have grown from 1- to 2-day sessions, the first portion being devoted to individual subcommittee meetings. The committee held two meetings during the past year with an average attendance of 47.5.

Subcommittee Organization. To effect functioning of this newly created committee it was organized into 12 standing subcommittees as follows: Administration, Standards and Publications, Cable Characteristics, Conductors, Insulations, Sheaths and Coverings, Cable Supply Systems, Utilization Wiring Systems, Special-Purpose Cable, Accessories, Structures, and Tests and Measurements. It is recommended that the standing subcommittee type of organization be continued. This plan is preferred to the project type because it is more productive of sponsored papers, which is one of the primary objectives of an AIEE committee. Also, it avoids the administrative detail and time lag involved in setting up new working groups as each project is suggested for study.

Technical Papers. The committee has reviewed a number of papers, and 11 were scheduled at meetings held during the past year. In addition, five papers will be on the program of the Summer General Meeting. The committee is making an effort to sponsor papers on topics neglected heretofore, or inadequately covered recently, and to include papers on engineering practices as well as on new developments in the art.

Standards. During the past 30 or so years the AIEE has not been a factor in the field of standards for either bare or insulated conductors. To some extent, this situation is attributable to the fact that, until 1947, the subject of conductors was not recognized with autonomous technical committee status. At present, the generally accepted major industry standards and specifications for conductors are sponsored by eight organizations. It is evident that the AIEE plays a very minor role in standards for conductors, and will continue to do so unless there is a major change in policy by the established sponsoring organizations. Standards under consideration by the committee are as follows: Wires and Cables (AIEE 30–1944); Low-Voltage Air Circuit Breakers (AIEE 20A–1946); Recommended Practice Electric Installations on Shipboard (AIEE 45–1948); Potheads (AIEE 48–1948); Cable Limiters; and Test Code for Paper-Insulated Power Cables.

Subcommittee Projects. A project numbering system for the numerous activities of the subcommittees has been adopted. The advantages of this system are

1. Helps the chairman co-ordinate and follow the various subcommittee activities.
2. Assists the secretary in preparing co-ordinated and continuous issues of minutes.
3. Assists in the preparation of annual reports.
4. Tends to keep letters confined to one subject.
5. Provides an automatic filing system.
6. Helps to provide continuity of effort and to avoid repetition of work from one administrative year to the next with changing chairmen and personnel.

Numerous projects are being considered by the subcommittees.

Co-ordination With EEI and AEIC. To avoid duplication of effort, an informal working agreement was effected with the Edison Electric Institute and the Association of Edison Illuminating Companies. In general, it is based on the principle that in case of possible duplication the AIEE will not actively engage in the project, provided the EEI and AEIC intend to publish the results of their work. In some cases the publication facilities of the AIEE will be utilized for this purpose.

General. At the request of the Power Group Co-ordinating Committee, consideration was given to the formation of a Pacific Coast Subcommittee. However, it was decided not to organize this subcommittee, as few if any meetings could be held because of the distances involved. Since the participation in subcommittee work would be by correspondence in either event, it was thought preferable for the Pacific Coast members to be in contact directly with the subcommittees rather than through a Pacific Coast Subcommittee. Studies are being conducted through the subcommittees on many interesting and valuable projects.

COMMITTEE ON POWER GENERATION

Five technical sessions were sponsored by the Committee on Power Generation during the year. A total of 13 technical and 5 conference papers were presented at these sessions. In addition, the committee held two meetings during the year, the first at the Midwest General Meeting, and the second at the Winter General Meeting. Subjects covered by the papers were

1. Hydroelectric station design and operation.
2. Speed governing of multivalved steam turbines.
3. Speed governing of automatic-extraction steam turbines.
4. System load division by accurate time control.
5. Hydraulic model of liquid-cooled turbine-alternator rotor.
6. Outage rates of steam turbines, boilers, and hydro units.
7. Gas turbine design, theory, and testing.

At the last meeting, plans were laid to hold a technical conference on the subject of "Problems of Meeting Load Demands With Limited Reserve in Light of Maintenance and Forced Outages." Time and place of the proposed conference are to be settled later.

Activities of the committee are delegated to the subcommittees which reported as follows during the year:

Prime Movers. Two sessions on gas turbines were presented at the Winter General Meeting. Three technical papers and four conference papers were presented on different aspects of these new prime movers in respect to stationary power generation. Plans are under way to organize a joint committee with the American Society of Mechanical Engineers to consider the problem of standardizing steam turbogenerators, 7,500-kw capacity and under.

Speed Governing. During the year, the speed governing specifications that had been in trial use were recommended to be accepted for standard practice. One session at the Midwest General Meeting was sponsored to present two papers on speed governing, one paper on load division, and one paper on liquid-cooled rotor. Four papers on speed governing are being organized for the Fall General Meeting.

Station Design. A session of two or three papers on station auxiliaries is being organized for the Summer General Meeting. A program of papers for the Winter General Meeting, 1950, is being planned on the subject of centralized control in generating stations.

Excitation Systems. Two papers had been initiated by this group during the year and presented at jointly-sponsored sessions held during the Winter General Meeting. The working group on definitions has produced about 12 definitions relating to excitation systems. These will be presented to ASA Committee *C-42* for consideration and inclusion in its project on electrical definitions.

Application of Probability Methods. Outage data on steam and hydro-units collected under the auspices of the Edison Electric Institute have been published by the AIEE as a project of this subcommittee. A bibliography on probability methods is being compiled as well as a collection of definition of terms. A survey is under way of the load scheduling methods and spinning reserve practices being used by various utility systems in the United States. Papers are being solicited for the 1950 Winter General Meeting on the development of short-cut methods in probability calculations and on the probability of forecasting errors.

Hydroelectric Systems. Seven papers were presented at the Summer General Meeting, and at the Pacific General Meeting. Three papers are in preparation or available for future meetings.

Pacific Coast. A paper describing the available test procedures for determining performance of large hydroelectric turbogenerator units will be solicited. Plans are being made to collect statistical data on electrical characteristics of recently installed hydro generators feeding extensive high-voltage long-distance transmission systems. It is intended to make qualitative and quantitative analyses of these data and to determine the reasons for selecting the various characteristics.

COMMITTEE ON PROTECTIVE DEVICES

The activities of the committee, carried on by three subcommittees, were as reported in the following.

Fault Limiting Devices Subcommittee. The "Application Guide for the Grounding of Synchronous Generator Systems," will deal with the different methods of grounding synchronous generator systems, such as unit, more than one generator on a bus without feeders, generators on a bus with feeders, and so forth. It will discuss the various methods of grounding, such as potential transformer, distribution transformer, resistance, reactance, and direct grounding. The basic considerations in grounding will be discussed, and the accepted and preferred methods will be summarized with the advantages and disadvantages of the various methods pointed out. It is expected that the guide will be presented at the Fall General Meeting.

The "Application Guide for the Grounding of Transmission Systems," will deal with the basic factors and the different methods of grounding transmission systems of 22 kv and above. It will present the acceptable methods of providing grounding connections on transmission systems. It discusses in general terms ground relaying, limitation of ground fault currents, arrester protection, and transient overvoltages. Grounding transformers, grounding reactors, grounding resistors, and ground fault neutralizers are reviewed with regard to their proper application. This guide is intended to summarize briefly those methods of grounding transmission systems which are considered to be best practice.

The "Guide for Application of Ground Fault Neutralizers," reviews the voltage, current, frequency, and time ratings of ground fault neutralizers, application criteria dealing with location and number of units, system insulation levels, electric-static balance to ground, system interconnections, and possible effect on loading of transformers. Operating information dealing with tuning and testing after installation is discussed. It is expected that the guide will be presented at the Fall General Meeting.

The "Bibliography of Literature on Grounding Devices," will be divided into two parts: Bibliography on Resonant Grounding (Neutralizer Grounding), which will be complete through 1947 for presentation at the Fall General Meeting, and Neutral Grounding Devices in general, which is still incomplete, with no definite indication as to its completion date.

The "Field Survey of Unbalanced Charging Currents," is intended to determine whether the unbalanced charging currents of transmission lines were sufficiently large to justify the requirements in the AIEE Standard for Neutral Grounding Devices, which requires the ability of ground fault neutralizers to be capable of carrying 30 per cent circulating current continuously. This survey has been completed and the results will be presented at the Fall General Meeting.

The "Power System Fault Limitation" report is being prepared on methods used to limit fault currents on a metropolitan underground system; a system composed of overhead transmission and use of series reactors. With the increases in generating, transmission, and distribution facilities that are being made, it is important to limit the short-circuit kilovolt-amperes to a reasonable value. This is true both from the standpoint of the installation of new equipment and also from the standpoint of overduty on existing circuit breaker installations.

Lightning Protective Devices Subcommittee. The "Application Guide for Lightning Protection of Substations," will deal with the selection and application of lightning protective devices for the protection of equipment in substations. Factors taken into consideration will include system operating conditions, distance of protective devices from apparatus protected, grounding conditions, and other related factors. Included in the guide will be problems peculiar to some unit-type stations where, because of cable connections, the location of lightning protection equipment may be a considerable distance from protected equipment. It is expected that this guide will be completed and presented at the Fall General Meeting.

The subcommittee is preparing a revision and combination of AIEE Standards 24, Protective Tubes; 28, Lightning Arresters for A-C Power Circuits; and 47, Expulsion-Type Distribution Lightning Arresters. Agreement has been reached on all of this proposed Standard, except for the dielectric tests on the insulation assemblies of the so-called grounded neutral arresters. It is expected that values for these tests will be agreed upon shortly so that the Standard may be reported to the main committee, with the possibility that a covering paper could be presented at the Fall General Meeting.

A report on "Performance Characteristics of Lightning Protective Devices," of present-day station-type 20- to 73-kv line-type 3- to 15-kv distribution arresters, line-type expulsion arresters (expulsion tubes), and rod gap characteristics, is in preparation. It will combine in one place the important characteristics of the various types of protective devices available. It is expected to be presented at the Fall General Meeting. This activity is followed closely by the AIEE delegation to the Triple Joint AIEE-EEI-NEMA Committee on Insulation Co-ordination.

A symposium on "Recovery Voltage Characteristics of Distribution Systems," was presented at the 1949 Winter General Meeting, covering both field and laboratory test data on the recovery voltage characteristics of distribution systems. These data now are being correlated with the probable range of pertinent parameters of existing systems, and the next step will be to make use of them in setting up duty cycle test specifications for incorporation in a future revision on that part of the combined arrester Standard dealing with expulsion arresters. These data also should prove useful to other groups concerned with Standards on distribution circuit fuses, circuit reclosures, and so on.

This activity has been carried on by a joint working group sponsored by both the Lightning Protective Devices Subcommittee and by the General Systems Subcommittee of the Committee on Transmission and Distribution.

Co-ordination of Insulation Subcommittee. This subcommittee was established to handle problems within the Committee on Protective Devices dealing with co-ordination of insulation. Its function has been to keep the members of the main committee informed concerning the activities of other committees that are doing work dealing with insulation co-ordination.

COMMITTEE ON RELAYS

The technical activities of the committee have required three meetings, and one to three meetings of each of its ten project committees. The 11 formal and six conference papers presented have included one final committee report and one preliminary report. A conference session was sponsored at the Winter General Meeting. Revised Standards for power relays have been prepared and are being submitted to the Standards Committee. The principal technical activities were carried on through project committees, which are appointed to fulfill specific assignments.

Generator Protection. Investigation of loss of excitation of generators and associated protection methods has provided further data for a committee report now being formulated to summarize the protection of a-c generators. Two conference papers on this phase of the problem will be presented at the Summer General Meeting. The influence of partial winding faults to ground on the capability of relaying systems to provide positive protection is receiving particular attention, especially in view of neutral grounding schemes which limit fault current to small values with faults at the generator terminals. Papers are being encouraged to provide better methods for calculating the currents associated with partial winding faults with various grounding methods.

Bibliography of Relay Literature. An interim report covering references to technical articles on relaying in the American and foreign press which have been published since the last formal report of this project committee has been distributed to the committee and other interested individuals. It is planned that another formal report covering three years will be available in 1950.

Co-ordination of Construction and Protection of Distribution Circuits (Joint Group). This joint working group of the AIEE Committee on Relays, the AIEE Distribution Subcommittee, and the EEI is continuing with the study of correlation of distribution circuit construction and protection practices. Three meetings of the group were held. As a result of these activities, some 30 electrical utilities are participating in a study which will provide performance data on representative distribution circuits. Special records are being kept on the number of transient and sustained faults, the extent and duration of interruptions to customers, and the correlation of this information with construction and protection details such as, relay settings, protection, conductor size, material, covering and spacing, span length, surge protection.

Standards for Power Relays. The project committee has completed a final draft of a proposed revision of ASA Standard C-42, "American Standard for Relays Associated with Electric Power Apparatus." This draft has been approved by the project committee and the Committee on Relays, and is being transmitted to the Standards Committee. The proposed new Standard would modernize previous definitions, amplify the bases of rating, including current, voltage, temperature, range of operating voltage, and dielectric tests. For the purpose of future revisions, information is being formulated on additional performance data such as fidelity of characteristics as influenced by variations in temperature, wave form, and the like.

Insulation Levels for Relays and Control Circuits. A value of 1,500 volts alternating current has been adopted as being an adequate test voltage for the current, potential, and trip circuits of relays and associated circuits. An investigation of the standard recommended contact spacings for various types of high-speed relays showed that all of these are adequate to withstand a test of at least 1,500 volts alternating current across contacts (provided any arc suppressive devices are disconnected).

An individual paper, "Voltage Surges in Relay Control Circuits," was presented at the Midwest General Meeting, outlining the origin of transient voltages in relay circuits and reviewing methods for reducing these voltages. The Committee on Switchgear has been asked to consider a provision in future revisions of circuit breaker Standards which would require limiting the transient voltage produced by inductive "kicks" of operating solenoids to 1,500 volts crest (or lower if feasible).

Sensitive Ground Protection. A preliminary report of this project committee was presented at the Winter General Meeting. Based on additional discussions, the project committee is preparing a final report which will summarize available methods for sensitive ground protection of 3-wire systems and outline the limitations of present available methods for the protection of systems with multigrounded neutrals.

Transmission Line Protection. Back-up protection and its influence on transmission-line relaying has been given special emphasis. Three conference papers on back-up protection were presented at a conference session during the Winter General Meeting. Discussion developed that recent trends toward heavier loading of transmission lines have minimized the available inherent "back-up protection" formerly obtained as a by-product of sensitive relay settings. Several schemes for providing local back-up protection have been developed, but all appear to have undesirable limitations. A survey is underway to determine present and preferred practices in back-up protection.

Electronic Relay Applications. This project committee is seeking to develop additional favorable applications of electronic devices to protective relaying. Two individual papers were presented, one at the Midwest General Meeting, describing an actual installation of all-electronic distance relays, and the other at the Winter General Meeting covering laboratory models of electronic distance and other relays with test information. The project committee also is studying the application of microwave channels to protective relaying.

Relaying of Interconnections Between Industrial and Utility Generating Systems. This project committee is continuing its investigation of the special relaying problems encountered in the parallel operation of industrial generating systems with those of interconnected transmission systems.

Protection of Stations Without High-Voltage Switching. In line with modern trends toward simplified switching schemes, this project committee has completed a survey of present and preferred practices in the protection of stations without high-voltage switching. Increasing costs of equipment and improved reliability of power transformers have emphasized the desirability of dispensing with high-voltage circuit breakers wherever justifiable. Protection schemes include single- and 3-pole automatic grounding switches actuated by local differential relays and also various schemes of remote tripping of line circuit breakers. The work of the group was completed with the presentation of a project committee report at the Winter General Meeting. However, it is expected that certain phases of the problem may justify additional investigation, and consideration is being given to this as one of the future projects of the Committee on Relays.

Joint and Associated Projects. Through representation, the committee has participated in the activities of the Instrument Transformer Subcommittee of the Committee on Transformers with special attention to the relaying performance of current transformers and the transient performance of capacitance potential devices. Close co-ordination has been maintained with activities of the Committee on Carrier Current and a special subcommittee to study circuit breaker duty on out-of-phase switching.

COMMITTEE ON ROTATING MACHINERY

A total of 48 papers, sponsored by this committee, were presented at the AIEE meetings during the past fiscal year. Of these, 29 were presented at the Winter General Meeting. The main committee meeting was held during the Winter General Meeting. Subcommittee activities reported at this meeting are briefed in the following:

Insulation Resistance Subcommittee. Report 43A "Recommended Practice for Insulation Testing of Rotating Machinery," was presented to the committee. Final agreement on this report is expected shortly, after which it will be published as an AIEE Standard.

D-C Machine Subcommittee. Results of the 3-year motor test program on motors from 1/2 to 50 horsepower and 1,750 rpm, conducted by five co-operating electrical manufacturers, were reported at the technical sessions of the Winter General Meeting. The results indicate that a revision of Standards is necessary relative to the measurement of temperature rise on d-c machines; results on large machines differ from those obtained on small machines and additional data must be obtained on large units. Further work of the committee includes obtaining further data on temperature rises on large d-c machinery, revision of Standards on temperature rise measurements on d-c machines, the investigation of a standard method for determining short-circuit currents on d-c machines.

Fractional-Horsepower Subcommittee. Current activities of the committee include the formulation of a test code for fractional-horsepower motors. Future plans of the committee include the formation of an application guide for fractional-horsepower motors. It was agreed in committee that fractional-horsepower d-c machines, as well as a-c machines, will be covered by this committee.

Induction Machine Subcommittee. A test code for induction machines is in preparation and it is hoped that this will be completed in time for consideration at the Summer General Meeting. A session on the application of induction motors is contemplated for a future meeting.

Synchronous Machine Subcommittee. Minor revisions in the test code are contemplated.

Test Code Subcommittee. A co-ordinating subcommittee of this committee has been formed to integrate and co-ordinate test code activities.

During the discussion of these reports and other business by the main committee the following recommendations were made:

1. The Committee on Instruments and Measurements be asked to establish Master Test Code for Power Measurements.

2. The publication policy of the Institute be reviewed to permit the publication of a greater number of papers resulting from the increasing activities of the technical committees.

A report on the compilation of a bibliography on rotating machinery indicated this project is nearly complete with a total of 394 volumes having been reviewed, from which 1,408 articles have been catalogued under 12 major and 47 minor classifications. It is expected that this bibliography will be published when completed.

COMMITTEE ON SUBSTATIONS

Work in Progress. This committee, formerly the Committee on Automatic Stations, recently completed organization of its subcommittees and working groups to cover the greatly expanded scope of the committee. The main committee has assigned the projects mentioned in the following for study and possible action by the subcommittees concerned. So far, little has been accomplished on any of these projects, because they were only recently assigned.

The Automatic and Supervisory Control Subcommittee has been assigned projects relating to investigating the reliability of supervisory and automatic control for substations, and also revision of American Standard C37.2–1945 covering such equipment.

The Transmission Substations Subcommittee and the Distribution and Conversion Substations Subcommittee have in hand projects dealing with the relative merits and standardization possibilities of the 1-line diagram connections and structural design of the substations falling within their jurisdiction, also the problem of safety considerations in substation design, station lighting practices, and the problem of station auxiliary power supply for substations.

The Distribution and Conversion Substations Subcommittee also has been assigned projects covering the relative merits of circuit breakers versus reclosing fuses for small distribution substations and the choice of bus regulation versus feeder regulation for distribution substations.

The Working Group on Device Function Numbers has prepared and recently circulated a questionnaire regarding the adequacy of the present standard device numbers as established in American Standard C37.2–1945. The work of that group will be very valuable in considering the next revision of that American Standard.

The Working Group on Rectifier Switchgear, which is responsible for treatment of all matters pertaining to the design, construction, and application of switching devices, instrumentation, and protective and functioning relays for electronic power converters, has prepared a conference paper on Electronic Power Rectifier Protection that was presented at the Winter General Meeting. This paper will be followed by a formal paper of similar scope intended for presentation at the Summer General Meeting.

The Working Group on Substation Grounding Practice is responsible for investigation and study of substation grounding system design and testing practices, including consideration of the relative merits of galvanized iron versus copper electrodes. This group includes representation from two of the subcommittees of the Committee on Protective Devices.

AIEE Papers. This committee sponsored papers at a joint technical session at the Midwest General Meeting, with the Committee on Transmission and Distribution. This committee expects to sponsor a technical session at the Summer General Meeting, and also one at the Fall General Meeting.

Committee Meetings. The main committee met in Milwaukee on October 20, and in New York on January 31. A meeting also is scheduled for June 20 at Swampscott.

COMMITTEE ON SWITCHGEAR

The Committee on Switchgear sponsored technical sessions at the Midwest and Winter General Meetings. It will sponsor a similar session in June.

The committee held one meeting during the Winter General Meeting.

Proposed Standards for Automatic Circuit Reclosers for A-C Distribution Systems have been submitted to the Standards Committee with the recommendation that they be approved for a 1-year trial.

A Guide for Application of Low-Voltage Circuit Breakers has been prepared and submitted to the committee for letter-ballot. The balloting is nearly completed and indicates the acceptance by a large majority of the committee. This guide will be discussed at the Summer General Meeting.

The Subcommittee on Circuit Breakers is working on a proposed revision of ASA Standards for Power Circuit Breakers.

Last year the committee approved for submission to the Standards Committee a proposed Standard on Air Interrupter Switches and a proposed revision of Standard 22 covering "Air Switches and Bus Supports." At the committee meeting in January, it was pointed out that these two Standards had much in common and were very closely related. It was agreed that they would be combined into one, to be submitted to the Standards Committee. This combination has been made, and the combined Standard is being submitted to the Standards Committee for action.

Proposed revision of the Standard on Switchgear Assemblies, 27, was approved by the committee for submission to the Standards Committee, and has been so submitted.

COMMITTEE ON SYSTEM ENGINEERING

This report covers the work of the second year of the committee. The general program of subcommittees was continued. However, there was considerable enlarging of the committee in that 14 new members were added at the end of the first year to give broader diversification, geographically, and to strengthen the important System Planning Subcommittee.

The committee sponsored three sessions during the year—two at the Midwest General Meeting, and one at the Winter General Meeting. A total of 13 formal papers and 8 conference papers have been presented by the committee. There were two committee meetings during the year. At these meetings, plans of the committee for future meetings were outlined. The report of the program of the Power Group Co-ordinating Committee has been presented, and the reaction of the committee to the publication policies of the Institute have been discussed. In general, it has been the policy of this committee to present a full and adequate review of one phase of system engineering at any session it sponsors.

It prepared two papers for the Summer General Meeting, one covering the broad subject of the principles in the design of a utility system from the generating station through to the ultimate distribution system, and the second covering a basic change in the system frequency of one system and the problems associated therewith. Both papers were well received. The committee also prepared a paper which was presented at the Pacific General Meeting, dealing with the economics of transmission design in overall system planning.

System Planning Subcommittee. This subcommittee sponsored two papers at the Midwest General Meeting, one on the development and growth of a large all-hydro system and the second on a similar development of a metropolitan steam system. These papers emphasized the fundamental factors influencing the development of these systems and demonstrated the influence of these factors in future planning. Similar papers are scheduled for the Summer General Meeting on another type of metropolitan system using extensive subtransmission voltages, and a smaller system where the generation is located away from the load centers. There was also a paper presented at the Winter General Meeting on the use of the calculating board in system planning.

System Controls Subcommittee. This subcommittee sponsored a group of conference papers at the Midwest General Meeting, dealing with the subject of turbine and boiler design to meet fluctuating loads. These conference papers provoked so much discussion, not only at the meeting, but also in the technical press afterwards, that they were made the basis for three formal papers and two conference papers at the Winter General Meeting. This subcommittee also sponsored two papers on load control apparatus at the Midwest General Meeting, and has prepared a committee report on the status of automatic load control equipment.

System Operations Subcommittee. This subcommittee sponsored a companion group of three papers at the Midwest General Meeting on the subject, "Emergency Control of System Loads." The subcommittee also has undertaken a study of the problem of load pickup on radial and network feeders following prolonged shutdowns, made at the request of the Committee on Substations. This study is planned for presentation at the 1949 Fall General Meeting.

Interconnection Contracts Subcommittee. This subcommittee has prepared a committee report on various aspects of interconnection operations as influenced by the types of contracts. This report is being circulated only within the committee for future study.

Joint Subcommittees. Representatives also have served on two joint subcommittees, one on probability study of equipment outage, and the other on out-of-phase high-voltage switching. The Probability Studies Subcommittee presented a very complete report at the Winter General Meeting on outage records of steam and hydro-generators and boilers. It is continuing its study of variations in load and load estimating on a probability analysis basis.

COMMITTEE ON TRANSFORMERS

The committee held three meetings, with an average attendance of 21 members. The activities of this committee have been quite varied, and, at the present time, 14 main subjects are under consideration.

Co-ordinated Study of Life of Transformer Insulation Working Group. A program was drawn up for a set of pilot tests on selected class *B* and class *H* materials to be carried out by the four participating laboratories: Allis-Chalmers, Dow-Corning, General Electric, and Westinghouse. The pilot tests are intended: to permit a choice between the test methods proposed by General Electric and by Westinghouse; to train laboratory personnel in the techniques of making the tests. Apparatus and materials now are being accumulated and the pilot tests will be under way shortly.

Temperature Gradients and Hot-Spot Temperatures for Dry-Type Transformers Working Group. The work of this committee was conducted principally by correspondence without formal meetings being held. A tentative value of 110 degrees centigrade was accepted by the Committee on Transformers as the tentative limiting hot-spot temperature rise, after which this working group was dismissed.

Joint Subcommittee on Instrument Transformers. Inasmuch as the interest in instrument transformers is rather broad, an effort has been made to obtain broad coverage by obtaining representatives from other interested technical committees as well as from the Committee on Transformers.

Meetings have been held, and considerable progress has been made. Inasmuch as the subcommittee is rather large, all the work has been carried on in working groups. The following working groups have been established: Relaying Performance of Current Transformers, Test Code, Rating Factors, Grounding Instrument Transformer Secondaries, Definitions, Revision of Test Code for Metering, Separate Publication of Instrument Transformer Standards, Transient Characteristics of Potential Devices.

Subcommittee on Papers. There are nine papers available for the Summer General Meeting. The Committee on Transformers has been assigned two sessions at this meeting.

Operating Guides for Regulators Working Group. The draft of the proposed guide for the loading of oil-immersed step-voltage and induction-voltage regulators was revised several times by correspondence. Each revision was acceptable in all respects except on several fundamental points. These points were agreed upon at a meeting of the working group held on January 31, 1949.

The guide "Proposed American Standard Guide for Loading of Oil-Immersed Step-Voltage and Induction-Voltage Regulators" has been completed by the working group. A letter-ballot is being sent to each member of the Committee on Transformers for approval.

Short-Circuit Thermal Ratings of Oil-Immersed Step-Voltage and Induction-Voltage Regulators Working Groups. The work of this group is to amplify section 15.050 in ASA C57.15 to cover regulators which supply feeders having automatic reclosing circuit breakers; also to check if the regulators will withstand 25 times normal current both thermally and mechanically for the times given in the standard. Since this question was primarily one of determining from the manufacturers the short-circuit thermal and mechanical rating of regulators, the manufacturers of this equipment were consulted. In view of all of the variables involved, the following statement was generally agreed upon:

> When used on circuits with breakers having reclosing cycles, the accumulated time a regulator is subjected to short-circuit current shall not exceed that given in the above table.

This statement was to be added to ASA C57.15 immediately after the table in section 15.050. The Standard now states that regulators must withstand 25 times rated current for four per cent impedance for two seconds. All manufacturers agree that their regulators will meet this requirement.

A letter-ballot was sent to each member of the Committee on Transformers asking for approval of the statement, as a result, the original statement was changed as follows, the words in italics being added:

> When used on circuits with breaker *or other devices, such as fuses*, having reclosing cycles, the accumulated time a regulator is subjected to short-circuit current, *during one reclosing cycle*, shall not exceed that given in the above table.

This question was discussed by the Committee on Transformers. After many comments and suggestions, the problem was referred back to the working group for further consideration.

The suggestion was made that the interpretation of section 15.050 may belong in the Guide for Loading of Regulators. This will be considered, and a recommendation will be made to the Committee on Transformers by the working group at a later date. It was suggested also that similar changes should be made in the guide for loading transformers on the same point.

Subcommittee on Magnetization Characteristics of Transformers. The work of this subcommittee has been divided into three natural divisions:

1. Magnetization characteristics of transformers.
2. Effect of magnetization current on operation of air break switches.
3. Effect of magnetization current on relay problems.

Meetings have been held, and reasonable progress is being made.

Short-Circuit Requirements of Transformers Working Group. At a meeting of the working group, a proposal was drafted for revising sections of ASA C 57-12. In September, the proposal was submitted by letter-ballot to the members of the main committee. After minor revisions, it was approved and adopted by the committee.

Guides for Operation and Maintenance of Dry-Type Transformers With Class B Insulation Working Group. One meeting was held to review data assembled by means of a questionnaire to the working group members and to determine what material should be included in the guides. As a result of this meeting, a draft of a proposed guide was prepared and submitted to the members of the committee for review. Comments have been received on this draft, and a second meeting is being arranged, at which time it is hoped that a revised guide can be prepared for submission to the main committee.

Co-ordination of Insulation Working Group. This is essentially a 1-man committee, whose assignment is liaison between the committee and the AIEE group of the Joint AIEE-EEI-NEMA Committee on Co-ordination of Insulation. Two meetings of the AIEE group have been held and considerable progress has been made.

Electronic Power Converters Working Group. One man was appointed to act as the liaison representative between the Committee on Transformers and the committee on Electronic Power Converters to take care of such subjects as required co-ordination between the two groups.

Subcommittee on Method of Making Transformer Heat Runs. The work of this subcommittee is essentially a continuation of that carried on previously, and has to do primarily with the change in the ASA C 57 Transformer Standard necessary to indicate that the short-circuit method is the standard method and will be used unless otherwise specified and agreed upon. Because of a misunderstanding, serious opposition to the proposed change has arisen in one of the

EEI committees. The immediate task of the working group is to rewrite the proposal so that a misunderstanding will be impossible, and to secure sufficient data to prove that the short-circuit method is reliable and gives correct results within the same degree of accuracy as the back-to-back method.

Revision of Dielectric Tests Working Group. The working group was set up to consider changes in the insulation tests, both as to the magnitude and the technique of testing. Several changes in test procedure have been proposed. One in particular was due to an editorial error, but still remains to be corrected. Another involved changes in testing technique, particularly the use of current neutral measurements during impulse tests. A meeting was held; and tentative plans for changes were made. In order that the group should have broad coverage in the industry, it included several manufacturers' representatives and operators' representatives. Proposals were made, but several differences of opinion were expressed which were of so serious a nature that further work will be required.

COMMITTEE ON TRANSMISSION AND DISTRIBUTION

The past year was marked by continued intensive activity within the subcommittees. The product of this work will come to fruition shortly in the form of five subcommittee reports. In addition to the subcommittee meetings, there were two meetings of the general committee—the first in Milwaukee on October 20, and the second in New York on January 31. A third meeting is planned for Chicago on May 4.

Technical Meetings. Two technical sessions were sponsored by the Committee on Transmission and Distribution at the Summer General Meeting, and two at the Pacific General Meeting. At the Midwest General Meeting, one separately-sponsored session, and one jointly-sponsored session with the Committee on Substations were held. Two solely-sponsored sessions and one jointly-sponsored session with the Committee on Protective Devices were held at the Winter General Meeting. For the Summer General Meeting, one separately-sponsored session and one jointly-sponsored session with the Committee on System Engineering are scheduled.

Capacitor Subcommittee. A revision of AIEE Standard 18 on capacitors is nearing completion by the Capacitor Subcommittee. This Standard has been circulated through the main committee, and requires only slight modifications before submission to the Standards Committee for final approval. A joint report with a corresponding working group of the EEI on the performance of shunt capacitors is likewise nearing completion, and should be available for presentation at the Fall General Meeting. The Capacitor Subcommittee also will have available for presentation at that meeting a report on the maintenance of shunt capacitors.

Distribution Subcommittee. A report of this subcommittee entitled "Preliminary Report on Electric Power Distribution System Practices and Trends in the United States," was presented at the Summer General Meeting. This since has been made a final report and published in *Electrical Engineering.* A Symposium on Primary Network Systems, at which four papers will be presented, is being sponsored by the Distribution Subcommittee for the Summer General Meeting. Consideration also is being given by the subcommittee to an economic study of different values of primary voltages. The subcommittee also is engaged jointly with other groups of the AIEE and EEI in a study of the construction and protection of distribution circuits.

General Systems Subcommittee. A report by this subcommittee on "Power System Overvoltages Produced by Faults and Switching Operations," was presented at the Summer General Meeting. A co-operative survey with a corresponding group of EEI is being made on the causes of outages and operating experience on transmission lines of 100 kv and over. A session at the Winter General Meeting was sponsored in co-operation with the Committee on Protective Devices on "Recovery Rates of Voltage on Distribution Systems."

Lightning and Insulator Subcommittee. One of the active projects of this subcommittee is the preparation of a bibliography of material relating to lightning. This is a compromise with the original plan, which contemplated the correlation and publication of papers relating to the subject similar to the previous Lightning Reference Book prepared by the same subcommittee. This plan was abandoned because of cost. The subcommittee is concerning itself also with a study of recommendations for lightning-proof transmission lines. A symposium of five papers on the subject of insulator contamination and corrective measures was sponsored by this subcommittee and presented at the Midwest General Meeting.

Towers, Poles, and Conductors Subcommittee. A report by this subcommittee on the types of conductors available for use on high-voltage transmission lines was presented at the Summer General Meeting. A report on transposition is under preparation, and should be available for presentation at the Fall General Meeting.

Other Activities. The Committee on Transmission and Distribution is jointly active with other committees in two projects, the first being the investigation of high-voltage switching when out-of-phase voltage exists at the time of arc interruption, and the second is the calculation of short-circuit currents on low-voltage systems.

Co-ordination With Edison Electric Institute. Close co-operation between the Committees on Transmission and Distribution of the EEI and the AIEE has continued during the past year. As mentioned previously, the Capacitor Subcommittee, the Distribution Subcommittee, and the General Systems Subcommittee have active projects that they are carrying on with groups of the Transmission and Distribution Committee of EEI.

Industry Group

COMMITTEE ON CHEMICAL, ELECTROCHEMICAL, AND ELECTROTHERMAL APPLICATIONS

The committee held one meeting, in New York during the Winter General Meeting. The accomplishments of the past year were reviewed and plans discussed for the coming year. The scope of each subcommittee was reviewed and specific projects outlined for 1949.

This technical committee is now composed of the following subcommittees: Arc Furnaces and Electrothermal Processes; Electrolytic Processes; Cathodic Protection; Metallic Rectifiers; Petroleum Refining and Production; Batteries, Battery Forming, and Charging; Chemical Industries. The subcommittees engaged in the following activities during the year.

Arc Furnaces and Electrothermal Processes. This subcommittee is studying to establish good engineering practices in these fields. Problems relating to design and installation are of particular interest and a number of papers covering selected phases of the subject will be sponsored in 1949.

Electrolytic Processes. This subcommittee held a meeting during the Winter General Meeting to discuss future plans. It is continuing its work on matters relative to theory, technique, plant efficiency, application of power conversion equipment, and operating hazards. It is engaged in the preparation of a subcommittee report on the influence of electrolytic cell line characteristics on the selection of electric power and conversion equipment.

Cathodic Protection. This subcommittee now is working on a report relative to design and operation of apparatus and systems to minimize corrosion. A number of papers on corrosion prevention will be sponsored this year.

Metallic Rectifiers. This subcommittee is continuing its standardization work on metallic rectifiers. Two technical sessions are planned for the Summer General Meeting on theory, design, and application of metallic rectifiers.

Petroleum, Refining and Production. This subcommittee sponsored two papers at the Summer General Meeting on the electrification of pipe lines and oil well pumping by electric power. It also sponsored a conference at the Winter General Meeting on wiring and distribution systems for petroleum refineries.

Batteries, Battery Forming, and Charging. This subcommittee is actively studying battery ratings and battery applications to clarify inconsistencies in present Standards, and it also is sponsoring a conference at the Summer General Meeting on batteries and battery charging.

Chemical Industries. This subcommittee sponsored a conference at the Winter General Meeting on wiring and distribution systems for chemical plants, and jointly sponsored, with the Committee on Transmission and Distribution, a conference on heat balance in chemical plants. The subcommittee is actively studying the application of electric equipment to the chemical industry, particularly switchgear and control devices, to establish good engineering practices to meet the exacting requirements of this industry.

COMMITTEE ON ELECTRIC HEATING

The principal activity of the committee during the past year has been the sponsorship of two conference sessions on electric heating at the Midwest General Meeting, which were well attended. The papers covered resistance heating, induction and dielectric heating, and radiant heating. The committee met in New York on February 3, 1949. At this meeting, each of the subcommittee chairmen reported on the activity of his subcommittee during the past year. Since the major work of the committee is carried on through the subcommittees, the remainder of this report consists of abstracted portions from the subcommittee reports.

The Subcommittee on Electronic Heating has proceeded with an agenda agreed upon by the Joint AIEE, IRE, NEMA Co-ordination Committee on Induction and Dielectric Heating. Various working groups within the subcommittee have been given specific assignments, some of which already have been completed. A Standard has been formulated on good engineering practice in the case of radio interference, which covers the proper methods of shielding, grounding, and installation of this equipment. The report has been accepted by the subcommittee and has been tentatively approved by the IRE. The report will be presented at the 1949 Summer General Meeting.

A data sheet for use by the FCC in reporting cases of radio interference has been formulated. This form has been accepted by the FCC, and is now in use by all their field engineers for cases of radio interference from industrial equipment. A set of standard definitions for induction and dielectric heating is being prepared. It is planned eventually to incorporate all such information in a formal Standard.

The Subcommittee on Radiation Measurements Above 200 Megacycles met five times during the year, the work following closely the pattern set during the preceding year. It is still thought that the best method for measuring radiation field strengths in the range of 200 megacycles and above is by the substitution method, using a standard signal generator, a suitable calibrated receiver, and a pickup device having known characteristics. Through a study of the literature and the building and testing of dipoles, dipoles with corner reflectors, and horns, the subcommittee has become convinced that it will be possible to give constructional details for various pickup devices which, when constructed according to specification, will permit the measurement of field strengths in the range from 200 megacycles to 7,350 megacycles with an accuracy of plus or minus approximately 20 per cent. This assumes the use of proper equipment, smooth lines, and proper technique, all of which must be described in a final specification of recommended procedure. Preparing such a Standard will be the major work of the coming year.

The Subcommittee on Resistance Heating and Electric Furnaces met twice during the year, and played a prominent part in the conference on electric heating held in Milwaukee in October 1948. Several project groups have been formed within the subcommittee to collect and prepare technical data with the hope it can be made available ultimately in some well edited form for the use of engineers. Close co-ordination is maintained with the Subcommittee on Radiant Heating because of the overlapping nature of resistance and radiant heating.

The Radiant Heating Subcommittee has 22 members, and is organized with 11 project groups working on specific problems. The subcommittee met four times in places widely separated geographically. Nine inspection trips were held in conjunction with these meetings, and several pages of publicity including photographs on these activities have been obtained in such publications as *Electrical Engineering*, *Electrical World*, *Industrial Heating*, *Industrial Finishing*, *American Paint Journal*, *Products Finishing*.

The subcommittee currently is assisting the National Fire Protection Association in connection with a code revision on industrial ovens and furnaces. It is further preparing numerous recommendations although to date only one official action has been taken, namely that the AIEE adopt and recommend through C-42 that infrared be spelled without a hyphen. The subcommittee activities now fully embrace industrial radiation heating with both lamp and glassless high-temperature resistance heaters.

COMMITTEE ON ELECTRIC WELDING

Conference on Electric Welding. The committee in co-operation with the Michigan Section sponsored a 3-day Conference on Electric Welding in Detroit. The purpose of this conference was to provide a forum on welding in Detroit, which is the center of this activity. One day was devoted to arc welding, and two days to resistance welding. The results of this conference were very gratifying because an attendance of over 400 was obtained. This is to be compared with a usual attendance, at similar sessions held during general meetings, of 35 to 100, depending on subject and location. The conference was so well received that plans are being made to repeat it in 1950.

Bibliography. The committee published a bibliography of resistance welding, prepared by Mr. Zucker.

Subcommittee on Power Supplies for Resistance Welding. Additional papers have been prepared and presented, also data gathered for a revision of the present report on "Power Supplies for Resistance Welding." Work of this subcommittee is about 40 per cent complete. This work will go on until possibly 1950 before being ready to publish.

COMMITTEE ON GENERAL INDUSTRY APPLICATIONS

Principles of Work. The committee has endeavored to follow the principles which were laid down when it was formed in the summer of 1947: (*a*) To study, discuss, and clarify electrical engineering problems in the several industries assigned to the committee, and (*b*) to attract to AIEE and to this work the electrical engineers interested in the foregoing industries.

In order to carry out this work effectively, it was necessary to subdivide it among several subcommittees, each to cover an industry or a group of related industries. The subcommittees are the Subcommittee on Machine Tools, Subcommittee on Rubber and Plastics Industries, Subcommittee on Textile Industry, Subcommittee on Pulp and Paper Industry, Subcommittee on Materials Handling. This list does not cover, by any means, all industries which come within the scope of the committee. One of the problems discussed at the February 3 meeting was: Shall we organize additional subcommittees to cover additional industries?"

Subcommittee on Machine Tools. This subcommittee organized a very successful all-day session at the Midwest General Meeting. The topic selected was "Automatic Contour Follower Control for Machine Tools." Nine papers were presented. The meeting was very well attended. Discussions were interesting and lively. It is hoped these papers and their discussions can be published in one pamphlet, which will be an authoritative reference book on this subject.

This subcommittee did not plan on holding any additional machine tool sessions in 1948–49, but is working on the program for a session at the Fall General Meeting. The plans call for a 2-day session, with trips to several machine tool manufacturing plants in the area.

Subcommittee on Rubber and Plastics Industries. This group has arranged to hold annual meetings. Considering the geographical location of the rubber industry, Akron, Ohio, has been selected as the preferred place for the meetings, which will be arranged jointly with the Akron Section. The date for the 1948–49 meeting was set for April 26, 1949. This was an all-day affair. The program included discussion of: banbury drives, mill drives, calender drives, tuber drives, electrical standards for industrial equipments, and preventive maintenance. The papers and discussions will be published in pamphlet form, as has been done in connection with the April 20, 1948, meeting in Akron.

Subcommittee on Textile Industry. A session was held during the Winter General Meeting. This was devoted to the synthetic fibres industry, and consisted of four papers, one a presentation by title only. Two conferences are scheduled for the spring, one in Boston on May 3, and one in Atlanta on May 26 and 27.

Subcommittee on Pulp and Paper Industry. This group held a technical session at the Midwest General Meeting. The theme of the session was the "Speed Regulators for Sectional Paper Machines." Three papers were presented. The meeting was well attended (about 125) with many paper mill engineers present. This subcommittee is planning to hold the next session at the Summer General Meeting, the theme being "Electric Drives for Supercalenders and Winders." Two committee reports will be presented.

Subcommittee on Materials Handling. One technical session was held at the Midwest General Meeting. Two papers were presented. The topics were discussed at great length by many engineers attending the meeting. The subcommittee is planning the organization of a special one-day conference on May 13, 1949, in Cleveland, jointly with the Cleveland Section. The theme of this meeting will be heavy materials handling equipment such as needed for ore bridges and unloaders.

Reviewing the activities for the last year and a half, it appears that the committee met with some degree of success in its main task: "To bring AIEE closer to engineers working on industrial problems." The technical sessions organized by the subcommittees represent the sole ground on which these engineers can meet others in the same fields and discuss the problems which loom the largest in their professional lives. It is noted that there are no engineering organizations in existence (except these subcommittees), which discuss electrical engineering problems in such industries as machine tools, rubber, textile, paper, and materials handling. Hence, this work begins to fill a definite need. Efforts will be made to make it more effective, and to lay plans for the future.

Note: L. A. Umansky, Chairman of the Committee on General Industry Applications, his committee, and subcommittees merit special mention due to the number of successful technical committee conferences which they have conducted during the year and for the number planned for the future.

COMMITTEE ON INDUSTRIAL CONTROL

The committee has continued to take an active part in the work of the Industry Coordinating Committee, and every effort has been made to interest industrial engineers by preparation and presentation of papers relating to control problems in industry.

The Subcommittee on Standards has completed its work on the revision of Standard 15—Industrial Control Apparatus. The new Standard has been forwarded to the American Standards Association through the Standards Committee.

The Subcommittee on Electronic Control has been active in the formation of five working groups. These groups are concerned with: electronic control of d-c motors, photoelectric control systems, electronic relaying devices, electronic regulators and regulating systems, and electronic control of a-c power. Considerable work has been done by these groups on definitions in close co-operation with corresponding groups of the American Standards Association.

The Subcommittee on Electronic Control, in co-operation with the Committee on Electronics, sponsored a 2-day Conference on the Industrial Applications of Electron Tubes, in Buffalo, in April.

The Subcommittee on Test Codes is proceeding with a code on interrupting ability of contactors and endurance tests on a-c contactors. Numerous other subjects are under consideration by the subcommittee. The bibliography referred to in last year's report has been brought up to date, and final abstracting is now in progress.

The Subcommittee on Servomechanisms, jointly sponsored by this committee and those on Basic Sciences and Instruments and Measurements, conducted a well-attended session at the Winter General Meeting, and joined in a second session with the Committee on Industrial Control. It is hoped that in the near future a close liaison will be established between this joint subcommittee and the Subcommittee on Electronic Control.

During the year, the committee sponsored two sessions at the Midwest General Meeting and two at the Winter General Meeting. The sessions were well attended, and the papers presented were freely discussed by the members.

COMMITTEE ON INDUSTRIAL POWER SYSTEMS

This committee sponsored a session on industrial power distribution systems at the Midwest General Meeting. Three papers were presented at this meeting. Chemical and petroleum industry problems were covered at the Winter General Meeting. Papers on power generation and steam balance were given at this meeting.

The committee has three subcommittees: the Subcommittee on Wiring Design for Commercial Buildings is expected to complete its work and issue its final report at the Summer General Meeting; the Subcommittee on Revision of the Red Book is now actively engaged in revising and bringing up to date the so-called Red Book on Industrial Power Distribution Systems; the Subcommittee on Transformer Ratios was appointed mainly as a group to watch developments in the EEI-NEMA Transformer Voltage Standardization Program.

COMMITTEE ON MINING AND METAL INDUSTRY

The Committee on Mining and Metal Industry was newly formed in 1947, at which time it was called the Committee on Mining, Metal Forming and Rolling. At the present time it has 33 members, and is divided into two subcommittees, one covering the metal industry, and the other covering the mining industry. The members of this committee have been very active and have co-operated to the fullest extent in the committee program.

The first major activity of this committee was the presentation at the 1948 Winter General Meeting of 22 papers in six sessions. Much interest in these sessions was exhibited by men from the steel and mining industries. The committee sponsored a session on steel and a session on mining at the Summer General Meeting. The committee also sponsored two papers for the Pacific General Meeting. At the Midwest General Meeting, the committee presented 11 papers in three sessions, which covered mining, ore beneficiation, and steel mills. Because of the lack of space, the committee was asked not to sponsor sessions for the Winter General Meeting and the Summer General Meeting.

The committee has held several meetings, the last of which was at the Winter General Meeting. A good percentage of the committee attended. Glenn B. Southward, Mechanization Engineer of The American Mining Congress, also attended and gave many helpful suggestions on the work of the committee and on co-operation with The American Mining Congress.

The committee now is making preparations for three sessions at the Fall General Meeting.

Communication and Science Group

COMMITTEE ON BASIC SCIENCES

The Committee on Basic Sciences, as its name implies, deals with the investigation and presentation of subjects of fundamental nature likely to be of present or future interest to electrical engineers. It is the duty and purpose of the committee to sponsor, and present to the members of the Institute the results of activity in newly developing fields not easily assignable—at least in the initial stages of their development—to one of the established technical committees of the Institute. When, in the opinion of the members of the committee, fundamental activity in a field becomes great enough, a subcommittee for the particular field is formed, which concerns itself with this field exclusively. Often the fundamental activity in a new field leads to practical engineering applications not fitting well into an established technical committee, in which case the subcommittee becomes a full-fledged new technical committee of the Institute. For instance, the Committee on Basic Sciences had a Joint Subcommittee on Servomechanisms with the Committee on Industrial Control and the Committee on Instruments and Measurements; this subcommittee now is being considered for elevation to full technical committee status with the suggested name of "Committee on Feedback Control Systems."

The Committee on Basic Sciences sponsored technical sessions at the Midwest General Meeting, and at the Winter General Meeting. At these two meetings, a total of nine papers were presented.

The committee also held two meetings, one at the Midwest General Meeting, and one at the Winter General Meeting, at which it dealt with the question of standardization of voltage notation, the reports and plans of its subcommittees, and the possible formation of new subcommittees.

Activities of the five subcommittees operating through the year are reported in the following:

Energy Sources. The subcommittee sponsored its third conference on energy sources at the Winter General Meeting. Four papers were presented, dealing with the effect of electricity on the human body and on the subject of nerve electricity. The close correlation of this subject with the phenomena of electric fishes was discussed, and a demonstration with electric eels was presented. The attendance at this meeting proved the great interest in this subject. The committee is trying to arrange for publication, in pamphlet form, of most of the 14 papers presented since the formation of the subcommittee in 1946.

Electric Circuit Theory. The Subcommittee on Electric Circuit Theory presented a symposium on circuit analysis at the Midwest General Meeting, consisting of three papers dealing with the history and present state of operational calculus and of the application of matrix algebra, as well as with circuit analysis in servomechanisms. The subcommittee continues to participate in the defining of terms in network theory and electrical measurements, and the chairman of the subcommittee has undertaken to trace out the historical origin of the terms used in circuit theory.

Applied Mathematics. This subcommittee sponsored a Conference on Applied Mathematics jointly with the American Mathematical Society. The conference was held during the Winter General Meeting, four papers having been presented at this time. The subcommittee has further plans for participation in the International Mathematical Convention to be held in Boston, September 1949, and the ASEE meeting in Troy, June 1949. It also is scheduling another conference at the Winter General Meeting of 1950.

Magnetics. This subcommittee formed this year, sponsored a symposium on magnetics at the Winter General Meeting, consisting of morning and afternoon sessions, in the course of which six papers were presented. The attendance at these two sessions indicated that the formation of this subcommittee had filled a definite need. The subcommittee is planning another symposium on the subject for the Winter General Meeting of 1950.

Electrical Properties of Solids. This subcommittee was formed during the past year. The properties of semiconductors only recently have begun to be understood a little more clearly, and the development of the transistor has attracted a great deal of interest. Recognizing this, the subcommittee has prepared a symposium on the electrical properties of semiconductors and transistors to be given at the Summer General Meeting. Owing to the great interest in these devices by other technical societies, activities are under way to establish a joint AIEE-IRE committee on jointly sponsored annual symposia on this subject, so that duplication of effort will be avoided. Initial plans have been made by the subcommittee for a future symposium on "Electrical Properties of Dielectrics."

Electrical Properties of Gases. This subcommittee is still in the process of formation but can be expected to become active in 1950.

COMMITTEE ON COMMUNICATION

The Committee on Communication has continued its policy of allocating practically full responsibility to the subcommittees and the associated chairmen for the initiation of the technical programs in their particular fields of interest, solicitation and review of timely papers, and conduct of the sponsored sessions. The general steering of these communication activities has been under the guidance and direction of a co-ordinating committee including the three administrative officers and the chairmen of the nine technical subcommittees. Functioning essentially on a group basis for the past two years, the committee has requested Board of Directors' approval of the advancement of the subcommittees to full committee status in an autonomous Communication Group, effective August 1, 1949.

During the past fiscal year, the committee sponsored 11 complete technical sessions, at which 48 papers were presented that specifically covered the fields of telephone switching, wire and radio communication systems, aural and television broadcasting, railroad, and other special communication applications.

The Summer General Meeting was supported by the committee with four papers on telephone, telegraph, and radio subjects. Two papers on the status of radio telephone service in the United States were contributed to a joint electronic and communication session at the Pacific General Meeting.

Participation in the Midwest General Meeting was extended by sponsoring five complete sessions that covered a comprehensive list of subjects including home radio receivers, microgroove recording, television broadcasting and associated coaxial and radio relay facilities, development in semiconductors and the transistor, and telephone and telegraph switching systems. In view of the locale for the meeting a full session was devoted to "Radio on the Great Lakes."

For the Winter General Meeting, the committee sponsored a 5-paper home radio and broadcasting session; radio communication systems session with four papers; 5-paper session relative to wire communication systems; and a fifth session composed of five papers on special communication applications.

Two meetings of the main committee were held for the consideration of general policy matters. The co-ordinating committee met three times during the year in order to formulate the general program of activities and for the discussion and approval of the arrangements worked out by the subcommittees.

The communication program for the Summer General Meeting has been finalized to provide for three sessions, and a total of 12 technical papers, with the principal emphasis being focused on a telephone switching symposium that will deal with the fundamental and functional design of relay switching circuits. Tentative plans are under way for the 1949 Fall General Meeting with a possibility of underwriting five sessions highlighted by a symposium on coaxial systems.

COMMITTEE ON COMPUTING DEVICES

Activities of the Committee on Computing Devices so far have been to sponsor several sessions at AIEE meetings, including a session on analog computers at the last Winter General Meeting, as well as several articles on computing machine developments in *Electrical Engineering*. A session is planned for the Summer General Meeting.

Two major subcommittees have been formed, one on analog computers with Dr. E. L. Harder as Chairman, and one on digital computers with Dr. J. W. Mauchly as Chairman.

Professor G. D. McCann is compiling a bibliography on computing machines, and Dr. W. H. MacWilliams, Jr., is co-ordinating with the IRE and ACM (Association for Computing Machinery) on nomenclature and definitions. Professor S. H. Caldwell maintains contact with the Committee on Instruments and Measurements, while C. Concordia (Chairman) and Dr. J. G. Brainerd (Vice-Chairman) are also members of the Committee on Basic Sciences. Professor W. C. Johnson, Jr., is Secretary.

COMMITTEE ON ELECTRONIC POWER CONVERTERS

The committee activities were concentrated largely on projects initiated during the preceding year. The committee held two meetings—on October 11 in Pittsburgh, and on April 12 in Buffalo. Meetings were held also by several of the subcommittees. A committee report, "Mercury-Arc Power Converters in North America," was presented at the 1948 Summer General Meeting. This is a comprehensive report on the designs and applications of mercury-arc rectifiers.

Protection of Electronic Power Converters. Part 1 of the report on protection, which deals with the calculation of fault currents in rectifier circuits, was completed by the Electronic Converter Circuits Subcommittee, and was presented during the Winter General Meeting, at a technical session on protection sponsored by the committee. Part 2 of the report, which deals with protection systems and devices, is nearing completion and will be presented at the Fall General Meeting. It is being prepared by representatives of the Committees on Substations and Switchgear, in co-operation with the Circuits Subcommittee. The complete report will be published after part 2 has been completed.

Survey of Rectifier Operation. The Electronic Converter Application Subcommittee is making a survey of operating experience and system characteristics in mercury-arc rectifier installations, by means of questionnaires sent to rectifier users. The subcommittee expects to have a preliminary report on the survey ready for presentation at the Fall General Meeting. The complete report probably will be ready for the next Winter General Meeting.

Rectifier Transformer Characteristics. The Rectifier Transformer Subcommittee prepared a report on rectifier transformer characteristics, which was presented at the Winter General Meeting. The report has been submitted to the AIEE for publication.

Bibliography on Electronic Power Converters. The Papers and Speakers Subcommittee has completed a bibliography on electronic power converters, and it has been submitted to the AIEE for publication. The bibliography covers the years from 1903 to the end of 1947. It has 1,550 references to articles in English-language and foreign-language publications.

Inductive Co-ordination on D-C Side of Rectifier Installations. A report on inductive co-ordination aspects relating to the d-c side of rectifier installations is being prepared by a working group under the auspices of the committee. The first draft of the report has been completed. It is expected to have the report completed in time for presentation at the Winter General Meeting.

Conference on Education in Power Electronics. A conference on education in power electronics was held during the Winter General Meeting, under the joint sponsorship of the Committees on Electronic Power Converters, Electronics, and Education. The program included speakers from engineering schools and industry. A report on the conference was published in the April issue of *Electrical Engineering*.

West-Coast Subcommittee. The first organization meeting of this subcommittee was held in Spokane, Wash., on August 26, 1948. The membership of the subcommittee and the scope of its activities were discussed.

COMMITTEE ON ELECTRONICS

One of the very important activities of the committee is that of insuring that early and adequate attention be given by the AIEE to new techniques and engineering practices employing electronic devices. In pursuit of that objective, the committee has added, during the past year, a Subcommittee on Magnetic Amplifiers, and a Subcommittee on Electronic Applications of Semiconductors, and has changed the Subcommittee on Infrared Applications to a Subcommittee on Industrial Spectroscopy, thus enlarging its scope.

The practice of operating with two distinctive types of subcommittees has been continued. One type deals with general functions, such as the processing of Standards, papers solicitations and Section contacts, maintenance of national and informational contacts, education, and liaison with other professional groups. The other type is responsible for attention to specific technical fields. The committee ended the year with seven general-function subcommittees and with 14 subcommittees dealing with specific technical activities.

Plans of operation for the 1948–49 period were formulated at a meeting of subcommittee chairmen in New York, N. Y., in September 1948. A meeting of the entire committee was held in Chicago, in November 1948, in connection with the National Electronics Conference. A meeting was held at the Winter General Meeting.

At the Summer General Meeting, the committee sponsored a technical session dealing with general trends in: electron tube design, mercury-arc converters, and radio-type equipment. Also, the committee assisted in arranging for a discussion of "Electronics in Engineering Education," in a program sponsored by the Committee on Education.

At the Winter General Meeting, the committee sponsored:

1. A technical session dealing with recent developments in electron tubes and circuits.
2. The usual conference session on new electronic devices.
3. A technical session on high-frequency cables.
4. A conference session on education in power electronics.
5. A technical session on electrostatic precipitation.

One of the committee's policies is to encourage the presentation, in sessions sponsored by other committees of the Institute, of papers having an important electronics content. It is interesting to note in this connection that approximately one-half of all of the papers which were presented at the Winter General Meeting referred to some phase of electronics as an essential feature of the treatment.

For the 1949 Summer General Meeting, the committee will sponsor a technical session on infrared applications, and a technical session on magnetic amplifiers; it also will add support to the symposium on semiconductors, sponsored by the Committee on Basic Sciences.

Late in November 1948, a 3-day Conference on Electronic Instrumentation in Nucleonics and Medicine was held in New York, N. Y., sponsored jointly by the Committees on Electronics and Nucleonics of the AIEE, and the Nucleonics Studies Committee of the IRE. This conference was very well attended, and indications are that a similar conference will be held in the late fall of 1949.

In April 1949, a 2-day Conference on Industrial Electron Tubes was held in Buffalo, jointly sponsored by the Subcommittee on Electron Tubes of the Committee on Electronics and the Subcommittee on Electronic Control of the Committee on Industrial Control. This conference consisted of five technical sessions and a dinner meeting. In general, the objective was to permit exchange of information between industrial electronic circuit engineers and industrial electron tube engineers. The conference was very well attended, there was active discussion by those who attended, and it is believed the program was a valuable contribution.

Activities of the subcommittees of the general-function type were as follows:

Electronic Papers Solicitation and Section Contacts. This subcommittee accepts the responsibility for organizing technical sessions at general meetings, for following through to procurement suggestions for technical papers originating within its own members and from members of the technical subcommittees, for encouraging and stimulating papers involving electronics but sponsored by other committees, and for assisting Sections in stimulating electronic contacts.

Electronic Standards. This subcommittee is the channel for processing Standards through to the American Standards Association, and renders active assistance toward familiarizing the committee as a whole with Standards procedures.

Liaison With IRE. This subcommittee has the responsibility of maintaining satisfactory co-ordination with the Institute of Radio Engineers.

Liaison With JETEC. This subcommittee has maintained active co-ordination with JETEC, particularly in regard to matters of policy and areas of standardization activity.

Education in Electronics. This subcommittee has been very active in strengthening contacts as to electronics between industry and universities, and in providing exchange as to points of view among universities in relation to methods of education in electronics. The most significant contribution this year was the technical session on this subject at the Winter General Meeting.

West Coast Subcommittee. This subcommittee has been active in maintaining local contact in the West Coast area, particularly California, between the various electronics activities of importance to the AIEE. Plans are being formulated for a symposium on electronic instrumentation to provide an interchange of ideas between electronic industries in the West Coast area.

Canadian Liaison Subcommittee. This subcommittee is functioning actively in maintaining close contact with electronic developments in Canada, and in addition has maintained a link with electronic developments in Europe.

Activities of subcommittees in technical fields have been as follows:

Electron Tubes. This subcommittee has four working groups in the field of industrial electron tube standards in the following fields: vacuum tubes, cathode-ray tubes, gas-filled thermionic and cold cathode tubes, and mercury-pool cathode tubes. This subcommittee was responsible for the share of the Committee on Electronics in the sponsoring of the Industrial Electron Tube Conference in Buffalo.

X-Ray Tubes and Apparatus. This subcommittee provided two technical papers for a technical session at the Winter General Meeting, has completed its recommendations for changes in regard to X-ray graphical symbols, and has undertaken a revision of the American Standards Association X-ray terms. A study of specifications of characteristics and ratings of tubes used for X-ray equipment is under way.

Electronic Instruments. Activity in the electronic instrument field is handled through the Joint Subcommittee on Electronic Instruments, sponsored co-operatively with the Committee on Instruments and Measurements. This joint subcommittee sponsored two technical sessions at the Winter General Meeting, one on electronic instruments, and one on electronic digital computers. It has actively reviewed the results of the Conference on Instrumentation Tubes held in March 1948 in Philadelphia, and has used the results of that conference for furthering subsequent activities of smaller groups in the same field, and for initiating a Standards program.

Electronic Precipitation. This subcommittee sponsored a technical session at the Winter General Meeting.

High-Frequency Conductors, Cables, and Connectors. This subcommittee was responsible for a technical session at the Winter General Meeting.

Hot-Cathode Electronic Power Converters. This subcommittee has continued to be very active in bringing nearer to completion standard definitions properly correlated with similar standards in mercury-pool rectifiers. Close liaison is being maintained in all respects between this group and the AIEE committee dealing with mercury-pool converters.

Liaison With Nucleonics. This subcommittee has been responsible for maintaining close contact with the activities of the Committee on Nucleonics and other similar groups, and has been instrumental in clarifying judgment as to the proper relationship of the main committee to nuclear studies.

Electronic Aids to Medicine. This subcommittee was the avenue of the sponsorship by the Committee on Electronics of the 1948 Conference on Electronic Instrumentation in Nucleonics and Medicine in New York, N. Y., and is carrying forward plans for a similar conference late next fall. Also, the subcommittee is canvassing the proper setup for adequate liaison with the American Medical Association.

Electronic Aids to Navigation. The responsibility of this subcommittee has been for maintaining close contact with, and making contributions to, the activities of an IRE committee active in this field. Closely associated through overlapping membership is a committee of the Radio Technical Commission for Aeronautics. Liaison is also maintained with the Institute of Aeronautical Sciences, the Joint Technical Advisory Committee, the Air Materiel Command, and the Institute of Navigation. There recently has been achieved by the IRE group agreement on a tentative list of terms and definitions, and other standards procedures are under way.

Electronic Aids to Geophysics. This subcommittee sponsored last fall, jointly with an IRE group, a symposium on electronic circuitry in geophysical work, held in the Texas area where the greatest activity in this field exists. An important objective of this subcommittee's work is to enlarge the area of contact, by means of technical publication and otherwise, between geophysical electronic work and other areas of electrical engineering.

Infrared Applications. This subcommittee arranged a technical session for the Summer General Meeting. At present it is planned to extend the scope of this subcommittee to include industrial spectroscopy, thus broadening the base of its activities.

Electronic Preparation and Preservation of Food. This recently organized subcommittee is looking forward to activity in providing for presentation to the Institute of technical papers regarding progress in this field.

Magnetic Amplifiers. This new subcommittee was organized during the past year in recognition of the facts that magnetic amplifiers recently have become of substantial importance, that the circuit principles involved are basically the same as those in purely electronic circuits, and that nearly all magnetic amplifiers include important electron tube elements in their circuits. This subcommittee has arranged a technical session for the Summer General Meeting, and is maintaining a close liaison with the Committee on Basic Sciences. Standards activities are being planned.

Electronic Applications of Semiconductors. It is the responsibility of this newly organized subcommittee to give adequate attention, in the way of technical sessions and papers within the AIEE, to engineering applications to the newer solid-state devices, including transistors, crystal rectifiers, and related devices. Standards activities are under consideration.

COMMITTEE ON INSTRUMENTS AND MEASUREMENTS

Because measurement is fundamental to scientific progress, the activities of the committee are related to all branches of Institute work, and close liaison is paramount. The work of the committee has been aimed at the stimulation of interest in instruments and measurements and the co-ordination of effort in this field, including the preparation of appropriate Standards. To keep pace with the expansion of the measuring art, the membership was increased to 49. Meetings were held on November 9, 1948, and March 30, 1949. The latter was at the National Bureau of Standards, with Dr. F. B. Silsbee as host, and included an inspection trip of a number of the laboratories of the Bureau of Standards, as well as a visit to the Naval Ordnance Laboratory the following day.

About 50 technical and conference papers were presented during the year under the auspices of the main committee and its direct subcommittees, and about 50 additional papers were sponsored by the joint subcommittees. The range of subject matter testifies to the extreme diversity of measurement work.

In considerable measure, the instruments and measurements work is carried out through nine subcommittees whose activities have been as follows:

Subcommittee on Revision of Standard 4 (Measurement of Test Voltage in Dielectric Tests). The most important item on the program of this subcommittee is the standardization of methods for short-time impulse measurements. This problem is being studied by the NEMA Laboratory Co-ordination Subcommittee.

Subcommittee on Revision of C39 (Electrical Indicating Instruments). This subcommittee has co-operated with the ASA subcommittee on the revision of the C39 Standard. The first part of the Standard, covering switchboard and panel instruments, has been approved by ASA and printed for general distribution. Work on the second part, covering portable and laboratory standard-type instruments, is nearing completion.

Subcommittee on Master Test Code for Resistance Measurements. A revised draft of the trial code published in September 1947 is being circulated for approval of the Standards Committee and final printing.

Subcommittee on Master Test Code for Temperature Measurements. A proposed new code was published in October 1948 for one year's trial use.

Subcommittee on Marking of Varmeters and Related Instruments. A questionnaire has been circulated to obtain data on present practices regarding the connection and scale markings of the varmeter and related instruments. Further activities of the subcommittee will be based on the results of this survey.

Subcommittee on Watt-Hour Meters. This subcommittee sponsored nine technical and conference papers.

Subcommittee on Electrical Tests on Dielectrics in the Field. This subcommittee sponsored two sessions at the Winter General Meeting, at which eight conference papers were presented.

Subcommittee on High-Frequency Measurements. This subcommittee was responsible for planning the Conference on High-Frequency Measurements, held in Washington, D. C., January 10–12, 1949, under the joint sponsorship of AIEE, IRE, and the National Bureau of Standards. Attendance totaled 584. The community of interest in the subject gave rise to animated discussion.

Subcommittee on Definitions. The activities of this subcommittee have been limited to review of the definitions prepared by Subcommittee 6 of ASA C42.

Necessary co-ordination with the work of the other technical committees of the Institute is achieved through a number of joint subcommittees and in some instances, by cross representation. The increasing number of technical committees has augmented the problem of maintaining proper liaison. While present arrangements have proved highly satisfactory, a study is being made of intercommittee relationships to see whether any modifications in the existing scheme might be advantageous. The activities of the joint subcommittees are summarized in the following:

Joint Subcommittee on Electronic Instruments. This subcommittee sponsored two sessions at the Winter General Meeting. Work is underway on the development of specifications for the performance of vacuum tube voltmeters and cathode-ray oscilloscopes.

Joint Subcommittee on Servomechanisms. This subcommittee sponsored one session at the Winter General Meeting.

Joint Subcommittee on Nucleonic Instruments. This subcommittee, in co-operation with the Committee on Nucleonics and the Committee on Electronics Subcommittee on Electronic Aids to Medicine, sponsored a joint AIEE-IRE Conference on Electronic Instrumentation in Nucleonics and Medicine in New York, N. Y., November 29–December 1, 1948. The conference was quite successful with a total attendance of 540. Also, the subcommittee sponsored sessions at the Pacific General Meeting and the Midwest General Meeting.

Joint Subcommittee on Telemetering. A revision of the October 1941 report on "Telemetering, Supervisory Control, and Associated Circuits," has been issued. Three technical papers were sponsored by the subcommittee.

Joint Subcommittee on Instrument Transformers. Working groups are active on current transformers, test code, and rating factors.

COMMITTEE ON NUCLEONICS

This committee continued its activities in the field of nucleonics with a membership of approximately 25, and with four subcommittees as follows: Joint Subcommittee on Nucleonic Instruments (Joint with Committee on Instruments and Measurements); Subcommittee on Isotopic Tracers; Subcommittee on Safety and Health Protection Against Radiation; Subcommittee on Nuclear Physics.

Committee meetings were held in Spokane, Wash., Washington, D. C., and New York, N. Y., and a meeting of the committee officers and subcommittee chairmen was held in Cambridge, Mass., on August 10, 1948.

The committee activities were directed mainly toward bringing nucleonic information to the Institute membership, and thus stimulating interest in this field in which the electrical engineer must play an increasingly important part. To this end the following meetings were arranged.

Conference session at Pacific General Meeting, with four papers covering health physics, nucleonic instruments, accelerators, and atomic power reactors, sponsored by the Subcommittee on Nucleonic Instruments.

Conference session at Middle Eastern District Meeting, with four papers by different authors covering the same subjects for the different audience, sponsored by the Subcommittee on Nucleonic Instruments.

Conference session at the Winter General Meeting, with four papers covering health physics, and industrial and research applications and techniques for radioactive tracers, sponsored by the Subcommittees on Isotopic Tracers and on Safety and Health Protection.

Conference session at South West District Meeting, with three papers covering accelerators and radioactive tracer applications in medicine and biology, sponsored by the Subcommittee on Nuclear Physics.

Joint AIEE-IRE Conference on Electronic Instrumentation in Nucleonics and Medicine held in the Engineering Societies' Building, in New York, on November 29, 30, and December 1, with 26 papers.

The last-named meeting was sponsored jointly with the Committee on Electronics Subcommittee on Electronic Aids to Medicine, in co-operation with the Institute of Radio Engineers. Approximately 540 people attended. The program was well received. A questionnaire was sent to those who attended, and, on the basis of replies, it has been decided that a similar conference should be held in 1949.

In addition to meetings, the committee sponsored the publication of additional papers on nucleonic subjects, and the nucleonic series in *Electrical Engineering* was completed. A bibliography was prepared covering other literature on the subject, and the complete series has been published in pamphlet form under the title "Elements of Nucleonics for Engineers."

Finally, the committee participated in arrangements for the co-ordination of the various glossaries on nucleonics under the National Research Council, and is collaborating on the preparation of the section on instrumentation.

General Applications Group

COMMITTEE ON AIR TRANSPORTATION

The Committee on Air Transportation held one meeting of the entire committee during the year. This meeting was held in Washington on the occasion of the Middle Eastern District Meeting. The activities of the committee for the coming year in the field of Standards were discussed, and plans were made for technical sessions at the 1949 Pacific General Meeting. Because of the wide geographical spread of the electrical engineers interested in aircraft transportation, it is thought that technical sessions on this subject should be concentrated at one principal meeting during the year. For this reason it is planned to concentrate efforts on technical papers at the coming Pacific General Meeting. The Standards activities of the committee are carried on by its several subcommittees, and the status of the committee's work is described under the headings of the several subcommittees in the following paragraphs.

Aircraft Electric Rotating Machinery. The "Proposed Test Code for D-C Aircraft Electric Rotating Machines," AIEE Report 800, which was issued in March 1947 for one year's trial use, is being revised for reissue. It was thought that in the present form this report was not ready for standardization in the form of a test code, and it is expected that this will be available for reissue within the next few months.

Joint Subcommittee on Carbon Brushes. This subcommittee, which is joint with the Committee on Rotating Machinery and the Committee on Air Transportation, was formed for the purpose of writing a code for testing carbon brushes and brush materials. It made an interim report at the recent Winter General Meeting. At that time, a recommendation was made that the present subcommittee be disbanded and a new joint subcommittee formed with the fore-named AIEE committees and the Carbon Brush Committee of the NEMA Technical Committee. This action now is awaiting the approval of the Standards Committee. It is hoped that the new joint subcommittee may be completed at an early date.

Aircraft Electrical Control, Protective Devices, and Cable. This subcommittee has prepared a "Proposed Test Code for Aircraft Circuit Interrupting Devices (12-, 24-, and 120-Volt D-C Systems)," which has been submitted to the Committee on Air Transportation for approval, with the view of recommending it to the Standards Committee for issue for one year's trial use. This subcommittee also is preparing a test code for aircraft generator voltage regulators. It is expected that this code also will be issued for one year's trial use by the industry before being proposed as a Standard. This subcommittee is planning next to prepare a code for testing the interrupting capacity of aircraft d-c circuit breakers. It is expected also that this group will undertake the preparation of a report on the "Recommended Practice for the Installation and Protection of Electric Wiring in Aircraft."

Basic Principles of Altitude Rating. This subcommittee has been assigned the task of preparing an AIEE Standard on the basic principles of altitude rating of electric machinery. It is expected that this rating will extend the altitude considerations to 50,000 feet and take into consideration such factors as temperature, pressure, and humidity, and will furnish data for determining safe rating of aircraft electric machinery. It is planned that a survey will be conducted of the users of altitude chambers to determine the correlation of data obtained in flight with that obtained in altitude chambers, and it is hoped that this survey may result in a more unified practice in the use of altitude chambers.

Aircraft Electric Systems. This subcommittee was engaged in the preparation of new material for inclusion in the report "Report on Aircraft Electric System Guide," AIEE 750, which was published in July 1947. The guide was issued in an incomplete state, and it is expected that revision will be a continual process in order that it may be kept up to date in regard to aircraft electric system development. It is planned that other subcommittees will furnish information pertaining to their assigned fields of activity for inclusion in the "Systems Guide."

COMMITTEE ON DOMESTIC AND COMMERCIAL APPLICATIONS

The committee has been active in the preparation and presentation of papers, and in promoting a greater awareness among the engineers in the appliance field both of the value of the Institute and of the part which appliance engineers play in developments in the electrical industry.

The scope of the Committee on Domestic and Commercial Applications is such that the engineering requirements do not lend themselves readily to standardization such as is normally considered within the field of the Institute's activities. As a consequence, no work on Standards has been conducted or contemplated. Many excellent papers have been prepared, however, and presented at various Institute meetings. Papers were presented at the Summer General Meeting and at the Midwest General Meeting, and a symposium of six papers on the control and protection of household electric equipment was held at the Winter General Meeting.

All sessions where papers were presented under the auspices of this committee were well attended, and four of the papers were considered to be of sufficient general interest to be acceptable for publication in *Electrical Engineering*.

Interest in the program sponsored by the committee resulted in six engineers affiliating with the Institute and the renewal of activity on the part of members in several Districts who had believed that their particular field was not adequately covered.

COMMITTEE ON LAND TRANSPORTATION

The committee held one well-attended and very full business meeting on February 1, 1949. At this meeting, the committee heard a very interesting discussion by T.M.C. Martin on the question "Will Complete Dieselization Save Oil?" The following activities also were discussed and organized.

Technical Meetings. Two transportation papers were presented at the Summer General Meeting, and four were presented at the Midwest General Meeting. These related to short-time ratings of diesel-electric locomotives, energy sources for railroad motive power, head end power on railroad passenger trains, and handling of suburban traffic on steam railroads. Seven papers were presented at the Winter General Meeting. These covered application of diesel-electric locomotives, 25 years progress in the design of traction motors (report on the progress of the art), the renaissance of the electric motive power, the electric equipment for Chesapeake and Ohio steam turbine-electric locomotives, selenium rectifiers in motor vehicle power systems, trackless trolley operations in Rhode Island, and a review of cities where trolley coaches have replaced busses.

Subcommittee Activities. The review of AIEE Standard 16, "Standards for Electric Railway Control Apparatus," has been completed, and this Standard now includes coverage of the electric portions of the electropneumatic brake system. This Standard is being issued for trial use.

COMMITTEE ON PRODUCTION AND APPLICATION OF LIGHT

One meeting of the entire committee was held. This meeting was held at the Winter General Meeting. A conference on fluorescent lighting, which was sponsored by the committee, was held on the afternoon of the same day. One of the subjects which was discussed at the committee meeting was the scope of the committee's activities. As a result of this discussion, it was concluded that one of the primary objectives is to keep AIEE membership advised on lighting matters, by encouraging meetings on the subject at general meetings, and Section meetings. Earlier in the year, the chairman had proposed to the Sections Committee that a list of speakers available for various local activities be distributed to the Sections. The Sections Committee had responded by indicating that it would like to consider the inauguration of such a procedure for all technical committees.

Reports were presented on the activities of the following subcommittees:

Subcommittee on Ultraviolet Radiation. This subcommittee has been working on a paper giving general background material on ultraviolet radiations. The manuscript was distributed to the committee, and several suggestions made for bringing it to completion.

Subcommittee on Infrared Radiation. This subcommittee is working in co-operation with two other AIEE infrared subcommittees; Radiant Heating Subcommittee under the Committee on Electric Heating; Infrared Applications Subcommittee under the Committee on Electronics. This subcommittee will encourage the use of electric infrared radiation for those processes for which it is suitable, by the collection and dissemination of available data, as well as the encouragement of new research. Investigations are being carried on at the present time on infrared sources, techniques, and measuring instruments. A program is also under way for the collection of fundamental application data.

Subcommittee on Nomenclature of Electric Discharge Lamps. The report which had been prepared by this subcommittee had been circulated to members of the committee prior to the meeting. A number of written comment had been received, and these were discussed, together with other comments which were brought in by other committee members or evolved out of the discussion. The chairman of the subcommittee agreed to prepare an additional draft, incorporating the suggestions which the committee had agreed should be included. The final draft should be ready for circulation shortly.

COMMITTEE ON MARINE TRANSPORTATION

Two well-attended meetings of the committee were held at Institute headquarters, one in January, and one in April, 1949.

The latest revision of "Recommended Practice for Electrical Installation on Shipboard," was issued in December 1948. The committee has been working on the matter of international standardization of electrical installation on shipboard. This work was delegated to the Committee on Marine Transportation by action of the Board of Directors of the Institute.

Technical Meeting. Four marine transportation sessions were held at the Middle Eastern District Meeting. The sessions were very well attended, each having from 50 to 125 people present. The first session covered the subjects of insulating materials and magnetic amplifiers, and the second covered the subjects of circuit breakers and shipboard degaussing installations. Three papers were presented at each. The third session covered the subject of power on shipboard, with four papers presented and discussed. The fourth session covered the subject of shipboard lighting, with three papers presented and discussed. These sessions all created considerable interest in the work of the committee.

Subcommittee Activities. These subcommittees have been quite active, each reviewing the sections of Standards 45 under its cognizance, and making reports to the main committee.

Awards

COMMITTEE ON AWARD OF INSTITUTE PRIZES

On account of the adoption of the new rules, with a shift from the calendar year to the administrative year, no awards were made this spring. The committee held one meeting for consideration of the proposed changes in rules submitted by a subcommittee of the Sections Committee, and will consider the proposals again when they are in final form.

CHARLES LEGEYT FORTESCUE FELLOWSHIP

During the year, one meeting of the committee was held to select the recipient of the Fortescue Fellowship award for the scholastic year 1949–50. One full fellowship of $1,500 was awarded to E. F. Koncel, Jr. A continuing fellowship in the amount of $500 was awarded to last year's recipient to assist him in completing the second year of his graduate work.

Consideration is being given to several proposals to arouse more interest on the part of faculty members so that competition for this fellowship will be more widespread.

COMMITTEE ON MEMBERS-FOR-LIFE FUND

The committee took no further action during the past year. The Board of Directors approved its recommendations that a statement regarding the use of the fund be published in *Electrical Engineering* and mailed to each new Member-for-Life with the notification; that a specially designed card be mailed to all living Members-for-Life and in the future be mailed with the official notice; and that, when the growth of the fund warrants, prize winners from all Districts be included in the provision for traveling expense allowance to the Summer General Meeting.

EDISON MEDAL

The Edison Medal for 1948 was awarded to Dr. Morris E. Leeds, Chairman of the Board, Leeds and Northrup Company, Philadelphia, Pa., "for his contributions to the industry through development and production of electrical precision measuring devices and controls," and was presented to him on February 2, 1949, in a general session of the Winter General Meeting.

The medal may be awarded annually for meritorious achievement in electrical science, electrical engineering, or the electrical arts. Awards are made by a committee of 24 members of the Institute.

LAMME MEDAL

The Lamme Medal for 1948 was awarded to Dr. V. K. Zworykin, Vice-President and Technical Consultant of the RCA Laboratories Division, Radio Corporation of America, Princeton, N. J., "for his outstanding contribution to the concept and design of electronic apparatus basic to modern television." The medal will be presented to him on June 22, 1949, during the Summer General Meeting.

The medal may be awarded annually by a committee of nine members to a member of the AIEE for "meritorious achievement in the development of electrical apparatus or machinery."

HOOVER MEDAL

The Hoover Medal is awarded periodically to an engineer for distinguished public service by a board representing the American Society of Civil Engineers, American Institute of Mining and Metallurgical Engineers, American Society of Mechanical Engineers, and AIEE. The medal was awarded, in 1948, to Malcolm Pirnie, Consulting Engineer, New York, N. Y., with a citation as: "Engineer, leader of engineers and servant of his fellowman, whose ideals and accomplishments in public life beyond the call of his profession have benefited men in his own and other countries of the world." The medal was presented to him on January 19, 1949, at the annual meeting of the ASCE, of which he is a past president.

JOHN FRITZ MEDAL

The John Fritz Medal may be awarded annually for notable scientific or industrial achievements, by a board of award composed of representatives of the ASCE, AIME, ASME, and AIEE. The 1949 medal was awarded to Charles M. Allen, Professor Emeritus of Hydraulic Engineering, Worcester Polytechnic Institute, with the citation:

"Exceptional achievements in hydraulic engineering. Founder of notable hydraulic laboratory; prominent teacher, consultant, inventor and author." The medal was presented to him on January 19, 1949, at the annual meeting of the ASCE.

MARSTON MEDAL

The Marston Gold Medal is awarded to an alumnus of Iowa State College, of at least 30 years' standing, for achievement in engineering in a broad field. The board of award includes representatives of the college, and five engineering societies: ASCE, AIME, ASME, AIEE, and AIChE. The 1948 award was to Roy W. Crum, Director of the Highway Research Board of the National Research Council, Washington, D. C.

WASHINGTON AWARD

The Washington Award for 1949 was made to John L. Savage, Consulting Engineer, Tennessee Valley Authority, and Chief Designing Engineer, United States Bureau of Reclamation, Denver, Colo. The award is made annually to an outstanding engineer, a citizen or resident of the United States, who ably has served human needs, and is administered by a commission representing the Western Society of Engineers, ASCE, AIME, ASME, and AIEE.

ALFRED NOBLE PRIZE

This prize was established in 1929 to perpetuate the name and achievements of Alfred Noble, Past President of the ASCE and the Western Society of Engineers. It may be made to a member of any of the co-operating societies, ASCE, AIME, ASME, AIEE, or WSE, for a technical paper of particular merit accepted by the publication committee of any of these societies, provided the author is not more than 30 years of age at the time of such acceptance.

Joint Activities

UNITED ENGINEERING TRUSTEES, INC.

The funds and property held jointly by the four Founder Societies, including the Engineering Societies Building, the Engineering Societies Library, and the endowment funds of the Engineering Foundation, are administered by the United Engineering Trustees, Inc., which also serves as treasurer of the Engineers' Council for Professional Development.

The building has recently required some major repairs. It is fully occupied at present, and two of the Founder Societies have substantial amounts of space in other buildings.

ENGINEERING FOUNDATION

The Engineering Foundation is a department of the United Engineering Trustees, Inc., and its general objective is "the furtherance of research in science and engineering."

With the income from its endowment funds, it supports a broad range of research projects, now numbering about 16. Some of its most effective contributions are those supporting studies and analyses necessary in the early stages of organizing projects on such bases that financial support from other sources becomes available. A special study is being devoted to the future employment of Foundation funds towards advancing research.

ENGINEERING SOCIETIES LIBRARY

The library is a department of the United Engineering Trustees, Inc., and was formed by combining the separate libraries of the ASCE, AIME, ASME, and AIEE, and is conducted as a free public reference library.

In addition to affording the use of a large collection of engineering books and periodicals, the library renders special services such as bibliographies, translations, photostats, searches, and book loans by mail.

A broad survey is in progress for the purpose of reorienting the library with other technical libraries in the United States and for obtaining greater financial support. The bylaws governing the Library Board have been amended to reduce the number of board members from 22 to 12.

ENGINEERS' COUNCIL FOR PROFESSIONAL DEVELOPMENT

The principal activities of ECPD include programs for the guidance of young persons thinking of entering the engineering field, the accrediting of curricula of engineering schools, and encouragement and assistance to individuals in their engineering and cultural studies during several years following graduation. The council represents ASCE, AIME, ASME, AIEE, American Institute of Chemical Engineers, the Engineering Institute of Canada, the American Society for Engineering Education, and the National Council of State Boards of Engineering Examiners.

ENGINEERS JOINT COUNCIL

The membership of this council includes the two most recent available past presidents and the secretaries, also the presidents as ex-officio members without votes, of the five member societies; ASCE, AIME, ASCE, AIEE, and AIChE. Its purpose is to promote co-operation among the branches of engineering by seeking and studying matters of mutual interest to the member societies, to recommend joint action, and to administer activities authorized by a majority of the societies. A constitution was recently adopted and referred to the societies for approval.

REPRESENTATIVES

A complete list of about 40 joint bodies upon which the Institute is represented appears in the Year Book and in the October issue of *Electrical Engineering* each year.

Appreciation

The continuing rapid increase in the membership and the income of the Institute are tangible symbols of the highly effective work of the national committees, and the District, Section, and Branch officers and committees. For their enthusiasm and excellent co-operation, the Board of Directors expresses sincere thanks and appreciation. It also thanks the members in general for their keen interest in the activities and for their splendid support of established policies.

Respectfully submitted for the Board of Directors.

H. H. HENLINE
Secretary

HASKINS & SELLS
CERTIFIED PUBLIC ACCOUNTANTS

1 EAST 44TH STREET
NEW YORK 17

ACCOUNTANTS' CERTIFICATE

American Institute of Electrical Engineers:

We have examined the balance sheet of American Institute of Electrical Engineers, and schedule of securities owned, as of April 30, 1949, and the related statements of cash receipts and disbursements of operating and restricted funds for the year then ended. Our examination was made in accordance with generally accepted auditing standards, and accordingly included such tests of the accounting records and such other auditing procedures as we considered necessary in the circumstances.

In our opinion, the accompanying balance sheet, schedule of securities owned, and statements of cash receipts and disbursements, respectively, present fairly the financial position of, and securities owned by, the Institute at April 30, 1949, and its recorded cash receipts and cash disbursements for the year then ended, in conformity with generally accepted accounting principles applied on a basis consistent with that of the preceding year.

(*Signed*) HASKINS & SELLS

New York,
May 19, 1949

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Balance Sheet, April 30, 1949

Exhibit A

ASSETS		
Property Fund Assets:		
One-fourth interest in physical properties of United Engineering Trustees, Inc.:		
Land, buildings, and equipment (less depreciation and renewal reserve)	$332,383.85	
Funded depreciation and renewal reserve	166,064.63	
Total	$498,448.48	
Equipment:		
Library (volumes and fixtures)—estimated value	36,366.37	
Office furniture and fixtures (less reserve for depreciation, $31,567.26)	15,746.17	
Works of art, etc.	3,001.35	
Total property fund assets		$ 553,562.37
Restricted Fund Assets:		
Securities—at cost, less reserve, $1,788.15 (quoted market value, $533,172.52)—Schedule 1	$523,824.26	
Cash (Exhibit *C*)	56,399.79	
Accrued interest receivable	184.98	
Total restricted fund assets		580,409.03
Operating Fund Assets:		
Cash (not including $1,674.87 for Federal taxes withheld from employees)—(Exhibit *B*)	$14,795.59	
Accounts receivable:		
Members—for dues (less reserve, $5,750.00)	11,675.34	
Advertisers	2,572.37	
Miscellaneous	6,638.63	
Technical Conference loans outstanding	650.00	
Accrued interest receivable	2,042.33	
Inventories:		
Transactions, etc.	1,308.00	
"Electrical Definitions"	537.00	
Work in process *Transactions* (Part II 1948), etc.	5,726.96	
Text and cover paper	11,311.46	
Badges	7,601.64	
Deferred charges—Production charges for May issue of *Electrical Engineering*	16,127.61	
Total operating fund assets		80,986.93
Total		$1,214,958.33

LIABILITIES		
Property Fund Reserve		$ 553,562.37
Restricted Fund Reserves:		
Reserve capital fund	$531,660.79	
Life membership fund	8,312.51	
Member-for-Life fund	9,632.17	
International Electrical Congress of St. Louis library fund	6,442.30	
Lamme Medal fund	4,440.44	
Mailloux fund	1,012.15	
Volta Memorial fund	18,908.67	
Total restricted fund reserves		580,409.03
Operating Fund Reserves, Liabilities, etc.:		
Accounts payable	$ 30,007.57	
Deferred income:		
Dues received in advance	3,501.36	
Entrance fees and dues advanced by applicants for membership	2,309.08	
Subscriptions to publications received in advance	20,597.00	
Miscellaneous (including unallocated receipts)	1,753.57	
Operating fund reserves	22,818.35	
Total operating fund reserves, liabilities, etc.		80,986.93
Total		$1,214,958.33

Exhibit B Statement of Cash Receipts and Disbursements of Operating Fund for the Year Ended April 30, 1949

Cash on Deposit with The National City Bank of New York, May 1, 1948 (not including $2,031.98 for Federal taxes withheld from employees)			$ 22,394.58
Receipts:			
Dues (including $168,882.00 allocated to *Electrical Engineering* subscriptions)		$363,816.95	
Advertising in *Electrical Engineering*		169,325.25	
Transaction subscriptions		15,874.93	
Electrical Engineering subscriptions		30,662.92	
Miscellaneous publications (preprints, Standards, special reports, etc.)		49,246.60	
Students' fees		39,356.25	
Entrance fees		19,584.56	
Registration fees—Institute Meetings and Technical Conferences		13,585.71	
Membership badges		5,264.20	
Transfer fees		3,440.00	
Interest and dividends on investments of Reserve Capital Fund		29,958.02	
Miscellaneous		496.90	
Total		$740,612.29	
Refund from Radio Technical Planning Board		722.28	
Refund of expenses paid, 1947 Summer General Meeting		53.03	
Contribution from Committee on Instrumentation and Measurements		94.00	
Transfer from Property Fund		612.91	
Transfer from Reserve Capital Fund–net (see Exhibit *C*)		11,817.00	
Total receipts			753,911.51
Total			$776,306.09
Disbursements:			
Publications expense:			
Electrical Engineering text	$175,472.46		
Electrical Engineering advertising	93,594.98	$269,067.44	
Transactions		21,166.69	
Proceedings		37,636.58	
"Year Book"		12,168.99	
Special publications		2,528.15	
Miscellaneous publications (preprints, Standards, etc.)		32,498.14	
Institute meetings		32,396.51	
Institute Sections		91,039.35	
Institute Branches, including prizes, etc.		7,706.99	
Finance Committee		850.00	
Headquarters Committee		344.86	
Membership Committee		19,512.63	
Standards Committee		20,711.33	
Technical Committees		11,684.65	
Committee on Public Relations		1,183.88	
Edison Medal Committee		296.51	
Forward		$560,792.70	
Total (forward)			$776,306.09

Total (forward)		$776,306 09
Disbursements (forward)	$560,792.70	
Traveling expenses:		
Geographical Districts:		
Executive committees	6,278.69	
Vice-Presidents	1,770.67	
Conferences on Student activities	8,783.72	
Board of Directors	15,750.87	
Nominating Committee	1,568.85	
President's appropriation	4,294.33	
Administrative expenses	90,436.12	
Geographical Districts—Branch paper prizes	100.11	
Institute prizes—National	532.70	
Retirement system AIEE—normal contribution	8,395.31	
American Standards Association	1,875.00	
Canadian Radio Technical Planning Board	10.00	
Canadian Council of Professional Engineers and Scientists	278.71	
Engineers' Council for Professional Development	1,808.50	
Engineering Foundation Project—Welding research	250.00	
Engineers Joint Council—General expenses	759.50	
John Fritz Medal	75.00	
National Committee State Board Engineering Examiners	500.00	
U. S. National Committee—International Commission on Illumination	300.00	
Constitution and Bylaws Committee	1,152.48	
United Engineering Trustees, Inc.:		
Building assessments	19,547.48	
Library assessments	13,053.35	
Library Retirement Plan	3,615.43	
Lamme Medal expense (see Exhibit *C*)	124.30	
Membership badges	5,768.42	
Legal services	987.53	
Office furniture and fixtures and repairs	3,067.03	
Rent, etc., Editorial Department, 500 Fifth Avenue	6,963.30	
Total	$758,840.10	
Transfer to Member-for-Life fund	2,020.40	
Technical Conference—Loans outstanding	650.00	
Total disbursements		761,510.50
Cash on Deposit, April 30, 1949:		
The National City Bank of New York (not including $1,674.87 for Federal taxes withheld from employees)	$ 12,441.23	
The Canadian Bank of Commerce, Toronto	2,354.36	$ 14,795.59

Exhibit C Statement of Cash Receipts and Disbursements of Restricted Funds for the Year Ended April 30, 1949

		Restricted Funds						
	Total	Reserve Capital Fund	Member-for-Life Fund	Life Membership Fund	International Electrical Congress of St. Louis Library Fund	Lamme Medal Fund	Mailloux Fund	Volta Memorial Fund
Cash on Deposit With The National City Bank of New York and Various Savings Banks, May 1, 1948	$ 39,398.08	$ 25,489.06	$ 8,416.86	$3,435.68	$ 891.47	$150.00	$1,015.01	
Receipts:								
Proceeds from sales, etc. of securities	$ 82,902.20	$ 82,902.20						
Income from bonds	545.34		$ 62.50	$ 127.56	$ 135.28	$160.00		$ 60.00
Interest on bank balances	63.14			48.00			$ 15.14	
Transfer from operating fund	1,957.90		1,957.90					
Life membership fees	209.56			209.56				
Gift from Italy America Society, Incorporated	16,759.51							16,759.51
Total receipts	$102,437.65	$ 82,902.20	$ 2,020.40	$ 385,12	$ 135.28	$160.00	$ 15.14	$16,819.51
Total	$141,835.73	$108,391.26	$10,437.26	$3,820.80	$1,026.75	$310.00	$1,030.15	$16,819.51
Disbursements:								
Purchase of securities	$ 71,791.32	$ 66,791.32	$5,000.00					
Transfers to operating fund:								
To be repaid when adequate funds become available	11,817.00	11,817.00						
Other	709.36			$ 709.36				
Purchase of medal, cost of engraving, etc. (exclusive of $124.30 paid from operating fund)	150.00					$150.00		
Library purchases	111.09				$ 93.09		$ 18.00	
Traveling expenses	857.17		857.17					
Total disbursements	$ 85,435.94	$ 78,608.32	$5,857.17	$ 709.36	$ 93.09	$150.00	$ 18.00	
Cash on Deposit With The National City Bank of New York and Various Savings Banks, April 30, 1949	$ 56,399.79	$ 29,782.94	$4,580.09	$3,111.44	$ 933.66	$160.00	$1,012.15	$16,819.51

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Securities Owned, April 30, 1949

Schedule 1

	Principal Amount of Bonds or Number of Shares of Stock	Restricted Funds						
		Reserve Capital Fund	Life Membership Fund	Member for Life Fund	International Electrical Congress Library Fund	Lamme Medal Fund	Volta Memorial Fund	Total
Railroad Bonds:								
Atlantic Coast Line First Consolidated 4%, due 1952	$ 4,000.00					$4,253.78		$ 4,253.78
Baltimore & Ohio, Pittsburgh, Lake Erie & West Virginia System Refunding 4%, due 1980	10,000.00	$ 6,450.00						6,450.00
Chicago & Erie Railroad Company 5% first mortgage, due 1982, registered	1,000.00	1,105.00						1,105.00
New York Central Railroad Company 4% series *A* consolidated mortage, due 1998	15,000.00	9,812.50						9,812.50
Northern Pacific Railway Company 4½% collateral trust, due 1975	15,000.00	15,307.50						15,307.50
St. Louis-San Francisco Railway Company 4% series *A*, first mortgage, due 1997	1,200.00	1,200.00						1,200.00
St. Louis-San Francisco Railway Company 4% series *A*, first mortgage, scrip	198.00	198.00						198.00
St. Louis-San Francisco Railway Company 4½% series *A*, second mortgage income bonds, due 2022	1,300.00	1,300.00						1,300.00
St. Louis-San Francisco Railway Company 4½% series *A*, second mortgage income bonds, scrip	61.10	61.10						61.10
Total railroad bonds		$ 35,434.10				$4,253.78		$ 39,687.88
Public Utility Bonds:								
American Telephone & Telegraph Company 2¾% convertible debentures, due 1961	$40,000.00	$ 33,154.75	$5,153.12		$5,457.85			$ 43,765.72
Consolidated Edison Company of New York, Incorporated 3% convertible debentures, due 1963	25,000.00	27,281.25						27,281.25
Total public utility bonds		$ 60,436.00	$5,153.12		$5,457.85			$ 71,046.97
United States Government Bonds:								
Treasury bonds 2%, due March 15, 1952/50	$ 3,000.00	$ 3,074.22						$ 3,074.22
Treasury bonds 3%, due September 15, 1955/51	2,000.00						$2,081.66	2,081.66
Defense bonds series *G*, 2½%, due December 1, 1954	18,000.00	13,000.00		$5,000.00				18,000.00
Treasury Savings bonds series *G*, 2½%, due September 1, 1955	40,000.00	40,000.00						40,000.00
Treasury Savings bonds series *G*, 2½%, due November 1, 1956	17,000.00	17,000.00						17,000.00
Treasury Savings bonds series *G*, 2½%, due May 1, 1957	20,000.00	20,000.00						20,000.00
Treasury Savings bonds series *G*, 2½%, due October 1, 1957	30,000.00	30,000.00						30,000.00
Total United States Government bonds		$123,074.22		$5,000.00			$2,081.66	$130,155.88
Capital Stocks:								
American Can Company	60 shares	$ 4,988.40						$ 4,988.40
American Gas & Electric Company	400 shares	15,137.62						15,137.62
Atchison, Topeka & Santa Fe Railway Company preferred	200 shares	19,174.71						19,174.71
Atlantic City Electric Company	40 shares	612.00						612.00
Boston Edison Company	200 shares	4,927.50						4,927.50
Commonwealth Edison Company	200 shares	7,580.68						7,580.68
Consolidated Natural Gas Company	100 shares	4,428.80						4,428.80
Dow Chemical Company $4 cumulative preferred, series A	100 shares	11,547.50						11,547.50
Eastman Kodak Company	315 shares	10,694.11						10,694.11
E. I. du Pont de Nemours & Company	80 shares	13,096.68						13,096.68
General Electric Company	200 shares	7,748.66						7,748.66
General Motors Corporation $5 preferred	200 shares	25,820.00						25,820.00
General Motors Corporation	100 shares	4,235.53						4,235.53
Gulf Oil Corporation	200 shares	12,264.20						12,264.20
Insurance Company of North America	100 shares	10,847.50						10,847.50
International Harvester Company	300 shares	5,030.50						5,030.50
International Match Realization Co. capital stock	6 shares	1,788.15*						1,788.15*
Louisville & Nashville Railroad Company	100 shares	6,278.13						6,278.13
Ohio Edison Company 4.40% preferred	200 shares	21,279.25						21,279.25
Pacific Gas and Electric Company	200 shares	8,420.99						8,420.99
Public Service Electric and Gas Company $1.40 dividend preference common stock	370 shares	11,150.00						11,150.00
St. Louis-San Francisco Railway Company voting trust certificates for 5% series *A* preferred stock	21 1800/10000ths shares	582.75						582.75
St. Louis-San Francisco Railway Company voting trust certificates for common stock	42 6000/10000ths shares	357.00						357.00
Scoville Manufacturing Company 3.65% cumulative preferred	100 shares	10,111.25						10,111.25
Sears, Roebuck and Co.	400 shares	6,014.97						6,014.97
Socony Vacuum Oil Company	408 shares	8,381.20						8,381.20
Standard Oil Company of Indiana	200 shares	8,170.32						8,170.32
Standard Oil Company of New Jersey	$212^{35}/_{200}$ shares	9,761.26						9,761.26
Texas Gulf Sulphur Company	$3^{5}/_{65}$ shares	144.60						144.60
Union Carbide & Carbon Corporation	300 shares	7,277.42						7,277.42
United Fruit Company	300 shares	11,985.00						11,985.00
United States Steel Corporation 7% cumulative preferred	100 shares	14,885.00						14,885.00
Total capital stocks		$284,721.68						$284,721.68
Total		$503,666.00	$5,153.12	$5,000.00	$5,457.85	$4,253.78	$2,081.66	$525,612.41
* Less reserve in full for securities considered to be of doubtful value		1,788.15						1,788.15
Total Securities, Less Reserve		$501,877.85	$5,153.12	$5,000.00	$5,457.85	$4,253.78	$2,081.66	$523,824.26

Officers of A.I.E.E. for 1949–1950

PRESIDENT

James F. Fairman
New York, N. Y.
(Term expires July 31, 1950)

PAST PRESIDENTS

Blake D. Hull, Dallas, Tex.
(Term expires July 31, 1950)

Everett S. Lee, Schenectady, N. Y.
(Term expires July 31, 1951)

VICE-PRESIDENTS

District
1 Victor Siegfried, Worcester, Mass.
3 J. L. Callahan, New York, N. Y.
5 E. W. Seeger, Milwaukee, Wis.
7 G. N. Pingree, Dallas, Tex.
9 Richard McKay, Spokane, Wash.
(Terms expire July 31, 1950)

District
2 C. G. Veinott, Lima, Ohio
4 W. J. Seeley, Durham, N. C.
6 W. C. DuVall, Boulder, Colo.
8 Ralph A. Hopkins, Los Angeles, Calif.
10 A. H. Frampton, St. Catharines, Ont.
(Terms expire July 31, 1951)

DIRECTORS

W. J. Barrett, Newark, N. J.
R. T. Henry, Buffalo, N. Y.
E. P. Yerkes, Philadelphia, Pa.
(Terms expire July 31, 1950)

W. L. Everitt, Urbana, Ill.
A. C. Monteith, Pittsburgh, Pa.
Elgin B. Robertson, Dallas, Tex.
(Terms expire July 31, 1951)

C. W. Fick, Cleveland, Ohio
M. D. Hooven, Newark, N. J.
F. O. McMillan, Corvallis, Oreg.
(Terms expire July 31, 1952)

E. W. Davis, Cambridge, Mass.
N. B. Hinson, Los Angeles, Calif.
H. J. Scholz, Birmingham, Ala.
(Terms expire July 31, 1953)

TREASURER

W. I. Slichter, New York, N. Y.
(Term expires July 31, 1950)

SECRETARY

H. H. Henline, New York, N. Y.
(Term expires July 31, 1950)

LOCAL HONORARY SECRETARIES

Australia—V. J. F. Brain, Electricity Authority of N.S.W., Box 2600 G.P.O., Sydney, New South Wales

Brazil—Richard H. Bowles, Sao Paulo Tramway Light and Power Company, Caixa Postal 26-B, Sao Paulo

England—Sir A. P. M. Fleming, Metropolitan Vickers Electric Company, Trafford Park, Manchester 17

India, Northern—S. S. Kumar, P.W.D. Electricity Secretariat, Ellerslie, Simla-E, East Punjab

India, Southern—S. M. Zubair, Tata Hydro Electric Company, Bombay House, Bruce Street, Fort Bombay

New Zealand—P. H. Powell, 130 Glandovey Road, Fendalton, Christchurch, N. W. 1 (Acting)

Sweden—Edy Velander, Box 5073, Stockholm 5

Transvaal—Francis E. Ingham, Box 6067, Johannesburg, Transvaal, South Africa

PAST PRESIDENTS—*1884–1949*

*Norvin Green	1884–85–86
*Franklin L. Pope	1886–87
*T. Commerford Martin	1887–88
*Edward Weston	1888–89
*Elihu Thomson	1889–90
*William A. Anthony	1890–91
*Alexander Graham Bell	1891–92
*Frank Julian Sprague	1892–93
*Edwin J. Houston	1893–94–95
*Louis Duncan	1895–96–97
*Francis Bacon Crocker	1897–98
*A. E. Kennelly	1898–1900
*Carl Hering	1900–01
*Charles P. Steinmetz	1901–02
*Charles F. Scott	1902–03
*Bion J. Arnold	1903–04
*John W. Lieb	1904–05
*Schuyler Skaats Wheeler	1905–06
*Samuel Sheldon	1906–07
*Henry G. Stott	1907–08
*Louis A. Ferguson	1908–09
*Lewis B. Stillwell	1909–10
Dugald C. Jackson	1910–11
Gano Dunn	1911–12
Ralph D. Mershon	1912–13
*C. O. Mailloux	1913–14
*Paul M. Lincoln	1914–15
*John J. Carty	1915–16
H. W. Buck	1916–17
*E. W. Rice, Jr.	1917–18
Comfort A. Adams	1918–19
*Calvert Townley	1919–20
*A. W. Berresford	1920–21
William McClellan	1921–22
Frank B. Jewett	1922–23
*Harris J. Ryan	1923–24
*Farley Osgood	1924–25
*M. I. Pupin	1925–26
*C. C. Chesney	1926–27
*Bancroft Gherardi	1927–28
*R. F. Schuchardt	1928–29
*Harold B. Smith	1929–30
*William S. Lee	1930–31
C. E. Skinner	1931–32
H. P. Charlesworth	1932–33
J. B. Whitehead	1933–34
*J. Allen Johnson	1934–35
*E. B. Meyer	1935–36
A. M. MacCutcheon	1936–37
W. H. Harrison	1937–38
John Castlereagh Parker	1938–39
F. Malcolm Farmer	1939–40
R. W. Sorensen	1940–41
David C. Prince	1941–42
Harold S. Osborne	1942–43
Nevin E. Funk	1943–44
C. A. Powel	1944–45
*W. E. Wickenden	1945–46
J. Elmer Housley	1946–47
B. D. Hull	1947–48
Everett S. Lee	1948–49

*Deceased.

Past and Present Officers of the Institute

Abbott, Arthur V., _V-P._, 1900–02.
Adams, Comfort A., _M._, 1912–15; _V-P._, 1915–17; _P._, 1918–19.
*Adsit, C. G., _V-P._, 1921–23.
Alger, P. L., _D._, 1944–48.
*Anthony, Wm. A., _V-P._, 1886–89; _P._, 1890–91; _V-P._, 1894–96.
*Armstrong, A. H., _M._, 1903–06; _V-P._, 1906–08.
*Arnold, Bion J., _M._, 1895–98; _V-P._, 1902–03; _P._, 1903–04.
Auty, K. A., _V-P._, 1932–34.

*Babcock, A. H., _V-P._, 1918–19.
Barker, J. W., _V-P._, 1940–42.
Barnes, H. H., Jr., _M._, 1910–13; _V-P._, 1913–15.
*Barstow, W. S., _M._, 1900–03; _V-P._, 1903–05.
Barton, T. F., _V-P._, 1939–40; _D._, 1940–44.
†Bates, D. H., _V-P._, 1885–87.
*Baum, Frank G., _V-P._, 1906–08.
Beardsley, C. R., _D._, 1937–41.
Beaver, J. L., _V-P._, 1927–29.
Bedell, Frederick, _M._, 1914–17; _V-P._, 1917–18.
*Behrend, B. A., _M._, 1913–16; _V-P._, 1916–18.
*Bell, Alexander G., _V-P._, 1884–85; _P._, 1891–92.
*Bell, Louis, _M._, 1891–94.
Bennett, Edward, _V-P._, 1924–26.
*Berresford, A. W., _M._, 1909–12; _V-P._, 1912–14; _P._, 1920–21.
Berry, J. H., _V-P._, 1947–49.
Bettis, A. E., _V-P._, 1926–28; _D._, 1928–32.
Bickelhaupt, C. O., _V-P._, 1927–29.
Bingham, L. A., _V-P._, 1943–45.
†Black, R. G., _M._, 1910–13.
Blackwell, O. B., _V-P._, 1936–38.
Blaisdell, L. T., _V-P._, 1936–38.
Bolton, F. C., _V-P._, 1938–40.
Bonney, R. B., _V-P._, 1933–35.
Bower, G. W., _V-P._, 1947–49.
*Brackett, Cyrus F., _M._, 1886–89.
*Bradley, Chas. S., _M._, 1894–97; _V-P._, 1897–99, _M._, 1899–1902.
*Brooks, David M., _M._, 1885–88.
*Brooks, Morgan, _M._, 1907–10; _V-P._, 1910–12.
*Brush, Chas. F., _M._, 1884–87.
Bryant, J. M., _M._, 1924–28.
Buck, Harold W., _M._, 1907–10; _V-P._, 1910–12; _P._, 1916–17.
*Buckingham, Chas. L., _M._, 1885–88.
Buckley, O. E., _V-P._, 1946–48.
Bush, Vannevar, _D._, 1937–41.
Bussey, H. E., _V-P._, 1923–25.

Callahan, J. L., _V-P._, 1948–50.
Carle, N. A., _M._, 1916–19; _V-P._, 1919–20.
*Carlton, W. G., _M._, 1908–11; _V-P._, 1911–13.
Carpenter, C. B., _V-P._, 1944–46.
*Carpenter, H. V., _V-P._, 1930–32.
*Carty, John J., _M._, 1893–96, 1900–03, 1903–04, 1906–08; _V-P._, 1904–06, 1908–11; _P._, 1915–16.
*Chamberlain, J. C., _M._, 1890–93.
Charlesworth, H. P., _M._, 1923–27; _V-P._, 1930–32; _P._, 1932–33.
*Chesney, C. C., _M._, 1905–08; _V-P._, 1908–10; _P._, 1926–27.
Chesterman, F. J., _D._, 1926–30.
Christie, C. V., _V-P._, 1935–37.
Chubb, L. W., _D._, 1931–35.
Chubbuck, L. B., _V-P._, 1931–33.
†Church, W. Lee, _M._, 1887–88.
*Clarke, Chas. L., _M._, 1904–05.
Clifford, H. E., _M._, 1908–11.
Coldwell, O. B., _V-P._, 1921–22.
*Compton, Alfred G., _M._, 1891–94.
Cone, D. I., _V-P._, 1947–49.
Cooper, A. B., _V-P._, 1927–29; _D._, 1930–34.
Coover, M. S., _D._, 1940–44; _V-P._, 1944–46.
Copley, A. W., _V-P._, 1931–33.
*Craft, E. B., _M._, 1920–24.
Craft, F. M., _V-P._, 1933–35.
*Crocker, Francis B., _M._, 1888–90; _V-P._, 1890–92, 1894–96; _P._, 1897–98.
†Crosby, Oscar T., _V-P._, 1892–94.
*Cross, Chas. R., _V-P._, 1884–85.
*Cuttriss, Chas., _M._, 1888–91.

Danner, R. F., _V-P._, 1946–48.
Davis, Ernest W., _V-P._, 1946–48; _D._, 1949–53.
Dawes, Chester L., _V-P._, 1938–40.
*Delany, Patrick B., _M._, 1890–93; _V-P._, 1893–95.
Del Mar, Wm. A., _M._, 1917–21; _V-P._, 1921–22.
Dewars, A. G., _V-P._, 1942–44.
Dobson, W. P., _V-P._, 1925–27.
*Dolbear, A. E., _V-P._, 1885–87.
*Don Carlos, H. C., _D._, 1926–30.
*Downing, P. M., _V-P._, 1925–27.
*Duncan, Louis, _P._, 1895–97.
Dunn, Gano, _M._, 1897–1900; _V-P._, 1900–02; _M._, 1902–05; _V-P._, 1905–07; _P._, 1911–12.
DuVall, W. C., _V-P._, 1949–51.

†Eales, H. W., _V-P._, 1921–26.
*Eckert, W. H., _M._, 1884–85.
*Edgar, Chas. L., _M._, 1905–08.
*Edison, Thos. A., _V-P._, 1884–85.
*Eglin, W. C. L., _M._, 1903–06; _V-P._, 1907–09.
Eldredge, M., _V-P._, 1935–37; _D._. 1939–43.
Ellestad, I. M., _V-P._, 1947–49.
*Emmet, W. L. R., _V-P._, 1900–02.
Evans, Herbert S., _V-P._, 1929–31.
Evenson, F. F., _V-P._, 1945–47.
Everitt, W. L., _D._, 1947–51.
Ewart, F. R., _V-P._, 1921–23.

*Faccioli, G., _M._, 1918–22; _V-P._, 1922–24.
†Fair, R. H., _V-P._, 1935–37.
Fairman, J. F., _V-P._, 1944–46; _D._, 1946–49; _P._, 1949–50.
Farmer, F. M., _D._, 1934–38; _V-P._, 1938–39; _P._, 1939–40.
*Ferguson, Louis A., _M._, 1904–07; _V-P._, 1907–08; _P._, 1908–09.
Ferguson, O. J., _V-P._, 1927–29.
Fick, C. W., _D._, 1948–52.
*Field, Stephen D., _M._, 1884–86.
Fields, E. S., _V-P._, 1945–47.
*Finney, John H., _M._, 1914–17; _V-P._, 1917–18.
*Fisken, John B., _M._, 1916–19; _V-P._, 1919–20.
Flanigen, J. M., _D._, 1945–49.
Fleager, C. E., _V-P._, 1929–31.
*Foster, Horatio A., _M._, 1890–93.
*Fowle, F. F., _M._, 1919–23.
*Fowler, A. C., _M._, 1887.
Fowler, M. M., _D._, 1925–29.
Frampton, A. H., _V-P._, 1949–51.
*Freeman, W. E., _V-P._, 1931–33.
Funk, N. E., _D._, 1934–38; _P._, 1943–44.

Gamble, Lester R., _V-P._, 1938–40; _D._, 1941–45.
Gaylord, J. M., _V-P._, 1943–45.
Gear, H. B., _D._, 1934–38.
Geiger, D. G., _V-P._, 1947–49.
*Geyer, Wm. E., _M._, 1888–92.
*Gherardi, Bancroft, _M._, 1905–08, 1914–17; _V-P._, 1908–10; _P._, 1927–28.
Gilson, Walter J., _V-P._, 1943–45.
Goldsborough, W. E., _M._, 1901–04; _V-P._, 1904–06.
*Gray, Elisha, _M._, 1884–86.
*Green, Norvin, _P._, 1884–86; _V-P._, 1886–88.
*Greene, S. Dana, _M._, 1899.

Hall, Walter A., _M._, 1917–21; _V-P._, 1921–22.
*Hamblet, Jas., _M._, 1891–94; _V-P._, 1894–96.
*Hamilton, George A., _V-P._, 1884–86; _Natl. Treas._, 1895–1930.
Hamilton, J. L., _V-P._, 1940–42.
*Hammer, William J., _V-P._, 1891–93; _M._, 1893–96.
Hanker, F. C., _D._, 1927–31.
Hansen, K. L., _V-P._, 1940–42; _D._, 1942–46.
*Harding, C. F., _V-P._, 1936–38.
*Harisberger, John, _V-P._, 1924–26.
Harrison, W. H., _V-P._, 1935–37; _P._, 1937–38.
*Haskins, Chas. H., _V-P._, 1884–86.
*Hasson, W. F. C., _V-P._, 1895–97; _M._, 1897–1900.
*Hazard, Rowland R., _Treas._, 1884–86; _V-P._, 1887–89.
*Hellings, M. L., _M._, 1884–85.
*Hellmund, R. E., _D._, 1939–42.
*Henderson, S. E. M., _V-P._, 1923–25.
Henline, H. H., _Act. Secy._, 1932; _Secy._, 1933–.
Henry, R. T., _V-P._, 1944–46; _D._, 1946–50.
*Hering, Carl, _V-P._, 1891–93, 1895–98; _P._, 1900–01.
*Herzog, F. Benedict, _M._, 1887–92.
†Hewitt, Chas., _M._, 1893–96.
†Hibbard, Angus S., _M._, 1892–95; _V-P._, 1895–97.
Hibshman, N. S., _V-P._, 1941–43.
*Higson, C. R., _V-P._, 1932–34.
Hinson, N. B., _V-P._, 1935–37; _D_, 1949–53.
Hitchcock, H. W., _V-P._, 1939–41.
*Hobart, H. M., _M._, 1922–26; _V-P._, 1926–28.
Hooven, M. D., _D._, 1948–52.
Hopkins, Ralph A., _V-P._, 1949–51.
Housley, J. E., _V-P._, 1941–43; _P._, 1946–47.
*Houston, Edwin J., _M._, 1884–87; _P._, 1893–95.
*Howell, John W., _M._, 1888–90.
Hull, A. H., _V-P._, 1933–35.
Hull, B. D., _V-P._, 1928–30; _D._, 1931–35; _P._, 1947–48.
*Humphrey, H. H., _V-P._, 1906–08.
Hunting, F. S., _M._, 1911–14; _V-P._, 1914–16.
*Hutchinson, Cary T., _M._, 1895–98; _V-P._, 1898–1900.
*Hutchinson, F. L., _Natl. Secy._, 1912–32.

*Imlay, L. E., _M._, 1919–23.
Jackson, Dugald C., _V-P._, 1897–99; _P._, 1910–11.
*Jackson, W. B., _M._, 1912–15; _V-P._, 1918–19.
†James, Wm. F., _V-P._, 1923–25.
Jamieson, B. G., _V-P._, 1926–28.
Jewett, F. B., _M._, 1915–18; _V-P._, 1918–19; _P._, 1922–23.
Johnson, F. Ellis, _D._, 1936–39.
*Johnson, J. Allen, _D._, 1928–32; _V-P._, 1932–34; _P._, 1934–35.
Jollyman, J. P., _V-P._, 1937–39.
Jones, A. L., _V-P._, 1941–43.
Jones, C. R., _D._, 1935–39; _V-P._, 1942–44.
*Jones, Francis W., _M._, 1884–85; _V-P._, 1885–87.
*Jorgensen, L. R., _V-P._, 1919–20.
Juhnke, P. B., _D._, 1933–37.
*Junkersfeld, P., _M._, 1913–16; _V-P._, 1916–18.

Kearns, J. E., _D._, 1929–33.
*Keith, Nathaniel S., _Secy._, 1884–85.
*Kelsch, Raymond S., _V-P._, 1918–19.
*Kennelly, Arthur E., _V-P._, 1892–94; _M._, 1894–97; _V-P._, 1897–98; _P._, 1898–1900.
Kidder, H. A., _M._, 1925–28; _V-P._, 1928–30.
*Knight, G. L., _M._, 1922–26; _V-P._, 1926–28.
Knowlton, A. E., _D._, 1930–34.
Kositzky, G. A., _D._, 1932–36.
Kouwenhoven, W. B., _V-P._, 1931–33; _D._, 1935–39.

Lacy, T. N., _V-P._, 1930–32.
Laffoon, C. M., _D._, 1942–47.
*Lamme, Benj. G., _M._, 1907–10.
Lane, F. H., _D._, 1937–41.
Lardner, H. A., _M._, 1913–16.
Lawton, F. L., _V-P._, 1945–47.
LeClair, T. G., _D._, 1941–45; _V-P._, 1946–48.
Lee, Everett S., _D._, 1933–37; _V-P._, 1940–42; _P._, 1948–49.
*Lee, William S., _M._, 1911–14; _D._, 1929–30; _P._, 1930–31.
*Leonard, H. Ward, _M._, 1890–93; _V-P._, 1893–95.
*Lieb, John W., _M._, 1896–99; _V-P._, 1899–1901; _M._, 1901–03; _V-P._, 1903–04; _P._, 1904–05.
*Lighthipe, J. A., _V-P._, 1913–15.
Lincoln, J. F., _M._, 1920–24.
*Lincoln, Paul M., _M._, 1906–09; _V-P._, 1909–11; _P._, 1914–15.
Liversidge, H. P., _D._, 1927–31.
*Lloyd, Herbert _M._, 1898–1901.
*Lockwood, Thos. D., _V-P._, 1886–87; _M._, 1888–90; _V-P._, 1891–93.
Lovell, A. H., _D._, 1932–36; _V-P._ ,1938–40.
*Lozier, R. T., _M._, 1902–04.
†Lunn, Ernest, _M._, 1922–26.

MacCutcheon, A. M., _D._, 1928–32; _P._, 1936–37.
*Macdonald, J. E., _V-P._, 1923–25.
*Macfarlane, Alexander, _M._, 1897–1900.
Maclachlan, Wills, _V-P._, 1919–20.
*Magnusson, C. E., _V-P._, 1920–21.
Mahood, E. T., _V-P._, 1942–44.
*Mailloux, C. O., _M._, 1886–89; _V-P._, 1898–99; _M._, 1899–1902; _V-P._, 1902–04; _M._, 1905–07; _P._, 1913–14.
*Mapes, L. R., _D._, 1938–42.
*Martin, T. Commerford, _Act. Secy._, 1884–85; _M._, 1885–87; _P._, 1887–88; _V-P._, 1888–90.
Martindale, E. H., _M._, 1917–20; _V-P._, 1920–21.
*Maver, Wm., Jr., _M._, 1888–91.
Maxwell, Fred R., Jr., _V-P._, 1939–41; _D._, 1941–45.
*Maynard, Geo. C., _V-P._, 1886–88.
*McAllister, A. S., _M._, 1914–17; _V-P._, 1917–18.
McClellan, L. N., _V-P._, 1937–39.
McClellan, William, _M._, 1912–15, _V-P._, 1915–17, _P._, 1921–22.
†McConahey, W. M., _M._, 1923–27.
McDowell, C. S., _V-P._, 1920–21.
McEachron, K. B., _D._, 1936–40; _V-P._, 1942–44.
McHenry, M. J., _V-P._, 1937–39; _D._, 1944–48.
McKay, Richard, _V-P._, 1948–50.
McMillan, F. O., _V-P._, 1934–36; _D._, 1948–52.
*Merriam, E. B., _M._, 1924–28; _V-P._, 1928–30.
Mershon, Ralph D., _M._, 1900–03; _V-P._, 1903–05; _P._, 1912–13.
*Meyer, E. B., _D._, 1927–31; _V-P._, 1932–34; _P._, 1935–36.
Meyer, F. J., _V-P._, 1934–36; _D._, 1939–43.
*Michaelis, O. E., _M._, 1886–89; _V-P._, 1889–90.
Mier, C. W., _D._, 1943–47.
Mills, G. A., _V-P._, Aug.–Oct. 1932.
Mitchell, W. E., _V-P._, 1925–27.
Montieth, A. C., _D._, 1947–51.
*Morehouse, L. F., _M._, 1919–23; _V-P._, 1924 26.
*Morrow, L. W. W., _D._, 1933–37.
Mortensen, S. H., _D._, 1943–47.
Morton, W. B., _D._, 1941–46.
Moultrop, I. E., _D._, 1926–30; _V-P._, 1930–32.
*Mullin, E. H., _M._, 1902–04.
*Murray, Wm. S., _M._, 1908–09, 1909–12; _V-P._, 1912–14.

Newbury, F. D., _M._, 1918–22.
*Nichols, E. L., _V-P._, 1889–91.
*Norris, H. H., _M._, 1909–12.
North, J. R., _D._, 1945–49.
Northmore, E. R., _V-P._, 1927–29.

Osborne, H. S., _D._, 1938–42; _P._, 1942–43.
*Osgood, Farley, _M._, 1911–14; _V-P._, 1914–16; _P._, 1924–25.
†Owens, R. B., _V-P._, 1898–1900.

Parker, John Castlereagh, _V-P._, 1921–22; _P._, 1938–39.
†Patton, P. H., _V-P._, 1931–33.
*Peek, F. W., Jr., _D._, 1930–33.
Pender, Harold, _M._, 1915–18; _V-P._, 1918–19.
*Perrine, Frederick A. C., _M._, 1898–1900.
*Phelps, Geo. M., Jr., _M._, 1885–87; _Treas._, 1886–95.
*Pickernell, F. A., _M._, 1896–99.
Pierce, A. G., _M._, 1921–25; _V-P._, 1925–27.
Pingree, G. N., _V-P._, 1948–50.
Plumb, H. T., _V-P._, 1922–24.
*Pope, Franklin L., _V-P._, 1884–86; _P._, 1886–87; _V-P._, 1887–89.
*Pope, Ralph W., _Secy._, 1885–1911; _Hon. Secy._, 1911–29.
Post, G. G., _V-P._, 1934–36.
Powel, C. A., _D._, 1936–40; _V-P._, 1942–43; _P._, 1944–45.
†Pratt, H. A., _M._, 1921–25.
*Prescott, Geo. B., _M._, 1884.
*Prescott, G. B., Jr., _M._, 1888–91.
†Price, C. A., _V-P._, 1941–43.
Prince, D. C., _D._, 1938–41; _P._, 1941–42.
*Puffer, Wm. L., _M._, 1896–99.
*Pupin, Michael I., _M._, 1892–95; _V-P._, 1895–97, 1901–03; _P._, 1925–26.

Quarles, D. A., _D._, 1944–48.
Quinan, G. E., _V-P._, 1928–30.

*Reber, Samuel, _M._, 1901–04; _V-P._, 1904–06.
*Rice, Calvin W., _M._, 1900–03; _V-P._, 1903–05.
*Rice, E. W., Jr., _P._, 1917–18.
Ricker, Claire W., _V-P._, 1943–45.
†Robbins, Chas., _M._, 1916–20; _V-P._, 1920–21.
Robertson, Elgin B., _D._, 1947–51.
Robertson, L. M., _V-P._, 1945–47.
*Robinson, L. T., _M._, 1913–16; _V-P._, 1916–18, 1920–21.
*Rodman, W. S., _V-P._, 1929–31.
Rogers, C. E., _V-P._, 1936–38.
*Ruffner, Chas. S., _M._, 1916–20; _V-P._, 1920–21.
*Rugg, W. S., _M._, 1910–13.
*Rushmore, D. B., _M._, 1908–11; _V-P._, 1911–13.
*Ryan, Harris J., _M._, 1893–96; _V-P._, 1896–98; _P._, 1923–24.
*Ryan, W. T., _V-P._, 1928–30.

*Sands, Herbert S., _V-P._, 1923–27.
*Sargent, W. D., _M._, 1885–86.
Schilling, E. W., _V-P._, 1942–44.
*Schoen, A. M., _M._, 1906–09; _V-P._, 1919–20.
Scholz, H. J., _D._, 1949–53.
Schoolfield, H. H., _V-P._, 1926–28.
*Schuchardt, R. F., _V-P._, 1922–24; _P._, 1928–29.
*Scott, Chas. F., _M._, 1895–98; _V-P._, 1899–1901; _P._, 1902–03.
*Scribner, Chas. E., _M._, 1910–13; _V-P._, 1913–15.
Seeley, W. J., _V-P._, 1949–51.
Sever, Geo. F., _M._, 1898–1901; _V-P._, 1901–03; _M._, 1903–06.
*Shaad, G. C., _V-P._, 1930–32; _D._, 1935–36.
*Shelbourne, Sidney, F., _M._, 1886–87.
*Sheldon, Samuel, _M._, 1898–1901; _V-P._, 1901–03; _M._, 1903–06; _P._, 1906–07.
Sibley, R., _V-P._, 1921–23.
Siegfried, Victor, _V-P._, 1948–50.
Sinclair, C. T., _V-P._, 1939–41.
Sisson, C. E., _V-P._, 1929–31.
Skinner, C. E., _M._, 1915–19; _V-P._, 1919–20; _P._, 1931–32.
Slichter, Walter I., _M._, 1918–22; _V-P._, 1922–24; _Treas._, 1930–.
*Smith, H. B., _M._, 1920–24; _V-P._, 1924-26; _P._, 1929–30.
Smith, Walter Charles, _V-P._, 1941–43; _D._, 1945–49.
Smith, W. R., _D._, 1942–46.
*Smith, W. W., _M._, 1884–85.
Sorensen, R. W., _V-P._, 1933–35; _D._, 1936–40; _P._, 1940–41.
*Spencer, Paul, _M._, 1906–09; _V-P._, 1909–11.
*Sprague, Frank J., _V-P._, 1890–92; _P._, 1892–93.
*Springer, F. W., _V-P._, 1921–23.
*Sprong, S. D., _M._, 1909–12; _V-P._, 1912–14.
*Stanley, Wm., _V-P._, 1898–1900.
Stein, I. Melville, _V-P._, 1937–39.
*Steinmetz, Chas. P., _M._, 1892–95; _V-P._, 1896–98; _M._, 1898–1901; _P._, 1901–02.
*Stephens, C. E., _D._, 1928–33.
Stevens, A. C., _D._, 1932–36; _V-P._, 1936–38.
*Stevens, J. Franklin, _M._, 1912–15; _V-P._, 1915–17.
*Stillwell, Lewis B., _M._, 1896–99; _V-P._, 1899–1901; _P._, 1909–10.
†Stine, Wilbur M., _V-P._, 1896–98.
Stokes, Stanley, _V-P._, 1932–34.
*Stone, Chas. W., _M._, 1908–11; _V-P._, 1911–13.
Stone, E. C., _D._, 1925–29; _V-P._, 1929–31.
*Storer, Norman W., _M._, 1911–14; _V-P._, 1914–16, 1921–23.
*Stott, Henry G., _M._, 1904–07; _P._, 1907–08.
Sykes, Wilfred, _M._, 1917–21.

Tapscott, R. H., _D._, 1930–34; _V-P._, 1934–36.
Taylor, A. LeRoy, _V-P._, 1940–42.
Taylor, John B., _M._, 1915–18; _V-P._, 1918–19.
Terrell, C. F., _V-P._, 1946–48.
*Terry, Chas A., _M._, 1902–05; _V-P._, 1905–07.
Terry, I. A., _V-P._, 1948–50.
*Tesla, Nikola, _V-P._, 1892–94.
*Thomas, B. F., _M._, 1885–87.
Thomas, Percy H., _M._, 1907–10; _V-P._, 1910–12.
†Thompson, Edward P., _M._, 1887–88.
*Thomson, Elihu, _V-P._, 1887–89; _P._, 1889-90.
Thomson, J. M., _V-P._, 1939–41.
Timbie, W. H., _V-P._, 1934–36.
*Townley, Calvert, _M._, 1905–08; _V-P._, 1908–10; _P._, 1919–20.
*Townsend, Henry C., _M._, 1899–1902.
*Trowbridge, Wm. P., _M._, 1884–86.
Turner, A. L., _V-P._, 1939–41.

*Upton, Francis R., _V-P._, 1889–90; _M._ 1890–92.

*Vail, Theo. N., _M._, 1884–86.
*Vanderpoel, W. K., _M._, 1923–27.
*Van Hoevenbergh, H., _M._, 1888–90.
Veinott, C. G., _V-P._, 1949–51.
*Vonsize, W. B., _M._, 1894–97.

*Wallace, Wm., _V-P._, 1893–95.
Warner, R. G., _D._, 1940–44.
Warner, R. W., _V-P._, 1944–46.
*Weaver, W. D., _M._, 1894–97; 1899–1902.
†Webb, Herbert L., _M._, 1891–94; 1897–1900.
*Weston, Edward, _M._, 1884–87; _P._, 1888–89; _V-P._, 1889–91.
*Wetzler, Jos., _M._, 1888–90; _V-P._, 1890–92.
*Wheeler, Schuyler S., _M._, 1887–90; _V-P._, 1890–91; 1902–04; _M._, 1904–05; _P._, 1905–06.
*White, Jas. G., _M._, 1904–07; _V-P._, 1907–09.
Whitehead, John B., _M._, 1924–28; _P._, 1933–34.
*Wickenden, W. E., _V-P._, 1943–45; _P._, 1945–46.
*Williamson, R. B., _M._, 1921–25.
*Wilson, A. M., _V-P._, 1933–35.
*Wirt, Chas., _M._, 1892–95.
*Wolcott, Townsend, _M._, 1902–05; _V-P._, 1905–07.
Wolf, H. B., _V-P._, 1945–47.
*Wood, Edwin D., _V-P._, 1937–39.
*Woodrow, H. R., _D._, 1931-35.

Yerkes, E. P., _D._, 1946–50.

Abbreviations: _P._, President; _V-P._, Vice-President; _M._, Manager; _D._, Director; _Natl. Secy._, National Secretary; _Secy._, Secretary; _Hon. Secy._, Honorary Secretary; _Natl. Treas._, National Treasurer; _Treas._, Treasurer.

*Deceased . †Non-members at present.

Honorary Members, 1884-1949

*Sir William Preece	1884
*Norvin Green	1889
*Moses G. Farmer	1890
*Lord Kelvin	1892
*Cyrus Field	1892
*E. W. von Siemens	1892
*Andre E. Blondel	1912
*C. E. L. Brown	1912
*Emil A. Budde	1912
*S. Z. de Ferranti	1912
*Antonio Pacinotti	1912
*S. P. Thompson	1914
*Guglielmo Marconi	1917
*Oliver Heaviside	1918
*Ferdinand Foch	1921
*Thomas A. Edison	1928
*John J. Carty	1928
*M. I. Pupin	1928
*Ambrose Swasey	1928
*Elihu Thomson	1928
Herbert Hoover	1929
* CharlesF. Scott	1929
*Charles F. Brush	1929
Motoji Shibusawa	1929
*Frank J. Sprague	1932
*W. L. R. Emmet	1933
*G. A. Hamilton	1933
*Arthur E. Kennelly	1933
R. A. Millikan	1933
*E. W. Rice, Jr.	1933
*Edward Weston	1933
*Bion J. Arnold	1937
*Alex Dow	1937
*Cummings C. Chesney	1938
Andrew G. L. McNaughton	1942
Dugald C. Jackson	1944
Charles E. Skinner	1945
Gano Dunn	1945
Frank B. Jewett	1945
Vannevar Bush	1949

* Deceased

Headquarters Staff

33 West 39th Street, New York 18

Secretary	H. H. Henline
Office Manager and Business Manager of Publications	F. A. Norris
Editor	C. S. Rich
Associate Editor	E. E. Grazda
Secretary, Technical Program Committee	Edward C. Day
Secretary, Board of Examiners and Standards Committee	J. J. Anderson, Jr.
Technical Committee Activities	R. S. Gardner
Advertising Manager	J. S. Lopes

General Counsel

Simon Presant........55 Liberty St., New York 5

Sections

Name	District	When Organized	Membership Aug. 1, 1949	Chairman	Secretary	Secretary's Address
Akron	2	Aug. 12, ’20	118	H. P. Davis	R. F. Miller	2284 14th St., Cuyahoga Falls, Ohio
Alabama	4	May 22, ’29	199	J. W. Davis	Gayle Riley	Alabama Power Co., 600 No. 18th St., Birmingham 2, Ala.
Arizona	8	Mar. 22, ’41	131	W. B. Bustard	I. G. Jenkins, Jr.	Central Arizona Light & Power Co., 2215 No. 25th St., Phoenix Ariz.
Arkansas	7	Apr. 23, ’47	79	W. A. Lewis, Jr.	R. B. Newman	Reynolds Metals Co., Jones Mills Plant, Jones Mills, Ark.
Arrowhead	5	Apr. 23, ’47	43	P. A. Beckjord	Roy B. Wiprud	137 Greenwood Lane, Duluth 3, Minn.
Beaumont	7	June 27, ’45	81	R. W. Parker	T. G. Smith	490 Bowie St., Beaumont, Tex.
Boston	1	Feb. 13, ’03	833	J. J. Loustaunau	Leslie J. Weed	Boston Edison Co., 182 Tremont St., Boston 11, Mass.
Canton	2	Nov. 5, ’47	79	C. L. Lucal	R. V. Kelch	4520 13th St., N. W., Canton 7, Ohio
Central Indiana	5	Jan. 12, ’12	232	G. M. Grabbe	J. W. Sears	Indiana Bell Tel. Co., 240 No. Meridian St., Indianapolis 9, Ind.
Chicago	5	1893	1587	H. E. Nason	E. L. Michelson	Commonwealth Edison Co., 72 West Adams St., Chicago, Ill.
Cincinnati	2	June 30, ’20	236	F. E. Wiatt	D. T. Michael	Cincinnati Gas & Elec. Co., 4th & Main Sts., Cincinnati 2, Ohio
Cleveland	2	Sept. 27, ’07	708	R. L. Oetting	J. A. Cortelli	Clark Controller Co., 1146 East 152nd St., Cleveland 10, Ohio
Columbus	2	Mar. 17, ’22	137	N. L. Greene	C. E. Warren	Ohio State Univ., Columbus 10, Ohio
Connecticut	1	Apr. 16, ’21	490	J. B. Powell	F. W. Buck	United Illuminating Co., 80 Temple St., New Haven 6, Conn.
Dayton	2	June 9, ’43	341	E. Herzog	J. G. Tolentino	2 Palermo Place, Dayton 4, Ohio
Denver	6	May 18, ’15	367	F. A. Eastom	E. R. Jones	Mountain States Tel. & Tel. Co., Box 960, Denver 1, Colo.
East Tennessee	4	Sept. 2, ’36	415	T. D. Talmage	J. D. Sharp, Jr.	3004 Pope Drive, Chattanooga, Tenn.
Erie	2	Jan. 11, ’18	130	M. J. Kolhoff	J. F. Davis	General Elec. Co., 2901 East Lake Road, Erie, Pa.
Florida	4	Jan. 28, ’31	137	Jess D. Thomas	J. D. Griffin	P. O. Box 26, Station G, Jacksonville, Fla.
Fort Wayne	5	Aug. 14, ’08	125	J. F. Eitman	J. H. Capps	General Elec. Co., 1635 Bway, Fort Wayne 2, Ind.
Georgia	4	Jan. 14, ’04	220	E. S. Lammers	H. P. Peters	Georgia Inst. of Tech., Engg. Experiment Sta. Atlanta, 3 Ga.
Houston	7	Aug. 7, ’28	271	W. O. Ray	W. E. Silvus	P. O. Box 2537, Houston, Tex.
Illinois Valley	5	June 30, ’45	151	E. W. Stone	E. Krefting	317 Persimmon St., Peoria 6, Ill.
Iowa	5	June 29, ’29	187	T. W. Schroeder	F. E. Keith	Keith Elec. Construction Co., 1213 Walnut St., Des Moines 9, Iowa
Ithaca	1	Oct. 15, ’02	180	H. L. Livingood	S. W. Zimmerman	102 Valley Road, Ithaca, N. Y.
Kansas City	7	Apr. 14, ’16	272	R. M. Kerchner	A. J. Nicholson	Kansas City Power & Light Co., P.O. Box 679, Kansas City 10, Mo.
Lehigh Valley	2	Apr. 16, ’21	324	C. H. Sprague	A. L. Price	Pennsylvania Power & Light Co., Hazleton, Pa.
Los Angeles	8	May 19, ’08	1289	E. L. Bettannier	E. K. Sadler	Kelman Elec. & Mfg. Co., 1650 Naud St., Los Angeles 12, Calif.
Louisville	4	Oct. 15, ’26	115	R. D. Spalding	H. T. Smith	2816 Virginia Ave., Louisville 11, Ky.
Lynn	1	Aug. 22, ’11	266	R. S. Schlotterbeck	A. M. Bjontegard	General Elec. Co., 920 Western Ave., West Lynn, Mass.
Madison	5	Jan. 8, ’09	75	Leonard Hesse	Rubin A. Imm	102 Electrical Lab., Univ. of Wis., Madison, Wis.
Mansfield	2	Mar. 6, ’39	74	E. C. Ryan	F. L. Hanson	The Ideal Elec. & Mfg. Co., Mansfield, Ohio
Maryland	2	Dec. 16, ’04	626	F. H. Rogers	J. W. Magee	Westinghouse Elec. Corp., Std. Oil Bldg., Baltimore 3, Md.
Memphis	4	May 22, ’30	124	W. B. Thompson	J. R. Owens	3563 Mimosa St., Memphis, Tenn.
Mexico	7	June 29, ’22	216	E. D. Luque	F. Aubert	Apartado Postal 1399, Mexico,Federal District, Mexico
Miami	4	Feb. 3, ’49	97	C. H. Summers	W. T. Edwards, Jr.	Southern Bell Tel. & Tel. Co., 1410—20th St., Miami Beach, Fla.
Michigan	5	Jan. 13, ’11	805	E. F. Dissmeyer	O. E. Bowlus	18628 Lauder, Detroit 19, Mich.
Milwaukee	5	Feb. 11, ’10	688	F. J. Van Zeeland	C. P. Feldhausen	Cutler-Hammer, Inc., 315 No. 12th St., Milwaukee 2, Wis.
Minnesota	5	Apr. 7, ’02	279	Lincoln Rietow	R. E. Willey	Northwestern Bell Tel. Co., 224 So. 5th St., Minneapolis 2, Minn.
Montana	9	June 24, ’31	90	J. R. Walker	Marion O. Hansen	112—14th St., Great Falls, Mont.
Montreal	10	Apr. 16, ’43	296	N. L. Morgan	A. Malkin	Canadian Car & Foundry Co. Ltd., 621 Craig St. W., Montreal, Quebec 3, Canada
Nebraska	6	Jan. 21, ’25	108	F. C. Howard	C. N. Allen	Allis-Chalmers Mfg. Co., 9th & Farnam Sts., Omaha 8, Nebr.
New Mexico-West Texas	7	Mar. 7, ’40	148	Oscar Gutsch	G. R. Yelderman	General Elec. Co., 109 North Oregon, El Paso, Tex.
New Orleans	4	Dec. 8, ’33	246	E. I. Blanchard	H. E. Pritchard, Jr.	5836 General Haig St., New Orleans 19, La.
New York	3	Dec. 10, ’19	4894	D. W. Taylor	J. P. Neubauer	402 Fairway Road, Ridgewood, N. J.
Niagara Frontier	1	Feb. 10, ’25	266	F. C. Rushing	C. J. Brumbaugh	129 Hunt Ave., Hamburg, N. Y.
Niagara International	10	Aug. 5, ’48	101	W. G. A. Barr	E. A. Dillon	11 York St., St. Catharines, Ontario, Canada
North Carolina	4	Mar. 21, ’29	229	G. G. Mattison	B. V. Martin	Westinghouse Elec. Corp., 210 East Sixth St., Charlotte 1, N. C.
North Texas	7	May 18, ’28	385	P. G. Wallace	A. L. Jones, Jr.	General Elec. Co., 1801 North Lamar St., Dallas, Tex.
Oklahoma City	7	Feb. 16, ’22	180	E. W. Allen	J. C. Turner	Southwestern Bell Tel. Co., 1004 Telephone Bldg., Oklahoma City 2, Okla.
Ottawa	10	June 23, ’49	56	Fred Fleming	Gordon Cathcart	137 Clarendon Ave., Ottawa, Ontario, Canada
Panhandle Plains	7	June 12, ’47	131	George Dupree	G. R. Bunton	Box 631, Lubbock, Tex.
Philadelphia	2	Feb. 18, ’03	1344	W. F. Henn	H. H. Sheppard	Rumsey Elec. Co., 1007 Arch St., Philadelphia 7, Pa.
Pittsburgh	2	Oct. 13, ’02	1050	F. H. Schlough	L. N. Grier	Aluminum Co. of America, 801 Gulf Bldg., Pittsburgh, Pa.
Pittsfield	1	Mar. 25, ’04	283	T. E. Rodhouse	G. L. Vallin	General Elec. Co., Pittsfield, Mass.
Portland	9	May 18, ’09	438	R. B. Temple	Waldo Porter	Aluminum Co. of America, Vancouver, Wash.
Providence	1	Mar. 12, ’20	132	O. E. Sawyer	C. B. Leathers	General Elec. Co., 111 Westminster St., Providence, R. I.
Richland	9	Apr. 23, ’48	94	W. J. Dowis	R. B. Crow	206 Barth Ave., Richland, Wash.
Rochester	1	Oct. 9, ’14	214	J. H. Rogers	E. M. Morecock	817 Chili Ave., Rochester, N. Y.
Rock River Valley	5	Apr. 23, ’47	84	S. A. Coxhead	W. C. Busch	1421 North Church St., Rockford, Ill.
St. Louis	7	Jan. 14, ’03	521	R. C. Horn	J. C. Lebens, Jr.	7322 Melrose Ave., University City, Mo.
San Diego	8	Jan. 18, ’39	157	J. F. Sinnott	O. L. Doolittle	4738 Works Place, San Diego 4, Calif.
San Francisco	8	Dec. 23, ’04	1259	T. C. McFarland	Remi Bollaert	Westinghouse Elec. Corp., 410 Bush St., San Francisco 8, Calif.
Schenectady	1	Jan. 26, ’03	928	J. C. Page	L. T. Rader	General Elec. Co., Bldg. 69, Schenectady 5, N. Y.
Seattle	9	Jan. 19, ’04	422	Ray Rader	H. R. Loew	General Elec. Co., 710 Second Ave., Seattle 11, Wash.
Sharon	2	Dec. 11, ’25	178	G. R. Monroe	A. J. Maslin	Westinghouse Electric Corp., Sharon, Pa.
Shreveport	4	June 12, ’47	74	L. T. Williams	C. M. L. Pons	3536 Del Rio, Shreveport 65, La.
South Bend	5	Feb. 26, ’41	102	R. A. Cliff	Rayhue Neighbours	526 So. Twyckenham Drive., South Bend 15, Ind.
South Carolina	4	Mar. 2, ’40	122	E. P. Miller	W. E. Dent, Jr.	The Springs Cotton Mills, Lancaster, S. C.
South Texas	7	May 23, ’30	163	L. L. Antes	R. R. Krezdorn	153 Engg. Bldg., University of Texas, Austin, Tex.
Spokane	9	Feb. 14, ’13	128	T. W. MacLean	R. M. Gilbert	Westinghouse Elec. Corp., 1023 West Riverside Ave., Spokane 8, Wash.
Springfield	1	June 29, ’22	88	W. E. Maxwell	W. L. Vest, Jr.	54 Churchill Road, West Springfield, Mass.
Syracuse	1	Aug. 12, ’20	237	J. D. Hershey	H. H. Graley	Central New York Power Corp., 300 Erie Blvd., W. Syracuse, N. Y.
Toledo	2	June 3, ’07	117	F. A. Furfari	W. M. Campbell	2145 Central Grove Ave., Toledo, Ohio
Toronto	10	Sept. 30, ’03	557	J. M. Somerville	W. R. Harmer	Hydro-Elec. Power Co. of Ont., 49 Bathurst St., Toronto, Ontario, Canada
Tulsa	7	Oct. 1, ’37	139	Dick Ray	W. H. Plumer	Southwestern Bell Tel. Co., 424 So. Detroit, Tulsa, Okla.
Urbana	5	Nov. 25, ’02	99	H. N. Hayward	P. R. Egbert	University of Illinois, Urbana, Ill.
Utah	9	Mar. 9, 17	115	F. O. Wald	J. A. McDonald	General Elec. Co., P. O. Box 779, Salt Lake City 1, Utah
Vancouver	10	Aug. 22, ’11	182	W. J. Lind	F. O. Wollaston	B. C. Elec. Ry. Co., Ltd., 425 Carrall St., Vancouver, British Columbia, Canada
Virginia	4	May 19, ’22	168	L. Saunders	P. R. Spracher	P. O. Box 1194, Richmond 19, Va.
Washington	2	Apr. 9, ’03	832	Dixon Lewis	L. V. Berkner	7213 Bradley Blvd., Bethesda 14, Md.
West Virginia	2	Apr. 9, ’40	120	E. B. Ellerbe	J. J. Fitzgibbon	306 McCorkle Ave., Charleston 28, W. Va.
Western Virginia	4	June 23, ’49	86	Ira H. McMann	E. L. Munday, Jr.	P. O. Box 2021, Appalachian Elec. Power Co., Roanoke, Va.
Wichita	7	Sept. 16, ’37	82	R. F. Dice	J. E. McNicol	Kansas Gas & Elec. Co., 201 No. Market, Wichita 2, Kans.
Worcester	1	Feb. 18, ’20	88	D. R. Percival	W. G. Coleman	General Elec. Co.. 507 Main St., Worcester 8, Mass.
Total Sections	85		30,460			

Subsections

Name	Chairman	Secretary	Secretary's Address
Albuquerque (New Mexico-West Texas Section)	W. O. McCord, Jr.	A. D. Taylor	1722 East Gold Ave., Albuquerque, N. Mex.
Baton Rouge (New Orleans Section)	F. R. Adams	E. F. Tims	3155 Lakeshore Drive, Baton Rouge 15, La.
Billings (Montana Section)	Frank M. Holly	Curtis B. Brown	751 Lewis Ave., Billings, Mont.
Binghamton Area (Ithaca Section)	K. S. Koon	J. P. Peterson	Westinghouse Elec. Corp., 86 Court St., Binghamton 62, N. Y.
Boulder City (Los Angeles Section)	W. H. Taylor	F. A. Latham	Los Angeles Water & Power Dept., 600 Nevada Highway, Boulder City, Nev.
Casper, Wyo. (Denver Section)			
Central Texas (North Texas)			
Centre County (Pittsburgh Section)	R. J. E. Hemman	R. M. Smith	Ord. Res. Lab., Penna. State Col., State College, Pa.
Charleston (South Carolina Section)			
Charlotte (North Carolina Section)	M. F. Leftwich	I. G. Wallace, Jr.	205 Wales Ave., Charlotte, N. C.
Columbia (South Carolina Section)	H. K. Walker	J. F. Rader, Jr.	1307½ Gladden St., Columbia, S. C.
Corpus Christi (South Texas Section)	A. B. Weaver	J. H. Watts, Jr.	Westinghouse Elec. Corp., 508 Medical Professional Bldg., Corpus Christi, Tex.
Fort Worth (North Texas Section)	J. L. Candry	Karl J. Ratliff	1101 Electric Bldg., Fort Worth 2, Tex.
Fox River Valley (Milwaukee Section)	Herbert F. Weckwerth	Donald Christison	209 Frederick St., Menasha, Wis.
Freeport (Houston Section)	H. K. Brown	R. B. Adams	P. O. Box 413, Lake Jackson, Tex.
Fresno (San Francisco Section)	C. L. Smith	William Green	U. S. Bureau of Reclamation, T. W. Patterson Bldg., Fresno 1, Calif.
Great Falls (Montana Section)			
Hamilton (Toronto Section)			
Hampton Roads (Virginia Section)	C. E. Hastings	S. H. Fletcher	Va. Electric & Power Co., Newport News, Va.
Hudson Valley (New York Section)	W. E. Smalley	J. E. Ruscher	49 Carroll St., Poughkeepsie, N. Y.
Jackson (Miss.) (New Orleans Section)	J. B. Fountain	A. N. Saxon	Graybar Electric Co., 758 Ricks St., Jackson, Miss.
Jacksonville (Florida Section)			
Johnstown (Pittsburgh Section)	W. R. Wood	R. S. Baum	Univ. of Pittsburgh, 325 Cypress Ave., Johnstown, Pa.
Lake Charles (New Orleans Section)	G. B. S. Ricketts	J. J. Alleman	29 Hibiscus Road, Maplewood, La.
Lancaster–York (Maryland Section)	W. H. Freund	H. H. Wittenberg	RCA Victor Div., Radio Corp. of America, Lancaster, Pa.
Lima (Dayton Section)	B. O. Austin	O. M. Swain	Westinghouse Elec. Corp., Engg. Dept., Lima, Ohio
Mid-State (North Carolina Section)	F. R. Jackson, Jr.	L. H. Allcorn, Jr.	2425 Elizabeth Ave., Winston-Salem 7, N. C.
Muscle Shoals (East Tennessee Section)	Lyle W. Jenkins	Arthur R. Rifkin	Apt. 4, Spring Creek Apts., Village 1, Sheffield, Ala.
Nashville (Memphis Section)	Walter Criley	E. R. Boruch	1229 Stahlman Bldg., 234—3rd Ave., North, Nashville 3, Tenn.
New Hampshire (Boston Section)			
New Jersey (New York Section)	J. S. Bell	Max Enderlin	Federal Telephone & Radio Corp., 900 Passaic Ave., East Newark, N. J.
Northern New York (Syracuse Section)	J. B. Pitman	L. F. Pries	New York State Agric. & Tech. Inst., 8 College St., Canton, N. Y.
Northwest Arkansas (Arkansas Section)	W. B. Stelzner	R. E. Anderson	Southwestern Gas & Elec. Co., De Queen, Ark.
Oak Ridge (East Tennessee Section)	M. C Becker	M. P. Falls	105 Vermont Ave., Oak Ridge, Tenn.
Quad-Cities (Iowa Section)	E. H. Breckenfelder	M. P. Roller	Iowa-Illinois Gas & Elec. Co., United Light Bldg., Davenport, Iowa
Red River Valley (Minnesota Section)	M. R. Hughes	H. K. Ugelstad	Northern States Power Co., 612 N. P. Ave., Fargo, N. Dak.
Richmond (Virginia Section)	C. L. Gilkeson	L. D. Johnson, 3rd	R. F. D. 7, Box 201, Richmond, Va.
Sacramento (San Francisco Section)	C. R. Day	G. A. Fleming	3711 17th St., Sacramento 18, Calif.
Saginaw Valley (Michigan Section)	E. L. Holmgren	F. H. Justin	401 W. Sugnet Road, Midland, Mich.
St. Maurice Valley (Montreal Section)	E. A. McKeagan	H. T. Ball	11 Wayagamack Island, Three Rivers, Que., Canada
San Jose (San Francisco Section)	L. O. Templeton	Warren Raab	1188 Thornton Way., Campbell, Calif.
Sangamon (Illinois Valley Section)	G. E. Sangster	J. T. Youngblood	Sangamo Elec. Co., Springfield, Ill.
Shasta (San Francisco Section)	W. G. Whitney	G. A. Thompson	U. S. Bureau of Reclamation, Redding, Calif.
Tacoma (Seattle, Section)	Glen H. Walker	Harry M. Manning	Tacoma City Light, 402 City Hall, Tacoma 2, Wash.
Tri-State (West Virginia Section)			
West Central Texas (North Texas Section)	R. D. Henley	C. B. Herbert	Route 2, Hamlin, Tex.
West Coast (Florida)	J. G. Ennis		
West Michigan (Michigan Section)	R. M. Chermak	L. A. Zahorsky	127 Colfax, N.E., Grand Rapids, Mich.
Wilmington (Philadelphia Section)	E. W. Randall, Jr.	E. R. Streed	R. F. D. 3, Box 156, Wilmington, Del.
Zanesville (Columbus Section)	R. W. Hartsook	C. G. Winters	1115 Seborn Ave., Zanesville, Ohio

Geographical District Executive Committees

District	Chairman (Vice-President, AIEE)	Secretary (District Secretary)	Chairman, District Committee on Student Activities
1 North Eastern	Victor Siegfried, American Steel & Wire Co., Worcester 7, Mass.	Frederick G. Webber, 9 Federal Court, Springfield 5, Mass.	A. G. Conrad, Yale University, New Haven, Conn.
2 Middle Eastern	C. G. Veinott, Westinghouse Electric Corp., Lima, Ohio	W. A. Dynes, 312 Lonsdale Ave., Dayton 9, Ohio	K. F. Sibila, University of Akron, Akron 4, Ohio
3 New York City	J. L. Callahan, R. C. A. Laboratories, Radio Corporation of America, 66 Broad St., New York 4, N. Y.	R. T. Oldfield, Public Service Commission, 233 Broadway, New York 7, N. Y.	F. A. Wahlers, Polytechnic Inst. of Brooklyn, 99 Livingston St., Brooklyn 2, N. Y.
4 Southern	W. J. Seeley, Duke University, Durham, N. C.	T. H. Mawson, Commonwealth & Southern Corp., 600 North 18th St., Birmingham 2, Ala.	H. B. Duling, Georgia Institute of Technology, Atlanta, Ga.
5 Great Lakes	E. W. Seeger, Cutler-Hammer, Inc., 315 N. 12th St., Milwaukee 1, Wis.	N. C. Pearcy, Pioneer Service & Engineering Co., 231 S. LaSalle St., Chicago 4, Ill.	E. W. Kane, Marquette Univ., 1515 W. Wisconsin Ave., Milwaukee 3, Wis.
6 North Central	W. C. DuVall, University of Colorado, Boulder, Colo.	G. E. Gless, Jr., University of Colorado, Boulder, Colo.	C. H. Chinburg, Colorado A. & M. College, Fort Collins, Colo.
7 South West	G. N. Pingree, General Electric Co., 1801 N. Lamar St., Dallas 2, Tex.	Gibbs A. Dyer, Southwestern Bell Telephone Co., Dallas 1, Tex.	Ralph W. Tapy, University of New Mexico, Albuquerque, N. Mex.
8 Pacific	R. A. Hopkins, Westinghouse Electric Corp., 600 St. Paul Ave., Los Angeles 14, Calif.	T. M. Blakeslee, Dept. of Water & Power, City of Los Angeles, Box 3669 Terminal Annex, Los Angeles 54, Calif.	R. C. Lewis, University of Southern Calif., Los Angeles 7, Calif.
9 North West	Richard McKay, Washington Water Power Co., West 825 Trent Ave., Spokane 6, Wash.	H. C. Glaze, Jr., General Electric Co., S. 162 Post St., Spokane 8, Wash.	G. D. Sheckels, Montana State College, Bozeman, Mont.
10 Canada	A. H. Frampton, English Elec. Co. of Canada Ltd., St. Catharines, Ontario, Canada	J. I. Gram, Hydro-Electric Power Comm. of Ont., Box 237, Niagara Falls, Ontario, Canada	

Note: Each District executive committee includes also the chairmen and secretaries of all Sections within the District, the District vice-chairman of the AIEE membership committee, and a member of the Sections Committee who is resident in the District.

Student Branches

Name and Location	District	Counselor (Member of Faculty)
Akron, University of, Akron, Ohio	2	K. F. Sibila
Alabama Polytechnic Inst., Auburn	4	R. D. Spann
Alabama, University of, University	4	W. F. Gray
Alberta, University of, Edmonton, Canada	10	R. E. Phillips
Arizona, University of, Tucson	8	J. C. Clark
Arkansas, Univ. of, Fayettesville	7	N. H. Barnette
British Columbia, Univ. of, Vancouver, Canada	10	W. B. Coulthard
Brooklyn, Polytechnic Inst. of, Brooklyn, N. Y., (Day)	3	F. A. Wahlers
Brooklyn, Polytechnic Inst. of, Brooklyn, N. Y., (Evening)	3	Anthony B. Giordano
Brown University, Providence, R. I.	1	F. N. Tompkins
Bucknell University, Lewisburg, Pa.	2	Ralph C. Walker
California Inst. of Technology, Pasadena	8	William H. Pickering
California, University of, Berkeley	8	R. M. Saunders
Carnegie Inst. of Technology, Pittsburgh, Pa.	2	George M. Anderson
Case Inst. of Technology, Cleveland, Ohio	2	Carl F. Schuneman
Catholic University of America, Washington, D. C.	2	J. C. Michalowicz
Cincinnati, Univ. of, Cincinnati, Ohio	2	A. C. Herweh
Clarkson College of Technology, Potsdam, N. Y.	1	George W. Reed
Clemson A. & M. College, Clemson, S. C.	4	F. T. Tingley
Colorado A. & M. College, Fort Collins	6	C. H. Chinburg
Colorado, University of, Boulder	6	W. G. Worcester
Columbia University, New York, N. Y.	3	W. A. LaPierre
Connecticut, Univ. of, Storrs	1	Clarence W. Schultz
Cooper Union, New York, N. Y.	3	E. W. Starr
Cornell University, Ithaca, N. Y.	1	M. S. McIllroy
Delaware, University of, Newark	2	H. S. Bueche
Denver, University of, Denver, Colo.	6	A. M. Stiles
Detroit, University of, Detroit, Mich.	5	H. O. Warner
Drexel Inst. of Technology, Philadelphia, Pa.	2	F. C. Powell
Duke University, Durham, N. C.	4	Otto Meier, Jr.
Fenn College, Cleveland, Ohio	2	R. W. Schindler
Florida, University of, Gainesville	4	Paul H. Nelson
George Washington Univ., Washington, D. C.	2	Jerome S. Antel, Jr.
Georgia Inst. of Technology, Atlanta, Ga.	4	H. B. Duling
Harvard University, Cambridge, Mass.	1	John C. Fisher
Idaho, University of, Moscow	9	J. Hugo Johnson
Illinois Inst. of Technology, Chicago	5	E. T. B. Gross
Illinois, University of, Urbana	5	E. A. Reid
Iowa State College, Ames	5	G. Richardson
Iowa, University of, Iowa City	5	E. B. Kurtz
Johns Hopkins University, Baltimore, Md.	2	T. Larsen
Kansas State College, Manhattan	7	Earl L. Sitz
Kansas, University of, Lawrence	7	E. B. Phillips
Kentucky, University of, Lexington	4	H. Alex Romanowitz
Lafayette College, Easton, Pa.	2	J. G. Reifsnyder
Lehigh University, Bethlehem, Pa.	2	W. Lyle Donaldson
Louisiana Polytechnic Inst., Ruston, La.	4	Milton R. Johnson, Jr.
Louisiana State University, Baton Rouge	4	A. K. Ramsey
Louisville, University of, Louisville, Ky.	4	Harry T. Smith
Maine, University of, Orono	1	P. M. Seal
Manhattan College, New York, N. Y.	3	Robert T. Weil
Marquette University, Milwaukee, Wis.	5	E. W. Kane
Maryland, University of, College Park	2	L. J. Hodgins
Massachusetts Inst. of Technology, Cambridge	1	E. W. Boehne
Michigan College of Mining & Technology, Houghton	5	G. W. Swenson
Michigan State College, East Lansing	5	C. E. Goodell
Michigan, University of, Ann Arbor	5	John J. Carey
Milwaukee School of Engineering, Milwaukee, Wis.	5	R. J. Ungrodt
Minnesota, University of, Minneapolis	5	J. H. Kuhlmann
Mississippi State College, State College	4	Paul B. Jacobs, Jr.
Missouri School of Mines & Metallurgy, Rolla	7	I. H. Lovett
Missouri, University of, Columbia	7	G. V. Lago
Montana State College, Bozeman	9	G. D. Sheckels
Nebraska, University of, Lincoln	6	F. J. Ballard, Jr.
Nevada, University of, Reno	8	I. J. Sandorf
Newark College of Engineering, Newark, N. J.	3	C. R. Moore
New Hampshire, University of, Durham, N. H.	1	A. J. Merlini
New Mexico College of A. & M. Arts, State College	7	Russell Riese
New Mexico, University of, Albuquerque	7	J. Lawton Ellis
New York, College of the City of, New York, N. Y.	3	Harry Baum
New York, University of, New York	3	James Ley
North Carolina State College, Raleigh	4	W. D. Stevenson, Jr.
North Dakota Agri. College, Fargo	5	Harry S. Dixon
North Dakota, University of, Grand Forks	5	K. B. MacKichan
Northeastern University, Boston, Mass.	1	Roland G. Porter
Northwestern University, Evanston, Ill.	5	A. H. Wing
Norwich University, Northfield, Vt.	1	
Notre Dame, University of, Notre Dame, Ind.	5	L. F. Stauder
Ohio Northern University, Ada	2	W. R. Ankrom
Ohio State University, Columbus	2	Edmund D. Ayres
Ohio University, Athens	2	D. B. Green
Oklahoma A. & M. College, Stillwater	7	David L. Johnson
Oklahoma, University of, Norman	7	Gerald Tuma
Oregon State College, Corvallis	9	Melvin J. Kofoid
Pennsylvania State College, State College	2	A. P. Powell
Pennsylvania University of, Philadelphia	2	Kenneth C. Fegley
Pittsburgh, University of, Pittsburgh, Pa.	2	R. C. Gorham
Pratt Institute, Brooklyn, N. Y.	3	Robert B. Morgan
Princeton University, Princeton, N. J.	2	Norman W. Mather
Puerto Rico, University of, Mayaguez, P. R.	3	Miguel Wiewall, Jr.
Purdue University, Lafayette, Ind.	5	C. R. Nichols
Rensselaer Polytechnic Institute, Troy, N. Y.	1	Emerson D. Broadwell
Rhode Island State College, Kingston	1	John L. Hummer
Rice Institute, Houston, Tex.	7	J. S. Waters
Rose Polytechnic Institute, Terre Haute, Ind.	5	C. C. Knipmeyer
Rutgers University, New Brunswick, N. J.	3	P. H. Daggett
Santa Clara, Univ. of, Santa Clara, Calif.	8	Henry P. Nettesheim
South Carolina, Univ. of, Columbia	4	S. A. Ferguson
South Dakota School of Mines & Tech., Rapid City	6	J. O. Kammerman
South Dakota State College, Brookings	5	Wm. H. Gamble
Southern California, Univ. of, Los Angeles	8	Rodney C. Lewis
Southern Methodist Univ., Dallas, Tex.	7	F. W. Tatum
Stanford University, Stanford, University, Calif.	8	W. G. Hoover
Stevens Inst. of Technology, Hoboken, N. J.	3	W. L. Sullivan
Swarthmore College, Swarthmore, Pa.	2	C. J. Garrahan
Syracuse University, Syracuse, N. Y.	1	Henry F. Cooke
Tennessee, University of, Knoxville	4	W. O. Leffell
Texas, A. & M. College of, College Station	7	N. F. Rode
Texas Technological College, Lubbock	7	
Texas, University of, Austin	7	A. J. McCrocklin, Jr.
Texas Western College Sub-Branch, El Paso		
Toronto, University of, Toronto, Ontario, Canada	10	L. S. Lauchland
Tufts College, Medford, Mass.	1	A. H. Howell
Tulane University, New Orleans, La.	4	Marion E. Forsman
Union College, Schenectady, N. Y.	1	Owen G. Owens
Utah, University of, Salt Lake City	9	O. C. Haycock
Vanderbilt University, Nashville, Tenn.	4	Walter Criley
Vermont, University of, Burlington	1	Howard M. Smith, J
Villanova College, Villanova, Pa.	2	John B. Clothier
Virginia Military Institute, Lexington	4	J. S. Jamison
Virginia Polytechnic Institute, Blacksburg	4	Claudius Lee
Virginia, University of, Charlottesville	4	Gordon K. Carter
Washington, State College of, Pullman	9	O. E. Osburn
Washington, University of, Seattle	9	H. M. Rustebakke
Washington University, St. Louis, Mo.	7	Pierre M. Honnell
Wayne University, Detroit, Mich.	5	Edward L. Fairchild
West Virginia University, Morgantown	2	Everette C. Dubbe
Wisconsin, University of, Madison	5	Vincent C. Rideout
Worcester Poly. Institute, Worcester, Mass.	1	Donald C. Alexander
Wyoming, University of, Laramie	6	R. O. Trueblood
Yale University, New Haven, Conn.	1	A. G. Conrad
Total Branches 129		

Committees and Representatives for 1949-1950

General Committees

Executive

J. F. Fairman, *chairman*, Consolidated Edison Company of New York, Inc, 4 Irving Place, New York 3, N. Y.

J. L. Callahan	New York, N. Y.
W. L. Everitt	Urbana, Ill.
R. T. Henry	Buffalo, N. Y.
Everett S. Lee	Schenectady, N. Y.
W. I. Slichter	New York, N..Y.
E. P. Yerkes	Philadelphia, Pa.

Board of Examiners

E. D. Doyle, *chairman*, Leeds & Northrup Company, 4901 Stenton Ave., Philadelphia 44, Pa.

P. H. Adams, *vice-chairman*,	Newark, N. J.
John J. Anderson, Jr., *secretary*	AIEE Headquarters
A. E. Anderson	Phildelphia, Pa.
R. W. Atkinson	Bayonne, N. J.
R. H. Barclay	New York, N. Y.
Robin Beach	Brooklyn, N. Y.
H. A. Dambly	Philadelphia, Pa.
E. W. Davis	Cambridge, Mass.
C. W. Franklin	New York, N. Y.
W. N. Goodwin, Jr.	Newark, N. J.
I. W. Gross	New York, N. Y.
N. S. Hibshman	Brooklyn, N. Y.
L. F. Hickernell	Hastings-on-Hudson, N. Y.
C. R. Jones	New York, N. Y.
I. F. Kinnard	West Lynn, Mass.
A. E. Knowlton	Short Beach, Conn.
C. M. Laffoon	East Pittsburgh, Pa.
Henry Logan	New York, N. Y.
L. G. Pacent	New York, N. Y.
R. F. Penman	New York, N. Y.
J. J. Pilliod	New York, N. Y.
R. C. Roe	New York, N. Y.
F. J. Scudder	Manhasset, N. Y.
George Sutherland	New York, N. Y.
Gordon Thompson	New York, N. Y.
H. M. Trueblood	Dobbs Ferry, N. Y.
S. S. Watkins	New York, N. Y.
H. H. Weber	New York, N. Y.
John Westbye	New York, N. Y.
Sidney Withington	New Haven, Conn.

Constitution and By-Laws

N. B. Hinson, *chairman*, Southern California Edison Company, Edison Building, Los Angeles 53, Calif.

F. M. Farmer	New York, N. Y.
W. Scott Hill	Baltimore, Md.
W. W. Lewis	Schenectady, N. Y.
W. S. Peterson	Los Angeles, Calif.
Walter C. Smith	Palo Alto, Calif.

Finance

E. P. Yerkes, *chairman*, Bell Telephone Company of Pennsylvania, 1401 Arch Street, Philadelphia 2, Pa.

W. J. Barrett	Newark, N. J.
R. T. Henry	Buffalo, N. Y.
H. J. Scholz	Birmingham, Ala.
Victor Siegfried	Worcester, Mass.

Headquarters

C. S. Purnell, *chairman*, Westinghouse Electric Corporation, 40 Wall Street, New York 5, N. Y.

H. H. Henline	New York, N. Y.
H. H. Race	Schenectady, N. Y.
E. P. Yerkes	Philadelphia, Pa.

Members-for-Life Fund

C. R. Beardsley, *chairman*, Consolidated Edison Company of New York, Inc., 4 Irving Place, New York 3, N. Y.

T. F. Barton	New York, N. Y.
N. E. Funk	Philadelphia, Pa.
A. H. Hull	Toronto, Ontario, Canada
E. H. Martindale	Cleveland, Ohio
F. D. Newbury	Pittsburgh, Pa.
W. I. Slichter	New York, N. Y.
R. W. Sorensen	Pasadena, Calif.
J. F. Tritle	Erie, Pa.

Membership

F. S. Black, *chairman*, *Electrical World*, 330 West 42 Street, New York 18, N. Y.

J. C. Woods, *vice-chairman*	Chicago, Ill.
F. A. Norris, *secretary*	AIEE Headquarters
C. H. Cutter	Seattle, Wash.
J. A. Duncan, Jr.	New York, N. Y.
C. W. Fick	Cleveland, Ohio
H. R. Heckendorn	Chicago, Ill.
R. C. Horn	St. Louis, Mo.
Thomas Ingledow	Vancouver, British Columbia, Canada
F. H. Knapp	Cleveland, Ohio
M. J. Lantz	Portland, Oreg.
W. Stone Leake	New Orleans, La.
W. W. Morris	Los Angeles, Calif.
H. N. Muller, Jr.	East Pittsburgh, Pa.
C. G. Rumohr	Denver, Colo.
A. M. Spaulding	Denver, Colo.

District Vice-Chairmen

W. C. Bloomquist (1)	Schenectady, N. Y.
M. L. Lehman (2)	Philadelphia, Pa.
Charles Clos (3)	New York, N. Y.
J. D. Harper (4)	Alcoa, Tenn.
H. M. Craig (5)	Detroit, Mich.
A. S. Anderson (6)	Denver, Colo.
E. W. Allen (7)	Oklahoma City, Okla.
P. L. Savage (8)	Los Angeles, Calif.
C. R. Kingsbury (9)	Seattle, Wash.
C. L. Roach (10)	Montreal, Quebec, Canada

Ex-officio

Chairman of membership committees of Sections

Planning and Co-ordination

M. D. Hooven, *chairman*, Public Service Electric and Gas Company, 80 Park Place, Newark 1, N. J.

D. I. Anzini	San Francisco, Calif.
W. R. G. Baker	Syracuse, N. Y.
G. W. Bower	Haddonfield, N. J.
H. C. Coleman	East Pittsburgh, Pa.
F. E. Harrell	Cleveland, Ohio
L. F. Hickernell	Hastings-on-Hudson, N. Y.
N. B. Hinson	Los Angeles, Calif.
T. G. LeClair	Chicago, Ill.
Everett S. Lee	Schenectady, N. Y.
K. B. McEachron	Pittsfield, Mass.
J. R. North	Jackson, Mich.
J J. Orr	New York, N. Y.
Elgin B. Robertson	Dallas, Tex.
H. I. Romnes	New York, N. Y.
M. J. Steinberg	New York, N. Y.
J. D. Tebo	New York, N. Y.
C. H. Willis	Princeton, N. J.
E. P. Yerkes	Philadelphia, Pa.

Publication

K. B. McEachron, *chairman*, General Electric Company, 100 Woodlawn Ave., Pittsfield, Mass.

F. R. Benedict	Pittsburgh, Pa.
J. M. Flanigen	Atlanta, Ga.
P. B. Garrett	Chicago, Ill.
H. H. Henline	New York, N. Y.
R. K. Honaman	New York, N. Y.
B. M. Jones	Pittsburgh, Pa.
F. O. McMillan	Corvallis, Oreg.
G. C. Quinn	Milwaukee, Wis.
B. R. Teare, Jr.	Pittsburgh, Pa.
G. C. Tenney	San Francisco, Calif.
C. H. Willis	Princeton, N. J.

Public Relations

R. K. Honaman, *chairman*, Bell Telephone Laboratories, Inc., 463 West Street, New York 14, N. Y.

G. T. Minasian, *vice-chairman*	New York, N. Y.
Guy Bartlett	Schenectady, N. Y.
G. W. Bower	Haddonfield, N. J.
M. Getting	Pittsburgh, Pa.
H. H. Henline	New York, N. Y.
A. E. Knowlton	Short Beach, Conn.
H. W. Marquardt	New York, N. Y.
K. B. McEachron	Pittsfield, Mass.
H. J. Scholz	Birmingham, Ala.
D. W. Taylor	Newark, N. J.
A. R. Thompson	New York, N. Y.
C. H. Willis	Princeton, N. J.

(*Continued in next column*)

Research

M. J. Kelly, *chairman*, Bell Telephone Laboratories, Inc., 463 West Street, New York 14, N. Y.

J. A. Hutcheson, *vice-chairman*	East Pittsburgh, Pa.
M. B. Long, *secretary*	New York, N. Y.
B. G. Ballard	Ottawa, Ontario, Canada
J. W. Barker	New York, N. Y.
R. D. Bennett	Silver Spring, Md.
L. V. Berkner	Washington, D. C.
E. W. Bochne	Cambridge, Mass.
Cledo Brunetti	Stanford, Calif.
C. R. Burrows	Ithaca, N. Y.
A. G. Conrad	New Haven, Conn.
H. P. Corwith	New York, N. Y.
H. E. Edgerton	Cambridge, Mass.
W. L. Everitt	Urbana, Ill.
R. S. Glasgow	Annapolis, Md.
T. A. Hopkins	Spokane, Wash.
T. J. Kilan	Washington, D. C.
J. T. Littleton	Corning, N. Y.
A. D. Moore	Ann Arbor, Mich.
B. K. Northrop	Ithaca, N. Y.
W. H. Pickering	Pasadena, Calif.
C. S. Schifreen	Philadelphia, Pa.
E. F. Seaman	Washington, D. C.
C. G. Suits	Schenectady, N. Y.
J. B. Thomas	Fort Worth, Tex.
Eric Walker	State College, Pa.
V. K. Zworykin	Princeton, N. J.

Safety

W. B. Kouwenhoven, *chairman*, The Johns Hopkins University, Baltimore 18, Md.

M. M. Brandon, *vice-chairman* and *secretary*	New York, N. Y.
Robin Beach	Brooklyn, N. Y.
J. H. Berry	Norfolk, Va.
Hendley Blackmon	New York, N. Y.
W. B. Buchanan	Toronto, Ontario, Canada
C. F. Dalziel	Berkeley, Calif.
G. S. Diehl	Lancaster, Pa.
W. P. Dobson	Toronto, Ontario, Canada
J. P. Ferguson	Los Angeles, Calif.
L. P. Ferris	New York, N. Y.
J. M. Flanigen	Atlanta, Ga.
J. A. Gienger	Rochester, N. Y.
M. C. Goodspeed	Harborcreek, Pa.
Floyd Goss	Los Angeles, Calif.
N. B. Hinson	Los Angeles, Calif.
O. S. Hockaday	Fort Worth, Tex.
J. O. Leslie	Reading, Pa.
R. L. Lloyd	Washington, D. C.
Albrecht Naeter	Stillwater, Okla.
G. R. Radley	Milwaukee, Wis.
C. N. Rakestraw	Cleveland, Ohio
P. X. Rice	State College, Pa.
W. T. Rogers	New York, N. Y.
A. H. Schirmer	New York, N. Y.
A. B. Smith	New York, N. Y.
W. R. Smith	Newark, N. J.
E. E. Turkington	Boston, Mass.
H. H. Watson	Bridgeport, Conn.
H. B. Williams	New York, N. Y.
William R. Wood	Windber, Pa.

Medical Consultants

Dr. Anna Baetjer, The Johns Hopkins University, Baltimore, Md.

Dr. Cecil K. Drinker, Harvard University, Cambridge, Mass.

Dr. T. L. Hazlett, Westinghouse Electric Corporation, Pittsburgh, Pa.

Dr. B. L. Vosburgh, General Electric Company, Schenectady, N. Y.

Sections

D. I. Anzini, *chairman*, General Electric Company, 806 Russ Building, San Francisco 4, Calif.

C. S. Purnell, *vice-chairman* (East)	New York, N. Y.
Walter C. Smith, *vice-chairman* (West)	Palo Alto, Calif.
F. S. Benson, *secretary*	San Francisco, Calif.
W. J. Barrett	Newark, N. J.
F. S. Black	New York, N. Y.
G. W. Bower	Haddonfield, N. J.
T. C. D. Churchill	Toronto, Ontario, Canada
Bradley Cozzens	Los Angeles, Calif.
H. A. Dambly	Philadelphia, Pa.
O. R. Enriquez	Mexico, D. F., Mexico
C. W. Fick	Cleveland, Ohio
R. C. Horn	St. Louis, Mo.
R. E. Kistler	Seattle, Wash.
C. P. Knost	New Orleans, La.
C. W. Lethert	Minneapolis, Minn.
A. L. O'Banion	Boston, Mass.
L. R. Patterson	Denver, Colo.
F. H. Pumphrey	Gainesville, Fla.
R. V. Shepherd	Schnectady, N. Y.
E. W. Stone	Peoria, Ill.
J. C. Strasbourger	Cleveland, Ohio
H. H. Wagner	Canonsburg, Pa.

(*Continued in next column*)

Ex-officio

Chairman of Sections

Standards

F. E. Harrell, *chairman*, Reliance Electric and Engineering Company, 1088 Ivanhoe Road, Cleveland 10, Ohio

E. B. Paxton, *vice-chairman*	Schenectady, N. Y.
John J. Anderson, Jr., *secretary*	AIEE Headquarters
Joseph Wm. Allen	Teterboro, N. J.
R. W. Atkinson	Bayonne, N. J.
E. L. Bailey	Highland Park, Mich.
Sterling Beckwith	Milwaukee, Wis.
F. R. Benedict	Pittsburgh, Pa.
R. C. Bergvall	East Pittsburgh, Pa.
M. M. Brandon	New York, N. Y.
J. E. Clem	Schenectady, N. Y.
C. L. Dawes	Cambridge, Mass.
R. D. de Kay	New York, N. Y.
W. P. Dobson	Toronto, Ontario, Canada
C. M. Gilt	New York, N. Y.
I. W. Gross	New York, N. Y.
John Grotzinger	Akron, Ohio
R. T. Henry	Buffalo, N. Y.
H. R. Huntley	New York, N. Y.
H. E. Kent	New York, N. Y.
A. C. Muir	Philadelphia, Pa.
J. R. North	Jackson, Mich.
J. E. O'Brien	Washington, D. C.
G. H. O'Sullivan	New York, N. Y.
F. B. Silsbee	Washington, D. C.

Ex-officio

Chairman of Standards subcommittees and co-ordinating committees

Chairman of AIEE technical committees

Chairman of AIEE delegations on other standardizing bodies or sole representative thereon

President, U. S. National Committee of the International Electrotechnical Commission

President, Canadian National Committee of the International Electrotechnical Commission

Standards Coordinating Committees

No. 1—REFERENCE VALUES FOR STANDARDS

P. L. Bellaschi, *chairman*	Sharon, Pa.
J. J. Clark	East Pittsburgh, Pa.
R. F. Field	Cambridge, Mass.
E. J. Rutan	New York, N. Y.
F. B. Silsbee	Washington, D. C.
J. J. Smith	Schenectady, N. Y.
I. M. Stein	Philadelphia, Pa.
Gordon Thompson	New York, N. Y.

No. 2—STANDARD VOLTAGES AND CURRENTS

C. A. Powel, *chairman*	East Pittsburgh, Pa.
W. P. Dobson (*ex-officio*)	Toronto, Canada
R. A. Hentz	Philadelphia, Pa.
D. M. Jones	Schenectady, N. Y.

No. 3—Surge Testing and Insulation

V. M. Montsinger, *chairman*	Schenectady, N. Y.
F. R. Benedict	East Pittsburgh, Pa.
F. E. Harrell	Cleveland, Ohio
Philip Sporn	New York, N. Y.

No. 4—Basic Principles for Rating of Electric Machinery and Apparatus

W. R. Hough, *chairman*	Cleveland, Ohio
P. L. Alger	Schenectady, N. Y.
R. T. Henry	Buffalo, N. Y.
H. E. Kent	New York, N. Y.
C. M. Laffoon	East Pittsburgh, Pa.
T. H. Morgan	Worcester, Mass.
J. J. Orr	Schenectady, N. Y.
E. B. Paxton	Schenectady, N. Y.
R. Rudenberg	Cambridge, Mass.

No. 5—Basic Theories and Units

E. E. Bennett, *chairman*	Madison, Wis.
E. C. Crittenden	Washington, D. C.
H. L. Curtis	Washington, D. C.
H. L. Hazen	Cambridge, Mass.
E. S. Lee	Schenectady, N. Y.
F. B. Silsbee	Washington, D. C.
J. Slepian	East Pittsburgh, Pa.
E. Weber	Brooklyn, N. Y.

No. 6—Interference, Sound, and Vibration Problems

R. S. Tucker, *chairman*	New York, N. Y.
P. L. Alger	Schenectady, N. Y.
W. F. Davidson	New York, N. Y.
H. E. Kent	New York, N. Y.
L. J. Plucknett	Washington, D. C.
Gordon Thompson	New York, N. Y.

No. 7—Conduction in Vacuum, Gases, Liquids, and Solids

Disbanded in 1944

No. 8—Insulation Coordination

J. H. Foote, *chairman*	Jackson, Mich.
J. E. Clem	Schenectady, N. Y.
I. W. Gross	New York, N. Y.
R. T. Henry	Buflalo, N. Y.
S. E. Schultz	Portland, Oreg.
T. G. A. Sillers	Milwaukee, Wis.
J. J. Taylor	Barberton, Ohio
C. F. Wagner	East Pittsburgh, Pa.

Technical Program

C. H. Willis, *chairman*, Princeton University, Princeton, N. J.

M. M. Brandon, *vice-chairman* New York, N. Y.

Edward C. Day, *secretary*	AIEE Headquarters
J. L. Callahan	New York, N. Y.
P. H. Light	Schenectady, N. Y.
F. O. McMillan	Corvallis, Oreg.
Robert D. Miller	San Francisco, Calif.
S. H. Mortensen	Milwaukee, Wis.
M. J. Steinberg	New York, N. Y.
Frank Thornton, Jr.	East Pittsburgh, Pa.
H. M. Turner	New Haven, Conn.

Ex-officio

Chairman of
- Technical co-ordinating committees
- Technical committees
- Committee on Education
- Committee on Planning and Co-ordination
- Committee on Research
- Committee on Safety
- Standards Committee

Transfers

F. W. Willcutt, *chairman*, Potomac Electric Power Company, 10th & E Streets, N. W., Washington 4, D. C.

C. W. Fick, *vice-chairman*	Cleveland, Ohio
L. F. Hunt, *secretary*	Los Angeles, Calif.
F. A. Cooper	St. Louis, Mo.
O. A. Demuth	Portland, Oreg.
John Gammell	Philadelphia, Pa.
J. D. Harper	Alcoa, Tenn.
W. R. Hough	Cleveland, Ohio
M. M. Moore	Washington, D. C.
L. R. Patterson	Denver, Colo.
F. H. Pumphrey	Gainesville, Fla.
John Wm. Robinson	San Francisco, Calif.
H. K. Smith	Chicago, Ill.
H. B. Wood	Boston, Mass.

(*Continued in next column*)

Ex-officio

Chairman of transfers committees of Sections

Professional Group Committees

Professional Group Co-ordinating

T. G. LeClair, *chairman*, Commonwealth Edison Company, 72 West Adams Street, Chicago 90, Ill.

W. Scott Hill

Chairman of professional group committees

Charles LeGeyt Fortescue Fellowship

H. N. Muller, Jr., *chairman*	East Pittsburgh, Pa.
H. P. Sedwick	Chicago, Ill.
	(*Terms expire July 31, 1950*)
J. W. Barker	New York, N. Y.
R. I. Wilkinson	New York, N. Y.
	(*Terms expire July 31, 1951*)
F. W. Bush	Milwaukee, Wis.
A. G. Conrad	New Haven, Conn.
	(*Terms expire July 31, 1952*)

Code of Principles of Professional Conduct

Harry Barker, *chairman*, Barker and Wheeler, 11 Park Place, New York 7, N. Y.

P. L. Alger	Schenectady, N. Y.
D. D. Ewing	Lafayette, Ind.
E. S. Fields	Cincinnati, Ohio
E. H. Flath	Dallas, Tex.
Harold Goodwin, Jr.	Wyncote, Pa.
Dugald C. Jackson	Cambridge, Mass.
R. D. Maxson	Chicago, Ill.
R. W. Sorensen	Pasadena, Calif.

Edison Medal

Appointed by the President for term of five years

C. V. Christie	Montreal, Quebec, Canada
Zay Jeffries	Pittsfield, Mass.
Harold Pender	Philadelphia, Pa.
	(*Terms expire July 31, 1950*)
C. A. Corney	Boston, Mass.
J. R. North	Jackson, Mich.
I. M. Stein	Philadelphia, Pa.
	(*Terms expire July 31, 1951*)
Marvin W. Smith	Philadelphia, Pa.
H. S. Osborne	New York, N. Y.
H. A. Winne	Schenectady, N. Y.
	(*Terms expire July 31, 1952*)
D. I. Cone	San Francisco, Calif.
J. B. MacNeill, *chairman*	East Pittsburgh, Pa.
Philip Sporn	New York, N. Y.
	(*Terms expire July 31, 1953*)
J. L. Callahan	New York, N. Y.
J. M. Flanigen	Atlanta, Ga.
R. I. Wilkinson	New York, N. Y.
	(*Terms expire July 31, 1954*)

Elected by the Board of Directors from its own membership for term of two years

R. T. Henry	Buffalo, N. Y.
M. D. Hooven	Newark, N. J.
Victor Siegfried	Worcester, Mass.
	(*Terms expire July 31, 1950*)
W. L. Everitt	Urbana, Ill.
A. H. Frampton	St. Catharines, Ontario, Canada
A. C. Monteith	Pittsburgh, Pa.
	(*Terms expire July 31, 1951*)

Ex-officio

James F. Fairman, president	New York, N. Y.
W. I. Slichter, treasurer	New York, N. Y.
H. H. Henline, secretary	New York, N. Y.

Education

B. R. Teare, Jr., *chairman*, Carnegie Institute of Technology, Pittsburgh, Pa.

(*Continued in next column*)

R. D. Bennett	Silver Spring, Md.
R. L. Biesele	Dallas, Tex.
D. S. Bridgman	New York, N. Y.
E. E. Dreese	Columbus, Ohio
W. L. Everitt	Urbana, Ill.
A. W. Friend	Princeton, N. J.
H. E. Hartig	Minneapolis, Minn.
A. R. Hellwarth	Detroit, Mich.
T. G. LeClair	Chicago, Ill.
T. C. Lloyd	Springfield, Ohio
A. H. Lovell	Ann Arbor, Mich.
K. B. McEachron, Jr.	Schenectady, N. Y.
H. N. Muller, Jr.	East Pittsburgh, Pa.
J. A. Northcott, Jr.	Notre Dame, Ind.
R. C. Putnam	Cleveland, Ohio
Walther Richter	West Allis, Wis.
J. D. Ryder	Urbana, Ill.
E. M. Strong	Ithaca, N. Y.
G. F. Tracy	Toronto, Ontario, Canada
C. E. Tucker	Cambridge, Mass.
E. A. Walker	State College, Pa.
M. L. Waring	New York, N. Y.
S. Reid Warren, Jr.	Philadelphia, Pa.

Lamme Medal

F. E. Harrell	Cleveland, Ohio
D. A. Quarles	New York, N. Y.
J. B. Thomas, *chairman*	Fort Worth, Tex.
	(*Terms expire July 31, 1950*)
J. H. Berry	Norfolk, Va.
T. H. Morgan	Worcester, Mass.
L. A. Umansky	Schenectady, N. Y.
	(*Terms expire July 31, 1951*)
C. B. Jolliffe	Princeton, N. J.
A. H. Kehoe	New York, N. Y.
J. B. MacNeill	East Pittsburgh, Pa.
	(*Terms expire July 31, 1952*)

Management

A. C. Monteith, *chairman*, Westinghouse Electric Corporation, 306 Fourth Avenue, Pittsburgh 30, Pa.

P. L. Alger	Schenectady, N. Y.
K. P. Applegate	Hartford, Conn.
C. J. Beller	Cleveland, Ohio
R. D. Bennett	Silver Spring, Md.
H. K. Breckenridge	Pittsburgh, Pa.
F. W. Bush	Milwaukee, Wis.
W. L. Cisler	Detroit, Mich.
E. E. Hill	New York, N. Y.
E. L. Moreland	Boston, Mass.
H. A. Peterson	New York, N. Y.
D. A. Quarles	New York, N. Y.
F. M. Roberts	Schenectady, N. Y.
D. D. Smalley	San Francisco, Calif.
O. Van Rye	Boston, Mass.

Prizes, Award of Institute

C. H. Willis, *chairman*, Princeton University, Princeton, N. J.

D. I. Anzini	San Francisco, Calif.
H. C. Coleman	East Pittsburgh, Pa.
H. L. Davis, Jr.	Philadelphia, Pa.
L. F. Hickernell	Hastings-on-Hudson, N. Y.
W. C. Kalb	Cleveland, Ohio
M. J. Kelley	New York, N. Y.
T. G. LeClair	Chicago, Ill.
K. B. McEachron	Pittsfield, Mass.
F. O. McMillan	Corvallis, Oreg.
J. J. Orr	New York, N. Y.
H. I. Romnes	New York, N. Y.
F. M. Sebast	Troy, N. Y.
J. D. Tebo	New York, N. Y.
H. M. Turner	New Haven, Conn.

Registration of Engineers

Elgin B. Robertson, *chairman*, P. O. Box 745, Dallas 1, Tex.

E. H. Bancker	Schenectady, N. Y.
L. H. Cleary	Washington, D. C.
J. M. Flanigen	Atlanta, Ga.
J. H. Foote	Jackson, Mich.
N. L. Freeman	Schenectady, N. Y.
J. E. Hobson	Stanford, Calif.
J. T. Hood	Tacoma, Wash.
K. B. McEachron	Pittsfield, Mass.
E. W. Oesterreich	Pittsburgh, Pa.
William W. Perry	Binghamton, N. Y.
L. M. Robertson	Denver, Colo.
E. W. Seeger	Milwaukee, Wis.
R. W. Sorensen	Pasadena, Calif.
P. G. Wallace	Dallas, Tex.
R. G. Warner	New Haven, Conn.
E. R. Whitehead	Brookfield, Ill.

Student Branches

F. O. McMillan, *chairman*, Oregon State College, Corvallis, Oreg.

F. W. Tatum, *vice-chairman*	Dallas, Tex.
R. G. Porter, *secretary*	Boston, Mass.
Sterling Bechwith	Milwaukee, Wis.
R. A. Galbraithe	Syracuse, N. Y.
J. H. Kuhlmann	Minnespolis, Minn.
H. N. Muller, Jr.	East Pittsburgh, Pa.
L. T. Rader	Schenectady, N. Y.
E. A. Reid	Urbana, Ill.
E. W. Spring	Detroit, Mich.
W. G. Warren	Santa Clara, Calif.
B. S. Wills	Ames, Iowa

Chairman, District Committee on Student Activities

A. G. Conrad (1)	New Haven, Conn.
K. F. Sibila (2)	Akron, Ohio
F. A. Wahlers (3)	Brooklyn, N. Y.
H. B. Duling (4)	Atlanta, Ga.
E. W. Kane (5)	Milwaukee, Wis.
C. H. Chinburg (6)	Fort Collins, Colo.
R. W. Tapy (7)	Albuquerque, N. M.
R. C. Lewis (8)	Los Angeles, Calif.
G. D. Sheckels (9)	Bozeman, Mont.

Ex-officio

Counselors of Student Branches

Volta Scholarship Trustees

W. L. Everitt	Urbana, Ill.
	(*Term expries July 31, 1951*)
C. A. Powel	Cambridge, Mass.
	(*Term expires July 31, 1953*)
H. S. Osborne, *chairman*	New York, N. Y.
	(*Term expires July 31, 1955*)

(The functions of the foregoing committees are stated in Secs. 65–86 of the By-laws.)

Technical Committees

Communications Group

Communication Co-ordinating Committee

H. I. Romnes, *chairman*; American Telephone and Telegraph Company, 195 Broadway, New York 7, N. Y.

F. B. Bramhall, *vice-chairman*	New York, N. Y.
A. J. Busch, *secretary*	New York, N. Y.

Members-at-Large

J. L. Callahan, representing Board of Directors	New York, N. Y.
Rothwell E. Smith	New York, N. Y.

Ex-officio members

Chairman of the technical committees in the Communication Group

Aural Broadcasting Systems

E. G. Ports, *chairman*; Federal Telephone and Radio Corporation, 100 Kingsland Road, Clifton, N. J.

J. L. Middlebrooks, *vice-chairman*	New York, N. Y.
M. L. Levy, *secretary*	New York, N. Y.
T. M. Bloomer	Baltimore, Md.
T. J. Boerner	Camden, N. J.
L. E. Clossen	Philadelphia, Pa.
E. W. Davis	New York, N. Y.
C. E. Dean	Little Neck, N. Y.
P. G. Edwards	New York, N. Y.
L. K. Findley	Cedar Rapids, Iowa
J. C. Gaudio	Chicago, Ill.
R. W. Masters	Columbus, Ohio
Gerard Mountjoy	Rochester, N. Y.
R. M. Sprague	Waltham, Mass.
J. K. Tyler	Sunbury, Pa.
R. H. Williamson	Syracuse, N. Y.

Transmission Systems Subcommittee

J. L. Middlebrooks, *chairman*; American Broadcasting Company, Inc., 30 Rockefeller Plaza, New York 21, N. Y.

G. M. K. Baker	Princeton, N. J.

(*Continued on page 1417*)

M. Bloomer	Baltimore, Md.
W. Davis	New York, N. Y.
G. Edwards	New York, N. Y.
K. Findley	Cedar Rapids, Iowa
W. Masters	Columbus, Ohio
M. Sprague	Waltham, Mass.
H. Williamson	Syracuse, N. Y.

CEIVERS SUBCOMMITTEE

E. Dean, *chairman;* Hazeltine Electronics Corporation, 58-25 Little Neck Parkway, Little Neck, L. I., N. Y.

E. Clossen	Philadelphia, Pa.
C. Gaudio	Chicago, Ill.
L. Levy	New York, N. Y.
rard Mountjoy	Rochester, N. Y.
K. Tyler	Sunbury, Pa.

ommunication Switching Systems

C. Davis, *chairman;* Bell Telephone Laboratories, Inc., 463 West Street, New York 14, N. Y.

Meszar, *vice-chairman*	New York, N. Y.
E. Frost, *secretary*	New York, N. Y.
L. Burns	Dallas, Tex.
G. Geiger	Toronto, Ontario, Canada
H. Gray	New York, N. Y.
B. Haigh	New York, N. Y.
F. Lynch	Poughkeepsie, N. Y.
B. Smith	Chicago, Ill.
L. Solomon	Rochester, N. Y.
G. Storey	Chicago, Ill.
M. Tomlinson	Philadelphia, Pa.

o subcommittees

adio Communication Systems

T. Royden, *chairman;* MacKay Radio and Telegraph Company, 67 Broad Street, New York 4, N. Y.

D. Becken, *vice-chairman*	New York, N. Y.
C. Dickieson, *secretary*	New York, N. Y.
M. Backer	Clifton, N. J.
H. B. Bartelink	New York, N. Y.
F. Byrnes	New York, N. Y.
H. Cusack	New York, N. Y.
D. Grieg	Nutley, N. J.
R. Messner	Washington, D. C.

OBILE RADIO SUBCOMMITTEE

C. Dickieson, *chairman;* Bell Telephone Laboratories, Inc., 463 West Street, New York 14, N. Y.

M. Backer	Clifton, N. J.
F. Byrnes	New York, N. Y.

OINT-TO-POINT RADIO SUBCOMMITTEE

D. Becken, *chairman;* RCA Communications, Inc., 66 Broad Street, New York 4, N. Y.

H. B. Bartelink	New York, N. Y.
H. Cusack	New York, N. Y.
D. Greig	Nutley, N. J.
R. Messner	Washington, D. C.

pecial Communications Applications

W. Kenefake, *chairman;* Electronics Department, General Electric Company, Syracuse, N. Y.

A. Smith, *vice-chairman*	New York, N.Y.
C. Holmes, *secretary*	Rochester, N. Y.
C. Bartlett	New York, N. Y.
W. Baughman	Swissvale, Pa.
F. Byrnes	New York, N. Y.
Melville Eastham	Cambridge, Mass.
D. Hailes	Rochester, N. Y.
E. Kearney	New York, N. Y.
H. Kline	Baltimore, Md.
Newton Monk	New York, N. Y.
A. Ready	Malden, Mass.
H. Spencer	Camden, N. J.
B. Vaile, Jr.	Stanford, Calif.
J. Warner	Nutley, N. J.

LECTRO-ACOUSTICS SUBCOMMITTEE

R. B. Vaile, Jr., *chairman;* Stanford University, Stanford Research Institute, Stanford, Calif.

F. Byrnes	New York, N. Y.
C. Holmes	Rochester, N. Y.
H. Spencer	Camden, N. J.

RAILROAD COMMUNICATIONS SUBCOMMITTEE

W. D. Hailes, *chairman;* General Railway Signal Company, 801 West Avenue, Rochester 11, N. Y.

G. W. Baughman	Swissvale, Pa.
L. E. Kearney	New York, N. Y.
Newton Monk	New York, N. Y.

SPECIAL ACTIVITIES SUBCOMMITTEE

A. J. Warner, *chairman;* Federal Telecommunication Laboratories, 500 Washington Avenue, Nutley 10, N. J.

Melville Eastham	Cambridge, Mass.
R. H. Kline	Baltimore, Md.
Newton Monk	New York, N. Y.
H. H. Spencer	Camden N. J.

Telegraph Systems

I. S. Coggeshall, *chairman;* Western Union Telegraph Company, 60 Hudson Street, New York 13, N. Y.

J. A. Duncan, Jr., *vice-chairman*	New York, N. Y.
E. C. Chamberlain, *secretary*	New York, N. Y.
M. Alden	Brockton, Mass.
E. P. Bancroft	New York, N. Y.
A. S. Benjamin	Chicago, Ill.
A. G. Cooley	New York, N. Y.
W. G. H. Finch	New York, N. Y.
H. H. Haglund	New York, N. Y.
J. V. L. Hogan	New York, N. Y.
Grosvenor Hotchkiss	New York, N. Y.
Alfred Kahn	New York, N. Y.
J. A. Krechek	Washington, D. C.
R. E. Mathes	Hartford, Conn.
R. B. Steele	Toronto, Ontario, Canada
L. A. Thompson	Cleveland, Ohio
E. F. Watson	New York, N. Y.
R. J. Wise	New York, N. Y.
G. B. Worthen	New York, N. Y.
C. J. Young	Princeton, N. J.

FACSIMILE SUBCOMMITTEE

W. G. H. Finch, *chairman;* Finch Telecommunications, Inc., 10 East 40th Street, New York, N. Y.

M. Alden	Brockton, Mass.
A. S. Benjamin	Chicago, Ill.
A. G. Cooley	New York, N. Y.
J. V. L. Hogan	New York, N. Y.
J. A. Krehek	Washington, D. C.
R. E. Mathes	Hartford, Conn.
L. A. Thompson	Cleveland, Ohio
R. J. Wise	New York, N. Y.
G. B. Worthen	New York, N. Y.
C. J. Young	Princeton, N. J.

Television Broadcasting Systems

J. B. Coleman, *chairman;* Radio Corporation of America, RCA Victor Division, Camden, N. J.

L. G. Cumming, *vice-chairman*	New York, N. Y.
L. M. Rodgers, *secretary*	Philadelphia, Pa.
G. H. Brown	Princeton, N. J.
D. G. Fink	New York, N. Y.
J. C. Gaudio	Chicago, Ill.
P. C. Goldmark	New York, N. Y.
T. T. Goldsmith, Jr.	Passaic, N. J.
W. R. Hewlett	Palo Alto, Calif.
E. M. Johnston	New York, N. Y.
I. J. Kaar	Syracuse, N. Y.
W. L. Lawrence	Camden, N. J.
G. M. Nixon	New York, N. Y.
D. W. Pugley	Syracuse, N. Y.
F. M. Rives	Syracuse, N. Y.
R. E. Shelby	New York, N. Y.
S. C. Spielman	Philadelphia, Pa.
V. E. Trouant	Camden, N. J.
N. H. Young	Nutley, N. J.

TRANSMISSION SYSTEMS SUBCOMMITTEE

W. L. Lawrence, *chairman;* Radio Corporation of America, RCA Victor Division, Camden, N. J.

G. H. Brown	Princeton, N. J.
P. C. Goldmark	New York, N. Y.
E. M. Johnson	New York, N. Y.
G. M. Nixon	New York, N. Y.
F. M. Rives	Syracuse, N. Y.
V. E. Trouant	Camden, N. J.
N. H. Young	Nutley, N. J.

RECEIVERS SUBCOMMITTEE

S. C. Spielman, *chairman;* Philco Corporation, C and Tioga Streets, Philadelphia 34, Pa.

D. G. Fink	New York, N. Y.
J. C. Gaudio	Chicago, Ill.
T. T. Goldsmith, Jr.	Passaic, N. J.
W. R. Hewlett	Palo Alto, Calif.
I. J. Kaar	Syracuse, N. Y.
D. W. Pugsley	Syracuse, N. Y.
R. E. Shelby	New York, N. Y.

Wire Communications Systems

H. A. Affel, *chairman;* Bell Telephone Laboratories, Inc., 463 West Street, New York 14, N. Y.

G. B. Ransom, *vice-chairman*	New York, N. Y.
L. G. Abraham, *secretary*	New York, N. Y.
A. L. Albert	Corvallis, Oreg.
W. M. Allen	Portland, Oreg.
E. H. B. Bartelink	New York, N. Y.
J. D. Booth	Baltimore, Md.
Hezzie Clark	Houston, Tex.
A. B. Credle	Mineola, N. Y.
G. E. Dodrill	Washington, D. C.
H. E. Ellithorn	Notre Dame, Ind.
L. G. Erickson	San Carlos, Calif.
G. H. Gray	New York, N. Y.
W. D. Hailes	Rochester, N. Y.
E. B. Harmon	Denver, Colo.
H. R. Hunkins	Clifton, N. J.
R. R. O'Connor	Chicago, Ill.
F. M. Rives	Syracuse, N. Y.
A. L. Rumsey	Albany, N. Y.
J. O'D. Shepherd	Atlanta, Ga.
Arthur Bessey Smith	Chicago, Ill.
W. T. Smith	New York, N. Y.

POWER LINE CARRIER SUBCOMMITTEE

F. M. Rives, *chairman;* General Electric company, Electronics Park, Syracuse, N. Y.

J. D. Booth	Baltimore, Md.
G. E. Dodrill	Washington, D. C.
W. D. Hailes	Rochester, N. Y.
E. B. Harmon	Denver, Colo.
W. T. Smith	New York, N. Y.

General Applications Group

General Applications Co-ordinating Committee

H. C. Coleman, *chairman;* Marine and Aviation Section of Industry Engineering Department Westinghouse Electric Corporation, East Pittsburgh, Pa.

C. C. Whipple, *vice-chairman*	Brooklyn, N. Y.
O. A. Wilde, *secretary*	Chester, Pa.

Member-at-Large

Elgin B. Robertson, representing Board of Directors	Dallas, Tex.

Ex-officio members

Chairman of the technical committees in the General Applications Group

Air Transportation

W. V. Boughton, *chairman;* Phaostron Company, 151 Pasadena Avenue, South Pasadena, Calif.

D. E. Fritz, *vice-chairman*	Cleveland, Ohio
W. L. Berry, *secretary*	Culver City, Calif.
J. W. Allen	Teterboro, N. J.
G. W. Almassy	Los Angeles, Calif.
J. H. Blankenbuehler	Troy, Ohio
W. C. Bryant	Burbank, Calif.
J. P. Dallas	Culver City, Calif.
Peter Duyan, Jr.	Santa Monica, Calif.
D. W. Exner	Seattle, Wash.
Alec Fisher	Lynn, Mass.
Elio Fueyo	Miami, Fla.
C. E. Gagnier	Los Angeles, Calif.
S. H. Hanville, Jr.	Washington, D. C.
W. T. Harding	Dayton, Ohio
J. G. Hutton	Schenectady, N. Y.
R. R. Jenner	Wichita, Kans.
R. D. Jones	Fort Wayne, Ind.

(*Continued in next column*)

W. B. Keller	Cleveland, Ohio
R. J. Lusk	East Pittsburgh, Pa.
Karl Martinez	Seattle, Wash.
C. F. McCabe	San Diego, Calif.
R. E. McDonald	St. Paul, Minn.
R. S. Mead	Kansas City, Mo.
R. A. Millermaster	Milwaukee, Wis.
J.D. Miner	Lima, Ohio
C. R. Moore	Los Angeles, Calif.
W. F. Moore	Schenectady, N. Y.
Joseph Nader	Chicago, Ill.
W. E. Pakala	East Pittsburgh, Pa.
H. F. Rempt	Burbank, Calif.
R. A. Rugge	Columbus, Ohio
K. R. Smythe	Baltimore, Md.
S. D. Summers	Washington, D. C.
Harold Tanke	Washington, D. C.
S. H. Webster	Burbank, Calif.

AIRCRAFT ELECTRICAL ROTATING MACHINERY SUBCOMMITTEE

L. R. Larson, *chairman;* Naval Research Laboratory, Washington, D. C.

J. W. Allen	Teterboro, N. J.
J. B. Crockford	Lima, Ohio
William Fell	Cleveland, Ohio
Alec Fisher	Lynn, Mass.
S. H. Hanville, Jr.	Washington, D. C.
R. D. Jones	Fort Wayne, Ind.
G. W. Sherman	Dayton Ohio
R. R. Smith	Baltimore, Md.

AIRCRAFT ELECTRICAL SYSTEMS SUBCOMMITTEE

G. A. Phillips, *chairman;* Aviation Division, General Electric Company, Schenectady, N. Y.

J. W. Allen	Teterboro, N. J.
G. W. Almassy	Los Angeles, Calif.
Phil. Binderman	Washington, D. C.
H. B. Bunce	East Pittsburgh, Pa.
L. M. Cobb	Baltimore, Md.
D. W. Exner	Seattle, Wash.
H. L. Hildebrandt	Dayton, Ohio
C. S. Milliken	Burbank, Calif.
W. F. Moore	Schenectady, N. Y.
D. H. Scott	Washington, D. C.
A. J. Snyder	Tulsa, Okla.
A. F. Trumbull	Santa Monica, Calif.

PRINCIPLES OF ALTITUDE RATING OF ELECTRICAL APPARATUS SUBCOMMITTEE

Karl Martinez, *chairman;* Boeing Airplane Company, Plant 1, Seattle, Wash.

J. W. Allen	Teterboro, N. J.
S. H. Hanville, Jr.	Washington, D. C.
V. C. Holloway	Washington, D. C.
J. G. Hutton	Schenectady, N. Y.
C. A. Maple	Dayton, Ohio
R. A. Rugge	Columbus, Ohio

AIRCRAFT ELECTRICAL CONTROL PROTECTIVE DEVICES AND CABLE SUBCOMMITTEE

B. O. Austin, *chairman;* Westinghouse Electrical Corporation, Small Motors Division, Lima, Ohio

Earl Barlow	Baltimore, Md.
E. E. Magee	Dayton, Ohio
R. A. Millermaster	Milwaukee, Wis.
H. S. Moore	New Haven, Conn.
W. F. Moore	Schenectady, N. Y.
J. Ottmar	Attleboro, Mass.
Milton Schach	Washington, D. C.
Omar Wally	Cleveland, Ohio
W. W. West	New York, N. Y.

Domestic and Commercial Applications

Carl F. Scott, *chairman;* General Electric Company, 1285 Boston Avenue, Bridgeport, Conn.

O. K. Coleman, *vice-chairman*	Lafayette, Ind.
V. G. Vaughan, Sr., *secretary*	Attleboro, Mass.
J. C. Beckett	San Francisco, Calif.
M. M. Brandon	New York, N. Y.
A. C. Bredahl	Pittsburgh, Pa.
W. B. Buchanan	Toronto, Ontario, Canada
O. K. Buck	Los Angeles, Calif.
T. H. Cline	Newark, Ohio
T. H. David, Jr.	Chicago, Ill.
M. D. Duffy	Portland, Oreg.
R. W. Fleming	South Bend, Ind.
R. R. Herrmann	Minneapolis, Minn.
T. C. Johnson	Bloomfield, N. J.
F. W. Linder	La Crosse, Wis.
B. H. Martin	Chattanooga, Tenn.

(*Continued on page 1418*)

L. W. McCullough New York, N. Y.
W. G. Meese Detroit, Mich.
B. F. Parr Mansfield, Ohio
A. L. Riche Freeport, Ill.
L. N. Roberson Seattle, Wash.
H. P. Seelye Detroit, Mich.
G. S. Smith Seattle, Wash.
G. C. Tenney San Francisco, Calif.
Wesley Weinerth Philadelphia, Pa.
No subcommittees

Land Transportation

H. F. Brown, *chairman;* New York, New Haven and Hartford Railroad, Railroad Station, New Haven 6, Conn.
L. W. Birch, *vice-chairman* Mansfield, Ohio
R. L. Kimball, *secretary* New York, N. Y.
J. C. Aydelott Erie, Pa.
I. S. Billau Baltimore, Md.
H. R. Blomquist Providence, R. I.
W. A. Brecht East Pittsburgh, Pa.
D. M. Burckett Boston, Mass.
C. N. Chase Coney Island, N. Y.
Andrew Costic Hoboken, N. J.
Llewellyn Evans Chattanooga, Tenn.
J. C. Fox Princeton, W. Va.
A. P. Frey Baltimore, Md.
K. H. Gordon Philadelphia, Pa.
W. S. H. Hamilton New York, N. Y.
P. H. Hatch New Haven, Conn.
G. L. Hoard Seattle, Wash.
J. G. Inglis Toronto, Ontario, Canada
Charles Kerr, Jr. East Pittsburgh, Pa.
Paul Lebenbaum San Francisco, Calif.
S. B. Lent Boston, Mass.
T. M. C. Martin Portland, Oreg.
P. A. McGee New York, N. Y.
E. B. Meissner St. Louis, Mo.
S. R. Negley Philadelphia, Pa.
I. S. Nippes Ridgway, Pa.
A. G. Ochler New York, N. Y.
W. R. Simmons Montreal, Quebec, Canada
D. L. Smith Highwood, Ill.
Jacob Stair, Jr. Philadelphia, Pa.
M. C. Swanson Schenectady, N. Y.
R. G. Thring Washington, D. C.
J. P. Tretton Indianapolis, Ind.
R. S. Warren Elkhart, Ind.
R. A. Williamson Erie, Pa.
G. M. Woods East Pittsburgh, Pa.
H. P. Wright Baltimore, Md.
Lawrence Wylie Seattle, Wash.

Heavy Traction Electrification Data Subcommittee

L. W. Birch, *chairman;* Ohio Brass Company, Mansfield, Ohio
H. F. Brown New Haven, Conn.
A. G. Oehler New York, N. Y.
R. A. Williamson Erie, Pa.
G. M. Woods East Pittsburgh, Pa.

Projects:

(*1*). Catenary overhead systems
(*2*). Third rail systems
(*3*). Electric locomotives
(4). Substations
(*5*). Power supply

The personnel of the projects is as follows:

(*1*) and (*2*). L. W. Birch, *chairman*, Ohio Brass Company, Mansfield, Ohio; H. F. Brown, New Haven, Conn.

(*3*), (*4*), and (*5*). A. G. Oehler, New York, N. Y.; R. A. Williamson, Erie, Pa.; G. M. Woods, East Pittsburgh, Pa.

Heavy Traction Papers and Plans Subcommittee

H. F. Brown, *chairman;* New York, New Haven, and Hartford Railroad, Railroad Station, New Haven, Conn.
A. L. Brecht East Pittsburgh, Pa.
R. L. Kimball New York, N. Y.

Light Traction Papers and Plans Subcommittee

G. M. Woods, *chairman;* Industry Engineering Department, Westinghouse Electric Corporation, East Pittsburgh, Pa.
H. R. Blomquist Providence, R. I.
D. L. Smith Highwood, Ill.

Subcommittee on Revision of AIEE Standard Number 16 (Electric Railway Control Apparatus) ASA C48

(*Continued in next column*)

R. A. Williamson, *chairman;* Railroad Rolling Stock Division of Locomotive and Car Equipments Divisions, General Electric Company, Erie, Pa.
W. S. H. Hamilton New York, N. Y.
P. H. Hatch New Haven, Conn.
J. G. Inglis Toronto, Ontario, Canada
G. M. Woods East Pittsburgh, Pa.

Marine Transportation

Oscar A. Wilde, *chairman;* Sun Shipbuilding and Dry Dock Company, Chester, Pa.
E. C. Alger, *vice-chairman* Quincy, Mass.
W. N. Zippler, *secretary* New York, N. Y.
W. B. Armstrong Washington, D. C.
R. A. Beekman Schenectady, N. Y.
H. C. Coleman East Pittsburgh, Pa.
P. J. DuMont New York, N. Y.
J. B. Feder Washington, D. C.
A. R. Gatewood New York, N. Y.
L. M. Goldsmith Philadelphia, Pa.
P. A. Guise New York, N. Y.
H. F. Harvey, Jr. Newport News, Va.
W. E. Jacobsen Schenectady, N. Y.
J. E. Jones Milwaukee, Wis.
Cornelius Krommenhock Brooklyn, N. Y.
Clarence Lynn East Pittsburgh, Pa.
V. W. Mayer Washington, D. C.
S. N. Mead Amesbury, Mass.
W. H. Reed New York, N. Y.
E. M. Rothen New York, N. Y.
L. B. Sinclair Sparrows Point, Md.
C. W. Souder Great Neck, N. Y.
E. H. Stivender Milwaukee, Wis.
G. O. Watson London, England

Power Generator Subcommittee

Clarence Lynn, *chairman;* Westinghouse Electric Corporation, East Pittsburgh, Pa.
W. B. Armstrong Washington, D. C.
R. A. Beekman Schenectady, N. Y.
H. C. Coleman East Pittsburgh, Pa.
L. M. Goldsmith Philadelphia, Pa.
W. E. Jacobsen Schenectady, N. Y.
E. H. Stivender Milwaukee, Wis.
O. A. Wilde Chester, Pa.

Power Application Subcommittee

L. M. Goldsmith, *chairman;* Atlantic Refining Company, 260 South Broad Street, Philadelphia, Pa.
W. B. Armstrong Washington, D. C.
H. C. Coleman East Pittsburgh, Pa.
W. E. Jacobsen Schenectady, N. Y.
J. E. Jones Milwaukee, Wis.
Clarence Lynn East Pittsburgh, Pa.
E. H. Stivender Milwaukee, Wis.

Wires and Cables Subcommittee

W. N. Zippler, *chairman;* Gibbs and Cox, Inc., 1 Broadway, New York 4, N. Y.
P. J. DuMont New York, N. Y.
J. B. Feder Washington, D. C.
A. R. Gatewood New York, N. Y.
W. E. Jacobsen Schenectady, N. Y.
V. W. Mayer Washington, D. C.
W. H. Reed New York, N. Y.

Switches and Control Subcommittee

H. C. Coleman, *chairman;* Westinghouse Electric Corporation, East Pittsburgh, Pa.
R. A. Beekman Schenectady, N. Y.
P. J. DuMont New York, N. Y.
H. F. Harvey, Jr. Newport News, Va.
J. E. Jones Milwaukee, Wis.
C. Krommenhock Brooklyn, N. Y.
L. B. Sinclair Sparrows Point, Md.
E. H. Stivender Milwaukee, Wis.

Distribution Subcommittee

H. F. Harvey, Jr., *chairman;* Newport News Shipbuilding and Dry Dock Company, Newport News, Va.
W. B. Armstrong Washington, D. C.
A. R. Gatewood New York, N. Y.
L. M. Goldsmith Philadelphia, Pa.
P. A. Guise North Bergen, N. J.
V. W. Mayer Washington, D. C.
W. N. Zippler New York, N. Y.

Fittings and Appliances Subcommittee

J. B. Feder, *chairman;* United States Coast Guard, Merchant Marine Technical Division, 1300 E. Street, N. W., Washington 25, D. C.
E. C. Alger Quincy, Mass.

(*Continued in next column*)

P. J. DuMont New York, N. Y.
P. A. Guise North Bergen, N. J.
H. F. Harvey, Jr. Newport News, Va.
V. W. Mayer Washington, D. C.
E. M. Rothen New York, N. Y.
L. B. Sinclair Sparrows Point, Md.
O. A. Wilde Chester, Pa.

Communications and Alarm Subcommittee

W. H. Reed, *chairman;* Bruce Electric Company, 196 West Houston Street, New York 14, N. Y.
E. C. Alger Quincy, Mass.
J. B. Feder Washington, D. C.
A. R. Gatewood New York, N. Y.
C. Krommenhock Brooklyn, N. Y.
S. N. Mead Amesbury, Mass.
E. M. Rothen New York, N. Y.
L. B. Sinclair Sparrows Point, Md.
C. W. Souder Great Neck, N. Y.

Navigation Equipment Subcommittee

S. N. Mead, *chairman;* Henschel Corporation, 14 Cedar Street, Amesbury, Mass.
E. C. Alger Quincy, Mass.
P. A. Guise New York, N. Y.
C. Krommenhock Brooklyn, N. Y.
E. M. Rothen New York, N. Y.
C. W. Souder Great Neck, N. Y.

Publicity, Personnel, and History Subcommittee

R. A. Beekman, *chairman;* General Electric Company, Federal and Marine Department, Schenectady, N. Y.
H. F. Harvey, Jr. Newport News, Va.
W. H. Reed New York, N. Y.

Editing Subcommittee

W. N. Zippler, *chairman;* Gibbs and Cox, Inc., 1 Broadway, New York, N. Y.
J. B. Feder Washington, D. C.
H. F. Harvey, Jr. Newport News, Va.
W. H. Reed New York, N. Y.

Production and Application of Light

E. H. Salter, *chairman;* Electrical Testing Laboratories, Inc., 2 East End Avenue, New York 21, N. Y.
R. C. Putnam, *vice-chairman* Cleveland, Ohio
R. L. Oetting, *secretary* Cleveland, Ohio
J. F. Angier Washington, D. C.
L. H. Brown Stanford, Calif.
S. W. Bruun New York, N. Y.
H. S. Bull Ann Arbor, Mich.
M. J. Dring South Bend, Ind.
Frank Hansen Los Angeles, Calif.
C. P. Hayes Fort Wayne, Ind.
S. G. Hibben Bloomfield, N. J.
O. W. Holden Los Angeles, Calif.
R. W. Loehr Cleveland, Ohio
H. E. Mahan West Lynn, Mass.
J. A. McDermott Corona, N. Y.
F. H. Murphy Portland, Oreg.
Harris Reinhardt Flushing, N. Y.
D. W. Rowten Cleveland, Ohio
R. G. Slauer Salem, Mass.
E. M. Strong Ithaca, N. Y.
H. F. Wall Detroit, Mich.
C. C. Whipple Brooklyn, N. Y.

Ultraviolet Radiations Subcommittee

Allen J. Dusalt, *chairman;* Westinghouse Lamp Division, Bloomfield, N. J.
W. T. Anderson, Jr. Newark, N. J.
H. W. Scott Cleveland, Ohio

Nomenclature of Electronic Lamps Subcommittee

R. C. Putnam, *chairman;* Case Institute of Technology, Cleveland, Ohio
G. S. Evans Bloomfield, N. J.
L. R. Keiffer Cleveland, Ohio
T. C. Sargent Salem, Mass.

Infrared Radiation Subcommittee

E. A. Linsday, *chairman;* General Electric Company, Nela Park, Cleveland 12, Ohio
George Parmallee Cleveland, Ohio

Industry Group

Industry Co-ordinating Committee

J. J. Orr, *chairman;* United States Rubber Company, 1230 Avenue of the Americas, New York 20, N. Y.
T. R. Rhea, *vice-chairman* Schenectady, N. Y.
R. S. Gardner, *secretary* New York, N. Y.

Member-at-Large

Victor Siegfried, representing Board of Directors Worcester, Mass.

Ex-officio members

Chairmen of the technical committees in the Industry Co-ordinating Group

Chemical, Electrochemical, and Electrothermal Applications

F. R. Benedict, *chairman;* Industry Engineering Department, Westinghouse Electric Corporation, East Pittsburgh, Pa.
L. W. Roush, *vice-chairman* South Charleston, W. Va.
F. V. Andreae Chattanooga, Tenn.
F. T. Bowditch Cleveland, Ohio
J. D. Brance Houston, Tex.
A. B. Campbell Houston, Tex.
W. H. Dickinson Elizabeth, N. J.
W. C. Dreyer Houston, Tex.
Felix S. Glaza Freeport, Tex.
L. M. Goldsmith Philadelphia, Pa.
W. E. Gutzwiller Milwaukee, Wis.
E. A. Hanff Pittsburgh, Pa.
J. D. Harper Alcoa, Tenn.
T. G. Hieronymus Kansas City, Mo.
J. E. Hobson Stanford, Calif.
H. E. Houck Wilmington, Del.
J. D. Huntsberger Trenton, N. J.
M. A. Hyde, Jr. East Pittsburgh, Pa.
Rollin Kennard Great Falls, Mont.
R. J. Kuhn New Orleans, La.
E. H. Lamberger East Pittsburgh, Pa.
D. H. Levy Dallas, Tex.
J. J. Loustaunau Boston, Mass.
J. A. McLellan Linden, N. J.
G. H. Orcutt Wyandotte, Mich.
Waldo Porter Vancouver, Wash.
R. L. Rayner Detroit, Mich.
T. R. Rhea Schenectady, N. Y.
H. C. Riggs Philadelphia, Pa.
G. B. Scheer Oakland, Calif.
E. H. Scheick New York, N. Y.
H. P. Sherer Cleveland, Ohio
E. O. Scott Welland, Ontario, Canada
Joel Tompkins Massena, N. Y.
J. J. Unger Cleveland, Ohio
G. W. Vinal Washington, D. C.
H. W. Wahlquist New York, N. Y.
H. H. Zielinski Schenectady, N. Y.

Arc Furnace and Electrothermal Processes Subcommittee

E. H. Browning, Jr., *chairman;* Westinghouse Electric Corporation, East Pittsburgh, Pa.
F. V. Andreae Chattanooga, Tenn.
W. L. Bundy Wilson Dam, Ala.
R. A. Geiselman East Pittsburgh, Pa.
E. A. Hanff Pittsburgh, Pa.
A. R. Oltrogge Schenectady, N. Y.
E. Scheick New York, N. Y.
H. H. Zielinski Schenectady, N. Y.

Cathodic Protection Subcommittee

A. E. Archambault New York, N. Y.
J. D. Brance Houston, Tex.
A. S. Brooks Newark, N. J.
Guy Corfield Los Angeles, Calif.
G. C. Cox Charleston, W. Va.
I. A. Dennison Washington, D. C.
L. J. Gorman New York, N. Y.
T. G. Hieronymus Kansas City, Mo.
R. J. Kuhn New Orleans, La.
D. H. Levy Dallas, Tex.
R. L. Rayner Detroit, Mich.
H. W. Wahlquist New York, N. Y.

Electrolytic Processes Subcommittee

W. E. Gutzwiller, *chairman;* Allis-Chalmers Manufacturing Company, Milwaukee, Wis.

(*Continued on page 1419*)

Felix Glaza Freeport, Tex.
J. D. Harper Alcoa, Tenn.
H. E. Houck Wilmington, Del.
F. L. Kaestle Schenectady, N. Y.
Rollin Kennard Great Falls, Mont.
F. L. Lawton Montreal, Quebec, Canada
G. H. Orcutt Wyandotte, Mich.
Waldo Porter Vancouver, Wash.
G. B. Scheer Oakland, Calif.
Joel Tompkins Massena, N. Y.

Petroleum Refining and Production Subcommittee

Lester Goldsmith, *chairman;* Atlantic Refining Company, 260 South Broad Street, Philadelphia 1, Pa.
H. J. Appel New York, N. Y.
W. H. Dickinson Elizabeth, N. J.
J. B. Glasby Philadelphia, Pa.
E. L. Hoyle East Chicago, Ind.
D. H. Levy Dallas, Tex.
E. L. Nopper Philadelphia, Pa.

Storage Batteries Subcommittee

H. C. Riggs, *chairman;* Electric Storage Battery Company, 19th and Allegheny Avenue, Philadelphia 32, Pa.
J. D. Huntsberger Trenton, N. J.
L. A. Murray Providence, R. I.
E. T. Rummel Cleveland, Ohio
J. J. Unger Cleveland, Ohio
K. A. Vaughn Depew, N. Y.
H. H. Zielinski Schenectady, N. Y.

Chemical Industries Subcommittee

L. W. Roush, *chairman;* Carbide and Carbon Chemicals Corporation, South Charleston, W. Va.
Felix Glaza Freeport, Tex.
E. M. Hansford Montgomery, W. Va.
J. A. Horacek Fairport Harbor, Ohio
H. E. Houck Wilmington, Del.
M. A. Hyde East Pittsburgh, Pa.
S. J. Rosch Hastings-on-Hudson, N. Y.

Electric Heating

J. C. Handy, *chairman;* Consolidated Gas, Electric Light and Power Company, Lexington Building, Baltimore, Md.
P. H. Goodell, *vice-chairman* Detroit, Mich.
L. M. Duryee, *secretary* Waterbury, Conn.
E. L. Bailey Highland Park, Mich.
R. M. Baker Baltimore, Md.
E. H. Browning East Pittsburgh, Pa.
Harold Bunte Chicago, Ill.
F. T. Chestnut Trenton, N. J.
R. H. Ellis Pittsburgh, Pa.
E. M. Guyer Corning, N. Y.
L. P. Hynes Haddonfield, N. J.
T. P. Kinn Baltimore, Md.
G. F. Leland Schenectady, N. Y.
W. H. Meyer Toronto, Ontario, Canada
A. E. Opdenweyer Portland, Oreg.
C. E. Peck Meadville, Pa.
Kennard Pinder Wilmington, Del.
W. C. Rudd Brooklyn, N. Y.
E. H. Scheick New York, N. Y.
E. A. Schmotzer Cleveland, Ohio
W. R. Schofield Philadelphia, Pa.
G. W. Scott, Jr. Lancaster, Pa.
G. S. Smith Seattle, Wash.
T. H. Story Camden, N. J.
J. T. Thwaites Hamilton, Ontario, Canada
G. E. Walters Peoria, Ill.
H. R. Winemiller Chicago, Ill.

Induction and Dielectric Heating Subcommittee

W. C. Rudd, *chairman;* Induction Heating Corporation, 181 Wythe Avenue, Brooklyn 11, N. Y.
W. R. G. Baker Syracuse, N. Y.
G. P. Bosomworth Akron, Ohio
E. W. Chapin Laurel, Md.
F. T. Chesnut Trenton, N. J.
H. E. Dinger Washington, D. C.
L. M. Duryee Waterbury, Conn.
J. E. Eiselein Camden, N. J.
C. W. Frick Schenectady, N. Y.
H. C. Gillespie Camden, N. J.
W. H. Hickok Louisville, Ky.
R. J. Hunn Wilmington, Del.
T. R. Kennedy Trenton, N. J.
T. P. Kinn Baltimore, Md.
W. H. Meyer Toronto, Ontario, Canada

(*Continued in next column*)

L. R. Milburn Detroit, Mich.
Eugene Mittleman Chicago, Ill.
G. W. Scott, Jr. Lancaster, Pa.
J. T. Thwaites Hamilton, Ontario, Canada
R. L. Uphoff Bound Brook, N. J.
G. E. Walters Peoria, Ill.
D. E. Watts Syracuse, N. Y.
Otto Weitmann New York, N. Y.
H. R. Winemiller Chicago, Ill.
A. C. Wooldridge Newark, N. J.

Radiant Heating Subcommittee

Paul H. Goodell, *chairman;* Jensen Specialties, Inc., 9331 Freeland Avenue, Detroit 28, Mich.
C. E. Russell, *secretary* Philadelphia, Pa.
I. J. Barber Fostoria, Ohio
E. J. Bates Dayton, Ohio
Earl Benson Salem, Mass.
P. O. Blackmore Newark, N. J.
A. H. Canada Schenectady, N. Y.
C. A. Casey Charlottesville, Va.
H. J. Garber Knoxville, Tenn.
M. M. Greer Pittsburgh, Pa.
S. G. Hibben Bloomfield, N. J.
W. F. Hickes Foxboro, Mass.
J. E. Johanson St. Louis, Mo.
E. A. Linsday Cleveland, Ohio
R. C. Machler Philadelphia, Pa.
A. D. Moore Ann Arbor, Mich.
C. T. Prendergast Chicago, Ill.
C. L. Raynor Detroit, Mich.
D. Y. Robinson Chicago, Ill.
Lawrence Stout St. Louis, Mo.
J. E. Sump Detroit, Mich.
W. H. Wannamaker Philadelphia, Pa.

Subcommittee on Radiation Measurements Above 200 Megacycles

R. M. Baker, *chairman;* Westinghouse Electric Corporation, 3601 Washington Boulevard, Baltimore, Md.
C. W. Frick, *secretary* Schenectady, N. Y.
H. H. Beizer Bradley Beach, N. J.
Marvin Bock Waltham, Mass.
R. E. Burgess Washington, D. C.
E. W. Chapin Laurel, Md.
H. E. Dinger Washington, D. C.
J. E. Eiselein Camden, N. J.
R. M. Showers Philadelphia, Pa.
H. E. Sorrows Washington, D. C.
A. J. Vadasz Syracuse, N. Y.

Resistance Heating and Electric Furnace Subcommittee

J. C. Handy, *chairman;* Consolidated Gas, Electric Light and Power Company, Lexington Building, Baltimore 3, Md.
M. M. Greer, *secretary* Pittsburgh, Pa.
F. E. Bash Harrison, N. J.
Harold Bunte Chicago, Ill.
A. G. Hotchkiss Schenectady, N. Y.
F. J. Jolly Detroit, Mich.
C. E. Peck Meadville, Pa.
Kennard Pinder Wilmington, Del.
D. Y. Robinson Chicago, Ill.
E. H. Scheick Perth Amboy, N. J.

Electric Welding

Myron Zucker, *chairman;* Myron Zucker Engineering Company, 1708 Crooks Road, Royal Oak, Mich.
R. C. McMaster, *vice-chairman* Columbus, Ohio
R. S. Gardner, *secretary* New York, N. Y.
J. H. Blankenbuehler Troy, Ohio
W. G. Bostwick Cincinnati, Ohio
G. M. Chute Detroit, Mich.
C. N. Clark Pittsburgh, Pa.
J. F. Deffenbaugh Warren, Ohio
D. D. Douglass Oakland, Calif.
R. C. Freeman Fitchburg, Mass.
J. W. Gore Baltimore, Md.
E. M. Guyer Corning, N. Y.
W. B. Kouwenhoven Baltimore, Md.
E. J. Limpel Milwaukee, Wis.
S. W. Luther Detroit, Mich.
R. S. Phair Philadelphia, Pa.
C. M. Rhoades, Jr. Schenectady, N. Y.
M. S. Shane Cleveland, Ohio
C. E. Smith Warren, Ohio
H. W. Snyder Bozeman, Mont.
C. B. Stadum Buffalo, N. Y.
H. I. Stanback Milwaukee, Wis.
E. F. Steinert Buffalo, N. Y.
G. I. F. Theriault Dayton, Ohio
H. W. Tietze Newark, N. J.

(*Continued in next column*)

L. P. Winsor Troy, N. Y.
B. L. Wise Bay City, Mich.

Power Supply for Resistance Welding Machines Subcommittee

C. N. Clark, *chairman;* Duquesne Light Company, 435 Sixth Avenue, Pittsburgh, Pa.
N. A. Kieb, *vice-chairman;* Detroit, Mich.
W. K. Boice Schenectady, N. Y.
W. G. Bostwick Cincinnati, Ohio
W. W. Brumby St. Catharines, Ontario, Canada
J. F. Deffenbaugh Warren, Ohio
E. F. Dissmeyer Jackson, Mich.
S. W. Luther Detroit, Mich.
R. S. Phair Philadelphia, Pa.
C. E. Smith Warren, Ohio
J. L. Solomon Chicago, Ill.
C. B. Stadum Buffalo, N. Y.
H. W. Tietze Newark, N. J.
Myron Zucker Royal Oak, Mich.

General Industry Applications

Kennard Pinder, *chairman;* E. I. du Pont de Nemours and Company, 10th and Market Streets, Wilmington, Del.
J. C. Fink, *vice-chairman* East Pittsburgh, Pa.
V. O. Johnson, *secretary* New York, N. Y.
B. T. Anderson Rockford, Ill.
A. T. Bacheler East Pittsburgh, Pa.
V. B. Baker East Pittsburgh, Pa.
E. L. Blankenbeker Pittsburgh, Pa.
S. A. Bobe Atlanta, Ga.
M. A. de Ferranti Schenectady, N. Y.
J. M. Delfs Schenectady, N. Y.
R. J. Demartini Schenectady, N. Y.
A. W. Frankenfield Wilmington, Del.
John Gammell Philadelphia, Pa.
E. M. Hays Pittsburgh, Pa.
L. W. Herchenroeder East Pittsburgh, Pa.
K. W. John Detroit, Mich.
G. W. Knapp Schenectady, N. Y.
A. F. Lukens Lynn, Mass.
H. B. Mathieu Pampa, Tex.
E. F. Mekelburg Milwaukee, Wis.
J. F. Moore Toronto, Ontario, Canada
E. K. Murphy New York, N. Y.
D. R. Percival Worcester, Mass.
G. E. Plaisted San Francisco, Calif.
N. L. Platt Binghamton, N. Y.
H. A. Rose Seattle, Wash.
R. G. Rudrow Wilmington, Del.
H. L. Smith New York, N. Y.
R. H. Smith Cleveland, Ohio
F. D. Snyder Boston, Mass.
R. F. Snyder Akron, Ohio
Ray Stroppel Cincinnati, Ohio
J. A. Tudor Portland, Oreg.
H. C. Uhl Sylacauga, Ala.
L. A. Umansky Schenectady, N. Y.

Machine Tools Subcommittee

D. R. Percival, *chairman;* Machinery Electrification, Inc., 98 Union St., Worcester 8, Mass.
B. T. Anderson Rockford, Ill.
R. W. Brown Windsor, Vt.
J. M. Delfs Schenectady, N. Y.
R. T. Fenn Springfield, Vt.
L. W. Herchenroeder East Pittsburgh, Pa.
J. J. Jaeger West Hartford, Conn.
E. F. Mekelburg Milwaukee, Wis.
W. J. Piper Springfield, Vt.
S. Rice Worcester, Mass.
E. J. Rivoira Cincinnati, Ohio
R. E. Stroppel Cincinnati, Ohio
W. B. Wigton Cincinnati, Ohio

Materials Handling Subcommittee

M. A. de Ferranti, *chairman;* Industrial Engineering Division, General Electric Company, Schenectady 5, N. Y.
E. L. Blankenbeker, *secretary* Pittsburgh, Pa.
F. W. Atz Philadelphia, Pa.
S. P. Blakeman Philadelphia, Pa.
E. O. Dunham Battle Creek, Mich.
D. C. Gray St. Paul, Minn.
E. M. Hays Pittsburgh, Pa.
J. R. Kilander Schenectady, N. Y.
C. B. Rissler East Pittsburgh, Pa.
George Thompson Memphis, Tenn.
H. W. Williams Jersey City, N. J.
G. W. Yanney Alliance, Ohio

Pulp and Paper Industry Subcommittee

V. B. Baker, *chairman;* Industry Engineering Department, Westinghouse Electric Corporation, East Pittsburgh, Pa.
D. C. Christison, *secretary* Neenah, Wis.
H. B. Barrow Atlanta, Ga.
G. C. Ehemann Rittman, Ohio
O. O. Fisk Cumberland Mills, Maine
G. W. Knapp Schenectady, N. Y.
E. K. Murphy New York, N. Y.
H. A. Rose Seattle, Wash.
J. L. Van Nort Cleveland, Ohio

Rubber and Plastics Industry Subcommittee

R. F. Snyder, *chairman;* Goodyear Tire and Rubber Company, 1144 East Market Street, Akron, Ohio
A. T. Bacheler, *secretary* East Pittsburgh, Pa.
E. J. Dalton Schenectady, N. Y.
John Gammell Philadelphia, Pa.
F. A. Green Akron, Ohio
K. W. John Detroit, Mich.
V. O. Johnson New York, N. Y.
G. V. Kullgren Akron, Ohio
J. F. Moore Toronto, Ontario, Canada
B. D. Morgan New Brunswick, N. J.
W. L. Ringland Milwaukee, Wis.
C. E. Robinson Cleveland, Ohio
R. G. Rudrow Wilmington, Del.
H. J. Secrest Akron, Ohio
R. E. Seddon Wilmington, Del.
H. L. Smith New York, N. Y.

Textile Industry Subcommittee

H. C. Uhl, *chairman;* Avondale Mills, Sylacauga, Ala.
R. J. Demartini, *secretary* Schenectady, N. Y.
A. T. Bacheler East Pittsburgh, Pa.
S. A. Bobe Atlanta, Ga.
I. S. Bull Greenville, S. C.
S. O. Cowan Charlotte, N. C.
W. E. Dent, Jr. Lancaster, S. C.
R. B. Flowers Atlanta, Ga.
A. W. Frankenfield Wilmington, Del.
H. Haley Philadelphia, Pa.
C. F. Hedlung Boston, Mass.
L. T. Jester Boston, Mass.
A. P. Lewis New Bedford, Mass.
A. F. Lukens Lynn, Mass.
Dan McConnell Greensboro, N. C.
Victor Sepavich Worcester, Mass.
F. D. Snyder Boston, Mass.

Industrial Control

G. W. Heumann, *chairman;* Control Engineering Division, General Electric Company, Schenectady 5, N. Y.
J. A. Cortelli, *vice-chairman* Cleveland, Ohio
H. L. Palmer, *secretary* Schenectady, N. Y.
E. H. Alexander Schenectady, N. Y.
H. H. Angel Bethlehem, Pa.
John Baird Madison, Wis.
T. H. Bloodworth Milwaukee, Wis.
G. S. Brown Cambridge, Mass.
L. J. Conover Easton, Pa.
W. H. Elliot Milwaukee, Wis.
C. T. Evans Milwaukee, Wis.
J. E. Gahagan Binghamton, N. Y.
J. A. Gienger Rochester, N. Y.
F. Y. Grepe Toronto, Ontario, Canada
K. W. John Detroit, Mich.
R. W. Jones Evanston, Ill.
J. D. Leitch Cleveland, Ohio
E. F. Mekelburg Milwaukee, Wis.
E. E. Moyer Troy, N. Y.
J. J. O'Connor New York, N. Y.
J. W. Picking Cleveland, Ohio
J. E. Ryan Schenectady, N. Y.
F. E. Strauss Oakland, Calif.
E. H. Vedder Buffalo, N. Y.

Bibliography Subcommittee

R. W. Jones, *chairman;* Department of Electrical Engineering, Northwestern University, Evanston, Ill.
H. H. Angel Bethlehem, Pa.
L. J. Conover Easton, Pa.
J. A. Cortelli Cleveland, Ohio
C. T. Evans Milwaukee, Wis.
D. K. Frost Rockford, Ill.
J. E. Gahagan Binghamton, N. Y.
J. A. Gienger Rochester, N. Y.
J. D. Leitch Cleveland, Ohio

(*Continued on page 1420*)

L. H. Matthias	Milwaukee, Wis.
E. F. Mekelburg	Milwaukee, Wis.
E. E. Moyer	Troy, N. Y.
E. H. Vedder	Buffalo, N. Y.

Electronic Control Subcommittee

H. L. Palmer, *chairman;* General Electric Company, 1 River Road, Schenectady 5, N. Y.

F. T. Bailey	East Pittsburgh, Pa.
H. B. Black	Ampere, N. J.
S. L. Burgwin	East Pittsburgh, Pa.
C. J. Collom	Detroit, Mich.
E. T. Davis	Philadelphia, Pa.
L. H. Dee	Schenectady, N. Y.
W. H. Elliot	Milwaukee, Wis.
William Few	Cleveland, Ohio
E. W. Hutton	Schenectady, N. Y.
L. U. C. Kelling	Schenectady, N. Y.
O. W. Livingston	Schenectady, N. Y.
E. B. McDowell	Schenectady, N. Y.
F. O. Nottingham	Lafayette, Ind.
J. W. Picking	Cleveland, Ohio
V. J. Porter	Philadelphia, Pa.
K. P. Puchlowski	Pittsburgh, Pa.
W. G. Roman	East Pittsburgh, Pa.
C. A. Schurr	Cleveland, Ohio
W. W. Snyder	Kew Gardens, N. Y.
H. I. Stanback	Milwaukee, Wis.
B. B. Stuart	Chicago, Ill.
C. M. Summers	Fort Wayne, Ind.
B. F. Tellkamp	Milwaukee, Wis.

Working Groups:

(1). Electronic Control of D-C Motors

J. W. Picking, *chairman;* Reliance Electric and Engineering Company, 1068–1088 Ivanhoe Road, Cleveland 10, Ohio

H. B. Black	Ampere, N. J.
C. J. Collom	Detroit, Mich.
L. H. Dee	Schenectady, N. Y.
W. H. Elliot	Milwaukee, Wis.
William Few	Cleveland, Ohio
W. G. Roman	East Pittsburgh, Pa.
H. I. Stanback	Milwaukee, Wis.
B. B. Stuart	Chicago, Ill.
B. F. Tellkamp	Milwaukee, Wis.

(2). Photoelectric Pickup Systems

L. U. C. Kelling, *chairman;* General Electric Company, 1 River Road, Schenectady 5, N. Y.

F. T. Bailey	East Pittsburgh, Pa.
H. A. Lindsay	Charleston, W. Va.
E. M. Pritchard	Boston, Mass.
L. L. Worner	Rankin, Ill.

(3). Electronic Relaying Devices

E. B. McDowell, *chairman;* General Electric Company, 1 River Road, Schenectady 5, N. Y.

C. A. Schurr	Cleveland, Ohio
B. F. Tellkamp	Milwaukee, Wis.

(4). Electronic Regulators and Regulating Systems

O. W. Livingston, *chairman;* General Electric Company, 1 River Road, Schenectady 5, N. Y.

H. B. Black	Ampere, N. J.
S. L. Burgwin	East Pittsburgh, Pa.
E. T. Davis	Philadelphia, Pa.
W. H. Elliot	Milwaukee, Wis.
William Few	Cleveland, Ohio
J. W. Picking	Cleveland, Ohio
W. W. Snyder	Kew Gardens, N. Y.

(5). Electronic Control of A-C Power

E. W. Hutton, *chairman;* General Electric Company, 1 River Road, Schenectady 5, N. Y.

H. B. Black	Ampere, N. J.
W. H. Elliot	Milwaukee, Wis.
W. G. Roman	East Pittsburgh, Pa.
B. F. Tellkamp	Milwaukee, Wis.

Standards Subcommittee

G. W. Heumann, *chairman;* General Electric Company, Control Engineering Division, Schenectady 5, N. Y.

Test Codes Subcommittee

C. T. Evans, *chairman;* Cutler-Hammer, Inc., 315 North 12th Street, Milwaukee, Wis.

D. K. Frost	Rockford, Ill.
B. W. Jones	Schenectady, N. Y.
H. E. Nason	Chicago, Ill.
W. E. Pakala	East Pittsburgh, Pa.
J. E. Ryan	Schenectady, N. Y.
C. A. Schaefer	Milwaukee, Wis.

Industrial Power Systems

J. S. Gault, *chairman;* Electrical Engineering Department, University of Michigan, Ann Arbor, Mich.

H. G. Barnett, *vice-chairman* East Pittsburgh, Pa.

S. A. Warner, *secretary* New York, N. Y.

D. L. Beeman	Schenectady, N. Y.
E. S. Dibble	Los Angeles, Calif.
Leo Dolkart	Chicago, Ill.
D. D. Douglass	Oakland, Calif.
Felix Glaza	Freeport, Tex.
W. S. Gordon, Jr.	Tacoma, Wash.
C. R. Johnson	New York, N. Y.
R. E. Johnson	Rockford, Ill.
C. M. Lathrop	Elizabeth, N. J.
M. C. Mapes	New Haven, Conn.
A. E. Marshall	St. Louis, Mo.
J. L. McKeever	Peterboro, Ontario, Canada
J. S. Morgan	Milwaukee, Wis.
W. A. Mosteller	Detroit, Mich.
J. S. Parsons	East Pittsburgh, Pa.
Dick Ray	Tulsa, Okla.
W. H. Rollman	Dallas, Tex.
C. C. Saunders	Wilmington, Del.
C. W. Schemm	St. Louis, Mo.
J. M. Smith	Cleveland, Ohio
J. F. Spease	Portland, Oreg.
W. D. Stevenson, Jr.	Raleigh, N. C.
T. O. Sweatt	New York, N. Y.
H. T. Theis	Bristol, Pa.
B. F. Thomas	New York, N. Y.
William Zelley	Roebling, N. J.
T. O. Zittel	Lackawanna, N. Y.

Subcommittee on Revision of Electric Power Distribution for Industrial Plants

John Grotzinger, *chairman;* Goodyear Tire and Rubber Company, 1144 East Market Street, Akron, Ohio

J. A. Arberry	Pittsburgh, Pa.
H. G. Barnett	East Pittsburgh, Pa.
Y. T. Chaney	Plainville, Conn.
William Deans	Philadelphia, Pa.
L. E. Fisher	Detroit, Mich.
N. B. Lainge	Detroit, Mich.
S. W. Luther	Detroit, Mich.
R. B. McKinley	Schenectady, N. Y.
J. S. Parsons	East Pittsburgh, Pa.
E. E. Turkington	Boston, Mass.
R. C. Waldron	Passaic, N. J.
R. M. Wilson, Jr.	Schenectady, N. Y.
J. A. Yetter	Schenectady, N. Y.

Mining and Metal Industry

T. B. Montgomery, *chairman;* Control Section, Allis-Chalmers Manufacturing Company, West Allis, Wis.

A. C. Muir, *vice-chairman* Philadelphia, Pa.

J. E. Breth	Bessemer, Ala.
Graham Bright	Pittsburgh, Pa.
A. B. Chafetz	Carlsbad, N. Mex.
R. J. Corfield	Garfield, Utah
J. A. Cortelli	Cleveland, Ohio
F. W. Cramer	Pittsburgh, Pa.
E. W. Davis	Cambridge, Mass.
J. M. Downie	Erie, Pa.
J. H. Edwards	Huntington, W. Va.
Sam Ein	Chicago, Ill.
R. H. Ellis	Pittsburgh, Pa.
Cecil Farrow	Cleveland, Ohio
J. J. Fitzgibbon	Charleston, W. Va.
Lanier Greer	Cleveland, Ohio
L. N. Grier	Pittsburgh, Pa.
C. L. Gust	Lead, S. Dak.
W. R. Harris	East Pittsburgh, Pa.
L. H. Harrison	Birmingham, Ala.
G. A. Hornbecker	Waterbury, Conn.
J. G. Ivy	Chicago, Ill.
L. H. James	Scranton, Pa.
J. Z. Linsenmeyer	East Pittsburgh, Pa.
R. C. McMaster	Columbus, Ohio
W. E. Miller	Schenectady, N. Y.
H. P. Musser	Charleston, W. Va.
F. C. Nicholson	Wilkes-Barre, Pa.
B. E. Price	New York, N. Y.
D. E. Renshaw	East Pittsburgh, Pa.
V. E. Schlossberg	East Chicago, Ind.
E. G. Schlup	Middletown, Ohio
F. O. Schnure	Sparrows Point, Md.
Victor Siegfried	Worcester, Mass.
H. I. Stanback	Milwaukee, Wis.
O. J. Swanson	Gary, W. Va.
C. L. Tyler	Irwin, Pa.
T. R. Weichel	Wilkes-Barre, Pa.
C. O. Wood	Chicago, Ill.
C. D. Woodward	Butte, Mont.

(Continued in next column)

Metal Industries Subcommittee

(Chairman not yet selected)

J. A. Cortelli	Cleveland, Ohio
F. W. Cramer	Pittsburgh, Pa.
Sam Ein	Chicago, Ill.
R. H. Ellis	Pittsburgh, Pa.
Cecil Farrow	Cleveland, Ohio
Lanier Greer	Cleveland, Ohio
L. N. Grier	Pittsburgh, Pa.
W. R. Harris	East Pittsburgh, Pa.
G. A. Hornbecker	Waterbury, Conn.
W. E. Miller	Schenectady, N. Y.
B. E. Price	New York, N. Y.
V. E. Schlossberg	East Chicago, Ind.
E. G. Schlup	Middletown, Ohio
F. O. Schnure	Sparrows Point, Md.
Victor Siegfried	Worcester, Mass.
H. I. Stanback	Milwaukee, Wis.

Mining Subcommittee

(Chairman not yet selected)

J. E. Breth	Bessemer, Ala.
Graham Bright	Pittsburgh, Pa.
A. B. Chafetz	Carlsbad, N. Mex.
R. J. Corfield	Garfield, Utah
E. W. Davis	Cambridge, Mass.
J. M. Downie	Erie, Pa.
J. H. Edwards	Huntington, W. Va.
J. J. Fitzgibbon	Charleston, W. Va.
C. L. Gust	Lead, S. Dak.
L. H. Harrison	Birmingham, Ala.
J. G. Ivy	Chicago, Ill.
L. H. James	Scranton, Pa.
G. F. Johnson	Valparaiso, Ind.
J. Z. Linsenmeyer	East Pittsburgh, Pa.
R. C. McMaster	Columbus, Ohio
A. C. Muir	Philadelphia, Pa.
H. P. Musser	Charleston, W. Va.
F. C. Nicholson	Wilkes-Barre, Pa.
D. E. Renshaw	East Pittsburgh, Pa.
O. J. Swanson	Gary, W. Va.
C. L. Tyler	Irwin, Pa.
T. R. Weichel	Wilkes-Barre, Pa.
C. O. Wood	Chicago, Ill.
C. D. Woodward	Butte, Mont.

Feedback Control Systems (Servomechanisms)

G. S. Brown, *chairman;* Massachusetts Institute of Technology, Building 32, Cambridge 39, Mass.

(Exact title, scope, and personnel to be announced later)

Power Group

Power Co-ordinating Committee

L. F. Hickernell, *chairman;* Anaconda Wire and Cable Company, Hastings-on-Hudson 6, N. Y.

W. R. Brownlee, *vice-chairman* Jackson, Mich.

R. S. Gardner, *secretary* New York, N. Y.

Members-at-Large

R. T. Henry, representing Board of Directors	Buffalo, N. Y.
L. F. Kennedy	Schenectady, N. Y.
A. E. Knowlton	New York, N. Y.
C. F. Wagner	East Pittsburgh, Pa.

Ex-officio members

Chairmen of the technical committees in the Power Group

Carrier Current

S. C. Bartlett, *chairman;* American Gas and Electric Service Corporation, 30 Church Street, New York 8, N. Y.

C. W. Boadway, *vice-chairman* Toronto, Ontario, Canada

E. W. Kenefake, *secretary* Syracuse, N. Y.

W. H. Blankmeyer	Butte, Mont.
J. M. Dunham	New York, N. Y.
L. G. Eaton	Boston, Mass.
J. R. Ellison	Oklahoma City, Okla.
R. C. Ericson	Hammond, Ind.
L. F. Ferri	Elyria, Ohio
L. F. Kennedy	Schenectady, N. Y.
S. C. Leyland	Newark, N. J.
Samuel Lubin	Washington, D. C.
L. E. Ludekens	Los Angeles, Calif.
D. M. MacGregor	New York, N. Y.
T. W. MacLean	Spokane, Wash.
R. H. Miller	San Francisco, Calif.
F. M. Rives	Syracuse, N. Y.
T. T. Smith	Vancouver, Wash.
O. A. Starcke	Kansas City, Mo.
B. W. Storer	Chicago, Ill.
H. J. Sutton	Beaumont, Tex.
T. D. Talmage	Chattanooga, Tenn.
Mead Warren, Jr.	Alcoa, Tenn.
M. H. Wood	Baltimore, Md.

(Continued in next column)

Subcommittee for Preparation of a General Interest Paper on Carrier Current

M. H. Wood, *chairman;* Westinghouse Electric Corporation, Electronics and X-Ray Division, 2519 Wilkens Avenue, Baltimore 3, Md.

S. C. Bartlett	New York, N. Y.
J. D. Booth	Baltimore, Md.
L. F. Kennedy	Schenectady, N. Y.

Subcommittee to Report on Application Guidance for Carrier Current

D. M. MacGregor, *chairman;* Ebasco Services, Inc., 2 Rector Street, New York 6, N. Y.

C.W. Boadway	Toronto, Ontario, Canada
T. D. Talmage	Chattanooga, Tenn.

Methods of Measurement Subcommittee

E. W. Kenefake, *chairman;* General Electric Company, Electronics Department, Syracuse, N. Y.

S. C. Bartlett	New York, N. Y.
C.W. Boadway	Toronto, Ontario, Canada
M. H. Wood	Baltimore, Md.

High-Frequency Characteristics of Power Equipment Subcommittee

S. C. Leyland, *chairman;* Westinghouse Electric Corporation, 95 Orange Street, Newark 2, N. J.

T. A. Cramer	Schenectady, N. Y.

Carrier Current Characteristics of Transmission Lines and Systems Subcommittee

R. C. Ericson, *chairman;* Northern Indiana Public Service Company, 5265 Hohman Avenue, Hammond, Ind.

W. H. Blankmeyer	Butte, Mont.
W. J. Googe	Shreveport, La.
L. E. Ludekens	Los Angeles, Calif.
T. D. Talmage	Chattanooga, Tenn.

Standard Terminology for Carrier Current Subcommittee

S. C. Leyland, *chairman;* Westinghouse Electric Corporation, 95 Orange Street, Newark 2, N. J.

Long-Life Tubes Subcommittee

L. G. Eaton, *chairman;* New England Power Service Company, 441 Stuart Street, Boston 16, Mass.

R. W. Beckwith	Syracuse, N. Y.
L. F. Ferri	Elyria, Ohio
Samuel Lubin	Washington, D. C.
M. H. Wood	Baltimore, Md.

Use of Microwave Equipment for Relaying, Telemetering, and Supervisory Control Subcommittee

S. C. Leyland, *chairman;* Westinghouse Electric Corporation, 95 Orange Street, Newark 2, N. J.

Operating Experience With Carrier Current Relaying Channels Subcommittee

O. A. Starcke, *chairman;* Kansas City Power and Light Company, Box 679, Kansas City, Mo.

T. A. Cramer	Schenectady, N. Y.
J. R. Curtin	Portland, Oreg.
W. J. Googe	Shreveport, La.
V. J. Hayes	Waterbury, Conn.
H. W. Lensner	Newark, N. J.
L. E. Ludekens	Los Angeles, Calif.
B. W. Storer	Chicago, Ill.
Mead Warren, Jr.	Alcoa, Tenn.

Insulated Conductors

Herman Halperin, *chairman;* Commonwealth Edison Company, 72 West Adams Street, Chicago 90, Ill.
V. A. Sheals, *vice-chairman* Schenectady, N. Y.
C. T. Hatcher, *secretary* New York, N. Y.
G. W. Acock Trenton, N. J.
C. F. Avila Boston, Mass.
H. G. Barnett East Pittsburgh, Pa.
C. E. Baugh San Francisco, Calif.
E. D. Bent Montreal, Quebec, Canada
Andrew Bodicky St. Louis, Mo.
A. S. Brookes Newark, N. J.
T. J. Brosnan Buffalo, N. Y.
W. R. Bullard New York, N. Y.
H. W. Clark Washington, D. C.
E. W. Davis Cambridge, Mass.
W. A. Del Mar Yonkers, N, Y.
C. D. Eash Jackson, Mich.
D. M. Farnham Montreal, Quebec, Canada
G. H. Fiedler Rochester, N. Y.
L. E. Fogg Phillipsdale, R. I.
J. D. Frantz Portland, Oreg.
M. W. Ghen N. S. Pittsburgh, Pa.
L. F. Hickernell Hastings-on-Hudson, N. Y.
R. A. Hopkins Knoxville, Tenn.
A. H. Kidder Philadelphia, Pa.
R. H. Kolks Cincinnati, Ohio
L. I. Komives Detroit, Mich.
M. J. Lowenberg Boston, Mass.
C. G. Mansfield Los Angeles, Calif.
R. E. Morse New York, N. Y.
Lawrence Palmer Seattle, Wash.
W. T. Peirce Worcester, Mass.
M. L. Perry Kansas City, Mo.
R. S. Peterson Chicago, Ill.
A. D. Pettee Bayonne, N. J.
R. A. Schatzel Rome, N. Y.
V. E. Schlossberg East Chicago, Ind.
G. B. Shanklin Schenectady, N. Y.
H. D. Short Toronto, Ontario, Canada
F. V. Smith Chicago, Ill.
L. G. Smith Baltimore, Md.
G. M. L. Sommerman Evanston, Ill.
E. G. Sturdevant Bristol, R. I.
Gordon Thompson New York, N. Y.
H. H. Watson Bridgeport, Conn.
R. J. Wiseman Passaic, N. J.
R. K. Woodson Kansas City, Mo.
K. S. Wyatt New York, N. Y.
C. W. Zimmerer New York, N. Y.
W. N. Zippler New York, N. Y.

Administration Subcommittee

Herman Halperin, *chairman;* Commonwealth Edison Company, 72 West Adams Street, Chicago 90, Ill.
V. A. Sheals, *vice-chairman* Schenectady, N. Y.
C. T. Hatcher, *secretary* New York, N. Y.
L. F. Hickernell, *ex-chairman* Hastings-on-Hudson, N. Y.

Subcommittee Chairmen:
G. W. Acock Trenton, N. J.
W. A. Del Mar Yonkers, N. Y.
G. H. Fiedler Rochester, N. Y.
L. E. Fogg Phillipsdale, R. I.
M. W. Ghen Pittsburgh, Pa.
L. I. Komives Detroit, Mich.
W. T. Peirce Worcester, Mass.
R. S. Peterson Chicago, Ill.
G. M. L. Sommerman Evanston, Ill.
Gordon Thompson New York, N. Y.
H. H. Watson Bridgeport, Conn.

Standards and Publications Subcommittee

L. E. Fogg, *chairman;* Kennecott Wire and Cable Company, Phillipsdale, R. I.
A. S. Brookes Newark, N. J.
M. H. McGrath Bayonne, N. J.
V. A. Sheals Schenectady, N. Y.
H. D. Short Toronto, Ontario, Canada
C. W. Zimmerer New York, N. Y.

Cable Characteristics Subcommittee

W. A. Del Mar, *chairman;* Phelps Dodge Copper Products Company, Habirshaw Cable and Wire Division, Yonkers, N. Y.
E. D. Bent Montreal, Quebec, Canada
C. T. Hatcher New York, N. Y.
A. H. Kidder Philadelphia, Pa.
C. G. Mansfield Los Angeles, Calif.
M. H. McGrath Bayonne, N. J.
J. H. Neher Philadelphia, Pa.
G. B. Shanklin Schenectady, N. Y.

(*Continued in next column*)

F. V. Smith Chicago, Ill.
E. R. Thomas New York, N. Y.
R. J. Wiseman Passaic, N. J.
R. K. Woodson Kansas City, Mo.

Conductors Subcommittee

G. W. Acock, *chairman;* J. A. Roebling's Sons, South Broad Street, Trenton 2, N. J.
R. C. Waldron Passaic, N. J.
G. W. Zink Yonkers, N. Y.

Insulations Subcommittee

G. M. L. Sommerman, *chairman;* Department of Electrical Engineering, Northwestern Technological Institute, Evanston, Ill.
J. C. Balsbaugh Cambridge, Mass.
C. E. Betzer Chicago, Ill.
F. M. Clark Schenectady, N. Y.
A. S. Dana Seymour, Conn.
E. W. Greenfield Hastings-on-Hudson, N. Y.
Thorstein Larson Baltimore, Md.
E. L. Lotz Irvington, N. J.
R. B. McKinley Bridgeport, Conn.
J. D. Piper Detroit, Mich.
R. A. Schatzel Rome, N. Y.
E. G. Sturdevant Bristol, R. I.
A. J. Warner New York, N. Y.
K. S. Wyatt Yonkers, N. Y.

Sheaths and Coverings Subcommittee

G. H. Fiedler, *chairman;* Rochester Gas and Electric Company, Rochester, N. Y.
C. F. Avila Boston, Mass.
C. E. Betzer Chicago, Ill.
Andrew Bodicky St. Louis, Mo.
A. S. Brockes Newark, N. J.
A. H. Kidder Philadelphia, Pa.
F. E. Kulman New York, N. Y.
A. D. Pettee Bayonne, N. J.
V. A. Sheals Schenectady, N. Y.
R. C. Waldron Passaic, N. J.
G. W. Zink Yonkers, N. Y.

Cable Supply Systems Subcommittee

L. I. Komives, *chairman;* Detroit Edison Company, 2000—2d Avenue, Detroit 26, Mich.
C. F. Avila Boston, Mass.
T. J. Brosnan Buffalo, N. Y.
W. R. Bullard New York, N. Y.
H. W. Clark Washington, D. C.
D. M. Farnham Montreal, Quebec, Canada
J. D. Frantz Portland, Oreg.
C. T. Hatcher New York, N. Y.
M. J. Lowenberg Boston, Mass.
A. D. Pettee Bayonne, N. J.
C. S. Schifreen Philadelphia, Pa.
G. B. Shanklin Schenectady, N. Y.
H. D. Short Toronto, Ontario, Canada
C. F. Titus Jacksonville, Fla.
R. J. Wiseman Passaic, N. J.
K. S. Wyatt Yonkers, N. Y.

Utilization Wiring Systems Subcommittee

H. H. Watson, *chairman;* General Electric Company, 1285 Boston Avenue, Bridgeport 2, Conn.
E. L. Bailey Detroit, Mich.
H. G. Barnett East Pittsburgh, Pa.
A. L. Barrett Franklin, Pa.
T. P. Branch Washington, D. C.
G. B. Dodds Pittsburgh, Pa.
C. A. Finkel Passaic, N. J.
R. W. Foster Canton, N. C.
F. S. Glaza Freeport, Tex.
Lester Goldsmith Philadelphia, Pa.
J. E. Huguelet Detroit, Mich.
M. J. Lowenberg Boston, Mass.
J. B. MacLean New York, N. Y.
S. J. Rosch Hastings-on-Hudson, N. Y.
R. A. Schatzel Rome, N. Y.
V. E. Schlossberg East Chicago, Ind.
J. S. Waddington Brockville, Ontario, Canada
C. W. Zimmerer New York, N. Y.
G. W. Zink Yonkers, N. Y.

Special-Purpose Cable Subcommittee

W. T. Peirce, *chairman;* American Steel and Wire Company, Worcester, Mass.
G. W. Acock Trenton, N. J.
J. F. Atkinson Washington, D. C.
A. L. Barrett Franklin, Pa.

(*Continued in next column*)

E. D. Bent Montreal, Quebec, Canada
E. W. Davis Cambridge, Mass.
R. I. Fort Chicago, Ill.
R. A. Hopkins Knoxville, Tenn.
J. E. Huguelet Detroit, Mich.
R. H. Kolks Cincinnati, Ohio
R. B. McKinley Bridgeport, Conn.
S. B. Pennell New York, N. Y.
M. L. Perry Kansas City, Mo.
V. E. Schlossberg East Chicago, Ind.
F. V. Smith Chicago, Ill.
E. G. Sturdevant Bristol, R. I.
C. F. Titus Jacksonville, Fla.
A. D. Whamond Philadelphia, Pa.
R. K. Woodson Kansas City, Mo.
W. N. Zippler New York, N. Y.
Myron Zucker Royal Oak, Mich.

Accessories Subcommittee

M. W. Ghen, *chairman;* Duquesne Light Company, 1241 Reedsdale St., N. S., Pittsburgh 12, Pa.
C. Christianson Melrose Park, Ill.
C. D. Eash Jackson, Mich.
J. D. Frantz Portland, Oreg.
G. G. Grissinger East Pittsburgh, Pa.
Fred Heller New York, N. Y.
J. E. Johnson Philadelphia, Pa.
R. H. Kolks Cincinnati, Ohio
E. L. Lotz Irvington, N. J.
H. E. Martin Brooklyn, N. Y.
R. E. Morse New York, N. Y.
J. H. Nicholas Chicago, Ill.
Lawrence Palmer Wenatchee, Wash.
M. L. Perry Kansas City, Mo.
F. P. West New York, N. Y.
L. Wetherill Pittsfield, Mass.
D. S. Young Toronto, Ontario, Canada

Structures Subcommittee

R. S. Peterson, *chairman;* Commonwealth Edison Company, 72 West Adams Street, Chicago 90, Ill.
C. E. Baugh San Francisco, Calif.
Andrew Bodicky St. Louis, Mo.
T. J. Brosnan Buffalo, N. Y.
H. W. Clark Washington, D. C.
C. D. Eash Jackson, Mich.
G. H. Fiedler Rochester, N. Y.
Lawrence Palmer Wenatchee, Wash.
C. G. Mansfield Los Angeles, Calif.
L. G. Smith Baltimore, Md.
F. P. West New York, N. Y.
R. K. Woodson Kansas City, Mo.

Tests and Measurements Subcommittee

Gordon Thompson, *chairman;* Electrical Testing Laboratories, Inc., 2 West End Avenue, New York 21, N. Y.
J. C. Balsbaugh Cambridge, Mass.
A. L. Brownlee Chicago, Ill.
A. S. Dana Seymour, Conn.
E. W. Davis Cambridge, Mass.
W. A. Del Mar Yonkers, N. Y.
E. W. Greenfield Hastings-on-Hudson, N. Y.
Thorstein Larson Baltimore, Md
J. C. Parker Memphis, Tenn.
G. M. L. Sommerman Evanston, Ill.
R. F. Tener Washington, D. C.
E. R. Thomas New York, N. Y.
R. C. Waldron Passaic, N. J.
C. W. Zimmerer New York, N. Y.

Power Generation

B. G. A. Skrotzki, *chairman;* *Power*, McGraw-Hill Publishing Company, Inc., 330 West 42d Street, New York 18, N. Y.
J. A. Brooks, *vice-chairman* New York, N. Y.
Giuseppe Calabrese, *secretary* New York, N. Y.
R. E. Appleyard Milwaukee, Wis.
J. E. Barkle, Jr. East Pittsburgh, Pa.
C. S. Bowden Cleveland, Ohio
E. P. Bryant Los Angeles, Calif.
R. M. Bush Washington, D. C.
Hubbell Carpenter Duluth, Minn.
A. D. Caskey Chicago, Ill.
H. E. Deardorff Dayton, Ohio
S. M. Denton Denver, Colo.
E. F. Dissmeyer Jackson, Mich.
Alex Dovjikov Portland, Oreg.
John Finlaw Philadelphia, Pa.
C. B. Fontaine Lynn, Mass.
C. A. Harrington Cleveland, Ohio
H. R. Harris Detroit, Mich.

(*Continued in next column*)

A. F. Hartung Kansas City, Mo.
A. P. Hayward Pittsburgh, Pa.
B. V. Hoard Portland, Oreg.
W. R. Johnson San Francisco, Calif.
L. A. Kilgore East Pittsburgh, Pa.
A. J. Krupy Chicago, Ill.
F. L. Lawton Montreal, Quebec, Canada
J. T. Madill Arvida, Quebec, Canada
E. F. Maryatt San Francisco, Calif.
J. B. McClure Schenectady, N. Y.
G. H. McDaniel New York, N. Y.
D. R. Pattison Johnstown, Pa.
M. L. Perry Kansas City, Mo.
W. W. Perry Binghamton, N. Y.
A. L. Pollard Seattle, Wash.
C. S. Randall Washington, D. C.
H. B. Robinson Raleigh, N. C.
G. M. Tatum Richmond, Va.
C. W. Watchorn Baltimore, Md.
C. C. Whelchel San Francisco, Calif.
R. R. Wisner Boston, Mass.
G. R. Woodman Los Angeles, Calif.

Prime Movers Subcommittee

B. G. A. Skrotzki, *chairman;* Associate Editor, *Power*, 330 West 42d Street, New York 18, N. Y.
J. A. Brooks New York, N. Y.
E. P. Bryant Los Angeles, Calif.
A. D. Caskey Chicago, Ill.
John Finlaw Philadelphia, Pa.
A. F. Hartung Kansas City, Mo.
C. S. Randall Washington, D. C.
H. B. Robinson Raleigh, N. C.
C. C. Whelchel San Francisco, Calif.

Speed Governing Subcommittee

J. B. McClure, *chairman;* General Electric Company, Central Station Engineering Divisions, Schenectady 5, N. Y.
R. E. Appleyard Milwaukee, Wis.
C. P. Almon, Jr. Chattanooga, Tenn.
R. M. Beville Chicago, Ill.
A. P. Hayward Pittsburgh, Pa.
T. H. Mawson Birmingham, Ala.
G. H. McDaniel New York, N. Y.
F. L. Reinmann Michigan City, Ind.

Excitation Systems Subcommittee

A. J. Krupy, *chairman;* Commonwealth Edison Company, 72 West Adams Street, Chicago 90, Ill.
J. B. McClure, *vice-chairman* Schenectady, N. Y.
J. E. Barkle, Jr. East Pittsburgh, Pa.
E. F. Dissmeyer Jackson, Mich.
Alex. Dovjikov Portland, Oreg.
C. B. Fontaine, Jr. Lynn, Mass.
L. A. Kilgore East Pittsburgh, Pa.
J. H. Kinghorn New York, N. Y.
H. A. P. Langstaff Pittsburgh, Pa.
T. B. Montgomery Milwaukee, Wis.
G. M. Tatum Richmond, Va.
F. D. Troxel Chicago, Ill.
R. L. Witzke East Pittsburgh, Pa.

Station Design Subcommittee

H. R. Harris, *chairman;* The Detroit Edison Company, 2000—2d Avenue, Detroit 26, Mich.
C. S. Bowden Cleveland, Ohio
R. M. Bush Washington, D. C.
Hubbell Carpenter Duluth, Minn.
H. E. Deardorff Dayton, Ohio
C. A. Harrington Cleveland, Ohio
W. R. Johnson San Francisco, Calif.
D. R. Pattison Johnstown, Pa.
M. L. Perry Kansas City, Mo.
W. W. Perry Binghamton, N. Y.
R. R. Wisner Boston, Mass.

Hydroelectric Systems Subcommittee

F. L. Lawton, *chairman;* Aluminum Laboratories, Ltd., 1800 Sun Life Building, Montreal, Quebec, Canada
S. M. Denton Denver, Colo.
J. T. Madill Arvida, Quebec, Canada
A. L. Pollard Seattle, Wash.
G. R. Woodman Los Angeles, Calif.

Pacific Coast Subcommittee

Alex. Dovjikov, *chairman;* Bonneville Power Administration, Engineering Department, Portland, Oreg.
E. P. Bryant Los Angeles, Calif.
S. M. Denton Denver, Colo.
B. V. Hoard Portland, Oreg.

(*Continued on page 1422*)

W. R. Johnson	San Francisco, Calif.
E. F. Maryatt	San Francisco, Calif.
A. L. Pollard	Seattle, Wash.
C. C. Whelchel	San Francisco, Calif.
G. R. Woodman	Los Angeles, Calif.

Protective Devices

W. J. Rudge, *chairman;* General Electric Company, 100 Woodlawn Avenue, Pittsfield, Mass.

H. R. Stewart, *vice-chairman*	Boston, Mass.
A. A. Johnson, *secretary*	East Pittsburgh, Pa.
L. J. Blaize	Dallas, Tex.
V. J. Cissna	Chattanooga, Tenn.
J. E. Clem	Schenectady, N. Y.
J. O. Coleman	Boston, Mass.
L. M. Connors	Toronto, Ontario, Canada
H. A. Cornelius	Chicago, Ill.
F. M. Defandorf	Washington, D. C.
G. K. Ditlow	Washington, D. C.
G. B. Dodds	Pittsburgh, Pa.
R. H. Earle	Milwaukee, Wis.
T. I. Eldridge	Philadelphia, Pa.
W. D. Hardaway	Denver, Colo.
E. B. Harmon	Denver, Colo.
E. M. Hunter	Schenectady, N. Y.
P. A. Jeanne	New York, N. Y.
W. A. Lewis, Jr.	Chicago, Ill.
F. R. Longley	Springfield, Mass.
Cort Lowerison	Los Angeles, Calif.
T. H. Mawson	Birmingham, Ala.
Corbett McLean	Portland, Oreg.
E. G. Norell	Chicago, Ill.
A. M. Opsahl	East Pittsburgh, Pa.
H. L. Rorden	Portland, Oreg.
H. D. Ruger	Sparrows Point, Md.
W. E. Rushlow	Washington, D. C.
O. L. Sidenfaden	Los Angeles, Calif.
W. T. Smith	New York, N. Y.
J. M. Towner	Baltimore, Md.
S. S. Watkins	New York, N. Y.
E. H. Yonkers	Chicago, Ill.

Fault Limiting Devices Subcommittee

E. G. Norell, *chairman;* Sargent and Lundy, 140 South Dearborn Street, Chicago 3, Ill.

(Personnel to be selected)

Lightning Protective Devices Subcommittee

E. H. Yonkers, *chairman;* Joslyn Manufacturing and Supply Company, 3700 S. Morgan Street, Chicago 9, Ill.

(Personnel to be selected)

Insulation Co-ordination Subcommittee

J. E. Clem, *chairman;* Central Station Engineering Division General Electric Company, Schenectady 5, N. Y.

(Personnel to be selected)

Relays

E. L. Michelson, *chairman;* Commonwealth Edison Company, 72 West Adams Street, Chicago 90, Ill.

A. J. McConnell, *vice-chairman*	Schenectady, N. Y.
J. R. Linders, *secretary*	Cleveland, Ohio
C. E. Asbury	Jackson, Mich.
M. A. Bostwick	Portland, Oreg.
G. E. Brooks	Oklahoma City, Okla.
R. J. Cooper	Atlanta, Ga.
R. E. Cordray	Philadelphia, Pa.
G. E. Dana	Binghamton, N. Y.
G. B. Dodds	Pittsburgh, Pa.
M. A. Faucett	Urbana, Ill.
F. S. Fehr	Reading, Pa.
L. F. Ferri	Elyria, Ohio
L. L. Fountain	East Pittsburgh, Pa.
Sidney Goldsmith	Cincinnati, Ohio
E. T. B. Gross	Chicago, Ill.
T. R. Halman	Detroit, Mich.
E. L. Harder	East Pittsburgh, Pa.
V. J. Hayes	Waterbury, Conn.
B. C. Hicks	Montreal, Quebec, Canada
J. H. Kinghorn	New York, N. Y.
R. A. Larner	Fort Worth, Texas
W. A. Lewis, Jr.	Chicago, Ill.
S. C. Leyland	Newark, N. J.
H. F. Lindemuth	New York, N. Y.
Cort Lowerison	Los Angeles, Calif.
W. E. Marter	Pittsburgh, Pa.
W. A. Morgan	Denver, Colo.
C. E. Parks	Indianapolis, Ind.
H. R. Paxson	Philadelphia, Pa.
F. C. Poage	New York, N. Y.
E. G. Ratz	Hamilton, Ontario, Canada
K. N. Reardon	Pittsburgh, Pa.
H. P. Sleeper	Newark, N. J.
W. K. Sonnemann	Newark, N. J.
George Steeb	Buffalo, N. Y.

(*Continued in next column*)

Project Committee on Bibliography of Relay Literature

E. T. B. Gross, *chairman* and *sponsor;* Illinois Institute of Technology, 3300 Federal Street, Chicago 16, Ill.

S. Goldsmith	Cincinnati, Ohio
C. E. Parks	Indianapolis, Ind.
C. L. Smith	Rochester, N. Y.

Project Committee on Generator Protection

H. R. Paxson, *chairman* and *sponsor;* Philadelphia Electric Company, 900 Sansom Street, Philadelphia 5, Pa.

L. L. Draper	Los Angeles, Calif.
B. C. Hicks	Montreal, Quebec, Canada
S. C. Leyland	Newark, N. J.
H. F. Lindemuth	New York, N. Y.
W. E. Marter	Pittsburgh, Pa.
J. B. McClure	Schenectady, N. Y.
A. J. McConnell	Schenectady, N. Y.
C. E. Parks	Indianapolis, Ind.
F. C. Poage	New York, N. Y.
C. L. Smith	Rochester, N. Y.

Project Committee on Co-ordination of Construction and Protection of Distribution Circuits (Joint with Distribution Subcommittee and Edison Electric Institute)

G. B. Dodds, AIEE *chairman* and *sponsor;* Duquesne Light Company, 435 Sixth Avenue, Pittsburgh 19, Pa.

R. O. Askey	Chicago, Ill.
T. J. Brosnan	Buffalo, N. Y.
W. B. Fisk, Jr.	New York, N. Y.
H. W. Haberl	Montreal, Quebec, Canada
C. L. Headley	Baltimore, Md.
C. Lowerison	Los Angeles, Calif.
A. L. Malmstrom	Detroit, Mich.
H. F. Ostman	St. Louis, Mo.
C. E. Parks	Indianapolis, Ind.
J. S. Parsons	East Pittsburgh, Pa.
R. F. Quinn	Schenectady, N. Y.
H. P. Sleeper	Newark, N. J.
C. L. Smith	Rochester, N. Y.

C. J. Belier, EEI *chairman* and *sponsor;* Cleveland Electric Illuminating Company, 75 Public Square, Cleveland 1, Ohio

G. H. Fiedler	Rochester, N. Y.
E. W. Knapp	Montreal, Quebec, Canada
William McLean	Baltimore, Md.

Project Committee on Revision of Standards for Power Relays

S. C. Leyland, *chairman* and *sponsor;* Westinghouse Electric Corporation, 95 Orange Street, Newark 2, N. J.

R. E. Cordray	Philadelphia, Pa.
V. L. Cox	Philadelphia, Pa.
L. L. Fountain	East Pittsburgh, Pa.
W. A. Lewis, Jr.	Chicago, Ill.
J. R. Linders	Cleveland, Ohio
E. L. Michelson	Chicago, Ill.
W. A. Morgan	Denver, Colo.
H. P. Sleeper	Newark, N. J.
George Steeb	Buffalo, N. Y.

Project Committee on Sensitive Ground Protection

E. T. B. Gross, *chairman* and *sponsor;* Illinois Institute of Technology, 3300 Federal Street, Chicago 16, Ill.

C. J. Berghs	Glencoe, Ill.
R. J. Cooper	Atlanta, Ga.
T. R. Halman	Detroit, Mich.
P. A. Jeanne	New York, N. Y.
C. Lowerison	Los Angeles, Calif.
W. K. Sonnemann	Newark, N. J.
A. R. van C. Warrington	Philadelphia, Pa.

Project Committee on Transmission Line Protection

E. L. Harder, *chairman* and *sponsor;* Westinghouse Electric Corporation, East Pittsburgh, Pa.

(*Continued in next column*)

L. J. Audlin	Syracuse, N. Y.
W. R. Brownlee	Jackson, Mich.
B. C. Hicks	Montreal, Quebec, Canada
J. H. Kinghorn	New York, N. Y.
W. A. Lewis, Jr.	Chicago, Ill.
W. E. Marter	Pittsburgh, Pa.
A. J. McConnell	Schenectady, N. Y.
M. S. Merritt	Chattanooga, Tenn.
E. L. Michelson	Chicago, Ill.
C. E. Parks	Indianapolis, Ind.
K. N. Reardon	Pittsburgh, Pa.

Project Committee on Electronic Relay Applications

A. J. McConnell, *chairman* and *sponsor;* General Electric Company, Schenectady 5, N. Y.

J. E. Barkle, Jr.	East Pittsburgh, Pa.
R. J. Cooper	Atlanta, Ga.
F. S. Fehr	Reading, Pa.
L. F. Ferri	Elyria, Ohio
E. L. Harder	East Pittsburgh, Pa.
J. H. Kinghorn	New York, N. Y.
D. M. MacGregor	New York, N. Y.
E. L. Michelson	Chicago, Ill.
E. G. Ratz	Hamilton, Ontario, Canada

Project Committee on Insulation Levels of Relays and Control Circuits

E. L. Michelson, *chairman* and *sponsor;* Commonwealth Edison Company, 72 West Adams Street, Chicago 90, Ill.

R. E. Cordray	Philadelphia, Pa.
S. Goldsmith	Cincinnati, Ohio
T. R. Halman	Detroit, Mich.
V. J. Hayes	Waterbury, Conn.
W. K. Sonnemann	Newark, N. J.

Project Committee on Relaying of Interconnections Between Industrial and Utility Generating Systems

K. N. Reardon, *chairman* and *sponsor;* West Penn Power Company, 14 Wood Street, Pittsburgh, Pa.

J. E. Barkle, Jr.	East Pittsburgh, Pa.
G. E. Dana	Binghamton, N. Y.
F. S. Fehr	Reading, Pa.
T. R. Halman	Detroit, Mich.
J. R. Linders	Lakewood, Ohio

Project Committee on Relaying Performance of Current Transformer (Emphasis on High Impedance Secondary Types)

W. E. Marter, *chairman* and *sponsor;* Duquesne Light Company, 435—6th Avenue, Pittsburgh 19, Pa.

J. E. Barkle, Jr.	East Pittsburgh, Pa.
J. E. Clem	Schenectady, N. Y.
R. E. Cordray	Philadelphia, Pa.
W. K. Dickinson	Lynn, Mass.
T. R. Halman	Detroit, Mich.
W. A. Lewis, Jr.	Chicago, Ill.
W. K. Sonnemann	Newark, N. J.
E. C. Wentz	Sharon, Pa.

Rotating Machinery

W. R. Hough, *chairman;* Reliance Electric and Engineering Company, 1088 Ivanhoe Road, Cleveland 10, Ohio

C. G. Veinott, *vice-chairman*	Lima, Ohio
J. L. Fuller, *secretary*	Cleveland, Ohio
J. E. Allen	Lancaster, Pa.
W. R. Appleman	Dayton, Ohio
E. L. Bailey	Detroit, Mich.
Sterling Beckwith	Milwaukee, Wis.
B. H. Caldwell	Schenectady, N. Y.
A. G. Conrad	New Haven, Conn.
James DeKiep	Minneapolis, Minn.
E. F. Dissmeyer	Jackson, Mich.
L. O. Dorfman	New York, N. Y.
W. H. Fifer	Washington, D. C.
D. E. Fritz	Cleveland, Ohio
J. S. Gault	Ann Arbor, Mich.
C. B. Hathaway	Buffalo, N. Y.
V. P. Hessler	Urbana, Ill.
L. E. Hildebrand	Lynn, Mass.
J. W. Jones	Philadelphia, Pa.
C. E. Kilbourne	Schenectady, N. Y.
A. A. Kronberg	Los Angeles, Calif.
I. M. Levy	Dayton, Ohio
T. M. Linville	Schenectady, N. Y.
M. R. Lory	East Pittsburg, Pa.
Clarence Lynn	East Pittsburgh, Pa.
W. E. Meserve	Ithaca, N. Y.
R. C. Moore	Milwaukee, Wis.
J. G. Noest	New York, N. Y.
E. I. Pollard	Ridgway, Pa.
C. P. Potter	St. Louis, Mo.
F. C. Rushing	Buffalo, N. Y.
V. E. Schlossberg	East Chicago, Ind.
M. L. Schmidt	Fort Wayne, Ind.
J. F. Sellers	Milwaukee, Wis.
P. H. Trickey	St. Louis, Mo.
R. W. Wieseman	Schenectady, N. Y.
W. W. Wishard	Chicago, Ill.
S. S. Wolff	St. Louis, Mo.

(*Continued in next column*)

Administrative Subcommittee

W. R. Hough, *chairman,* Reliance Electric and Engineering Company, 1068–1088 Ivanhoe Road, Cleveland 10, Ohio

C. G. Veinott, *vice-chairman*	Lima, Ohio
J. L. Fuller, *secretary*	Cleveland, Ohio
B. H. Caldwell	Schenectady, N. Y.
James DeKiep	Minneapolis, Minn.
J. L. Fuller	Cleveland, Ohio
J. W. Jones	Philadelphia, Pa.
I. M. Levy	Dayton, Ohio
S. S. Wolff	St. Louis, Mo.

Synchronous Machinery Subcommittee

James DeKiep, *chairman;* Electric Machinery Manufacturing Company, 1331 Tyler Street, Minneapolis 13, Minn.

Sterling Beckwith	Milwaukee, Wis.
J. F. Calvert	Evanston, Ill.
S. B. Crary	Schenectady, N. Y.
L. O. Dorfman	New York, N. Y.
E. L. Harder	East Pittsburgh, Pa.
R. A. Hopkins	Knoxville, Tenn.
L. F. Hunt	Los Angeles, Calif.
J. W. Jones	Philadelphia, Pa.
C. E. Kilbourne	Schenectady, N. Y.
M. R. Lory	East Pittsburgh, Pa.
G. W. McIver	Toledo, Ohio
W. P. Monroe	Chicago, Ill.
J. G. Noest	New York, N. Y.
E. I. Pollard	Ridgway, Pa.
S. S. Wolff	St. Louis, Mo.

Induction Machinery Subcommittee

S. S. Wolff, *chairman;* Century Electric Company, 1806 Pine Street, St. Louis 3, Mo.

R. E. Arnold	West Lynn, Mass.
E. C. Barnes	Cleveland, Ohio
S. F. Henderson	Buffalo, N. Y.
C. J. Koch	Schenectady, N. Y.
I. M. Levy	Dayton, Ohio
T. C. Lloyd	Springfield, Ohio
R. C. Moore	Milwaukee, Wis.
J. E. Noest	New York, N. Y.
C. P. Potter	St. Louis, Mo.
F. C. Rushing	Buffalo, N. Y.
P. H. Trickey	Finderne, N. J.
F. M. Winterhalter	Norwood, Ohio

D-C Machinery Subcommittee

B. H. Caldwell, *chairman;* General Electric Company, 1 River Road, Schenectady 5, N. Y.

Florence F. Buckland	Schenectady, N. Y.
A. G. Conrad	New Haven, Conn.
A. G. Darling	Schenectady, N. Y.
W. H. Fifer	Washington, D. C.
D. E. Fritz	Cleveland, Ohio
Lanier Greer	Cleveland, Ohio
C. B. Hathaway	Buffalo, N. Y.
V. P. Hessler	Urbana, Ill.
V. B. Honsinger	Cincinnati, Ohio
Paul Lebenbaum, Jr.	Lynn, Mass.
T. M. Linville	Schenectady, N. Y.
Clarence Lynn	East Pittsburgh, Pa.
A. T. McClinton	Washington, D. C.
J. G. Noest	New York, N. Y.
L. G. Opel	Buffalo, N. Y.
J. H. Schneider	Ridgway, Pa.
J. F. Sellers	Milwaukee, Wis.
P. H. Trickey	Finderne, N. J.
S. S. Wolff	St. Louis, Mo.

Single-Phase and Fractional-Horsepower Subcommittee

I. M. Levy, *chairman;* Delco Products Division, General Motors Corporation, Dayton, Ohio

W. R. Appleman	Dayton, Ohio
J. S. Gault	Ann Arbor, Mich.
E. E. Kimberly	Columbus, Ohio
T. C. Lloyd	Springfield, Ohio

(*Continued on page 1423*)

R. F. Munier	St. Louis, Mo.
C. P. Potter	St. Louis, Mo.
M. L. Schmidt	Fort Wayne, Ind.
P. H. Trickey	Finderne, N. J.
C. G. Veinott	Lima, Ohio
S. S. Wolff	St. Louis, Mo.

Test Code Subcommittee

J. L. Fuller, *chairman;* Reliance Electric and Engineering Company, 1088 Ivanhoe Road, Cleveland 10, Ohio

J. E. Allen	Lancaster, Pa.
Sterling Beckwith	Milwaukee, Wis.
J. S. Gault	Ann Arbor, Mich.
L. E. Hildebrand	Lynn, Mass.
Clarence Lynn	East Pittsburgh, Pa.
R. F. Munier	St. Louis, Mo.

Insulation Resistance Subcommittee

J. W. Jones, *chairman;* Philadelphia Electric Company, Edison Building, Philadelphia 5, Pa.

J. E. Allen	Lancaster, Pa.
L. J. Berberich	East Pittsburgh, Pa.
E. B. Curdts	Philadelphia, Pa.
E. F. Dissmeyer	Jackson, Mich.
Orville Fawcett	Pittsburgh, Pa.
D. D. Higgins	Chicago, Ill.
J. L. Kuehlthau	Milwaukee, Wis.
V. E. Schlossberg	East Chicago, Ind.
H. R. Stewart	Boston, Mass.
J. B. Swering	Hartford, Conn.
A. L. Van Emden	Washington, D. C.
R. W. Wieseman	Schenectady, N. Y.

Substations

G. S. Lunge, *chairman;* Central Station Engineering Divisions, General Electric Company, Schenectady 5, N. Y.

R. C. Ericson, *vice-chairman*	Hammond, Ind.
Marino Fraresso, *secretary*	Toronto, Ontario, Canada
T. J. Allen	Atlanta, Ga.
Herman Bany	Philadelphia, Pa.
Harris Barber	Boston, Mass.
R. O. Bell	Milwaukee, Wis.
F. J. Berger	Philadelphia, Pa.
M. K. Brown	Buffalo, N. Y.
C. C. Crane	Madison, Wis.
B. D. Dexter	San Francisco, Calif.
M. V. Eardley	Los Angeles, Calif.
G. P. Fallon	Baltimore, Md.
H. F. Gidlund	Denver, Colo.
K. B. Hoffman	New York, N. Y.
E. M. Hunter	Schenectady, N. Y.
S. C. Killian	Chicago, Ill.
W. J. Lank	Washington, D. C.
N. G. Larson	Chicago, Ill.
J. T. Lienert	Portland, Oreg.
W. M. McCauley	Greensburg, Pa.
J. L. McKee	New York, N. Y.
I. S. Mendenhall	Detroit, Mich.
D. C. Millard	Denver, Colo.
M. E. Reagan	East Pittsburgh, Pa.
J. H. Rixse, Jr.	Washington, D. C.
W. S. Scheering	Springfield, Mass.
C. H. Smoke	Portsmouth, Va.
R. F. Stevens	Portland, Oreg.
K. L. Wheeler	Cleveland, Ohio

Executive Subcommittee

G. S. Lunge, *chairman;* Central Station Engineering Divisions, General Electric Company, Schenectady 5, N. Y.

M. V. Eardley	Los Angeles, Calif.
R. C. Ericson	Hammond, Ind.
Marino Fraresso	Toronto, Ontario, Canada
H. B. Hoffman	New York, N. Y.
M. E. Reagan	East Pittsburgh, Pa.

Automatic and Supervisory Control Subcommittee

M. E. Reagan, *chairman;* Switchboard Engineering Department, Westinghouse Electric Corporation, East Pittsburgh, Pa.

T. J. Allen	Atlanta, Ga.
Herman Bany	Philadelphia, Pa
R. O. Bell	Milwaukee, Wis.
B. D. Dexter	San Francisco, Calif.
R. C. Ericson	Hammond, Ind.
J. L. McKee	New York, N. Y.

Distribution and Conversion Substation Subcommittee

M. V. Eardley, *chairman;* Department of Water and Power, City of Los Angeles, Los Angeles 54, Calif.

(*Continued in next column*)

T. J. Allen	Atlanta, Ga.
Herman Bany	Philadelphia, Pa.
R. O. Bell	Milwaukee, Wis.
M. K. Brown	Buffalo, N. Y.
C. C. Crane	Madison, Wis.
G. P. Fallon	Baltimore, Md.
E. M. Hunter	Schenectady, N. Y.
J. T. Lienert	Portland, Oreg.
I. S. Mendenhall	Detroit, Mich.

Transmission Substation Subcommittee

K. B. Hoffman, *chairman;* Burns and Roe, Inc., 233 Broadway, New York 7, N. Y.

T. J. Allen	Atlanta, Ga.
Harris Barber	Boston, Mass.
F. J. Berger	Philadelphia, Pa.
H. P. Cadario	Toronto, Ontario, Canada
C. C. Crane	Madison, Wis.
Marino Fraresso	Toronto, Ontario, Canada
H. F. Gidlund	Denver, Colo.
E. M. Hunter	Schenectady, N. Y.
S. C. Killian	Chicago, Ill.
N. G. Larson	Chicago, Ill.
W. M. McCauley	Greensburg, Pa.
D. C. Millard	Denver, Colo.
J. H. Rixse, Jr.	Washington, D. C.
W. S. Schering	Springfield, Ill.
R. F. Stevens	Portland, Oreg.

Working Group on Device Function Numbers

R. C. Ericson, *chairman;* Northern Indiana Public Service Company, 5265 Hohman Avenue, Hammond, Ind.

Herman Bany	Philadelphia, Pa.
L. B. LeVesconte	Chicago, Ill.
M. E. Reagan	East Pittsburgh, Pa.
M. S. Schneider	Cincinnati, Ohio

Working Group on Substation Grounding Practice

H. F. Gidlund, *chairman;* Public Service Company of Colorado, 810—15th Street, Denver 2, Colo.

F. J. Berger	Philadelphia, Pa.
M. K. Brown	Buffalo, N. Y.
F. R. Longley	Springfield, Mass.
J. M. Towner	Baltimore, Md.

Working Group on Rectifier Switchgear

M. E. Reagan, *chairman;* Switchgear Engineering Department, Westinghouse Electric Corporation, East Pittsburgh, Pa.

D. C. Hoffman	Philadelphia, Pa.
H. V. Nye	West Allis, Wis.

Working Group on Basic One-Line Diagrams

E. M. Hunter, *chairman;* General Electric Co., Central Station Engg. Div., Schenectady 5, N. Y.

(Personnel to be selected)

Switchgear

F. A. Lane, *chairman;* American Gas and Electric Service Corporation, 30 Church Street, New York 8, N. Y.

H. V. Nye, *vice-chairman*	West Allis, Wis.
Otto Naef, *secretary*	New York, N. Y.
F. S. Benson	San Francisco, Calif.
C. K. Beyette	Fort Worth, Tex
Harry Brown	New Haven, Conn.
J. S. Brown	Pittsburgh, Pa.
E. A. Childerhose	Boston, Mass.
H. W. Codding	Newark, N. J.
V. L. Cox	Philadelphia, Pa.
G. K. Ditlow	Washington, D. C.
G. H. Doan	Detroit, Mich.
J. A. Elzi	Jackson, Mich.
W. E. Enns	Portland, Oreg.
K. J. C. Falck	Philadelphia, Pa.
R. M. Ferrill	Chattanooga, Tenn.
J. M. Geiger	Buffalo, N. Y.
W. J. Gilson	Toronto, Ontario, Canada
J. W. Graff	Birmingham, Ala.
C. W. Gragg	Amarillo, Tex.
W. D. Hardaway	Denver, Colo.
V. J. Hayes	Waterbury, Conn.
M. H. Hobbs	East Pittsburgh, Pa.
L. M. Hovey	Winnipeg, Manitoba, Canada
C. L. Killgore	Denver, Colo.
I. T. Knight	Kansas City, Mo.
H. A. P. Langstaff	Pittsburgh, Pa.

(*Continued in next column*)

H. J. Lingal	East Pittsburgh, Pa.
F. W. McCloska	Chicago, Ill.
W. J. McLachlan	Schenectady, N. Y.
R. G. Meyerand	St. Louis, Mo.
E. F. Miller	Rock Island, Ill.
V. G. Muzsnay	Schenectady, N. Y.
M. L. Perry	Kansas City, Mo.
H. H. Rudd	Greensburg, Pa.
E. K. Sadler	Los Angeles, Calif.
A. W. Shelhorse	Atlanta, Ga.
O. L. Sidenfaden	Los Angeles, Calif.
J. F. Sinnott	San Diego, Calif.
Garland Stamper	Dayton, Ohio
A. Van Ryan	South Milwaukee, Wis.
J. H. Vivian	Los Angeles, Calif.
R. L. Webb	New York, N. Y.
C. E. Winegartner	Cleveland, Ohio
J. D. Wood	Philadelphia, Pa.
J. C. Woods	Chicago, Ill.
S. W. Zimmerman	Ithaca, N. Y.

Circuit Breaker Subcommittee

R. L. Webb, *chairman;* Consolidated Edison Company of New York Inc., 4 Irving Place, New York 3, N. Y.

F. W. Cramer	Pittsburgh, Pa.
R. M. Ferrill	Chattanooga, Tenn.
M. D. Hardaway	Denver, Colo.
A. W. Hill	East Pittsburgh, Pa.
M. H. Hobbs	East Pittsburgh, Pa.
C. L. Killgore	Denver, Colo.
W. J. McLachlan	Schenectady, N. Y.
J. R. North	Jackson, Mich.
H. V. Nye	West Allis, Wis.
F. V. Smith	Chicago, Ill.
A. Van Ryan	South Milwaukee, Wis.
J. H. Vivian	Los Angeles, Calif.
C. E. Winegartner	Cleveland, Ohio
J. D. Wood	Philadelphia, Pa.
J. C. Woods	Chicago, Ill.
B. W. Wyman	Philadelphia, Pa.

Working Groups:

A—Power Circuit Breakers
B—Low-Voltage Air Circuit Breakers
C—Automatic Reclosers
D—Calculations of Short-Circuit Currents in Low-Voltage Systems

Switchgear Assemblies Subcommittee

H. V. Nye, *chairman;* Allis-Chalmers Manufacturing Company, West Allis, Wis.

F. W. Cramer	Pittsburgh, Pa.
J. M. Geiger	Buffalo, N. Y.
C. H. Kreger	Chicago, Ill.
E. F. Miller	Rock Island, Ill.
G. M. Reed	Philadelphia, Pa.
H. H. Rudd	Greensburg, Pa.
B. K. Sturgis	Philadelphia, Pa.
O. B. Vikoren	Philadelphia, Pa.
C. P. West	East Pittsburgh, Pa.
W. F. Wetmore	Detroit, Mich.

Switches, Fuses, and Insulators Subcommittee

H. H. Rudd, *chairman;* Railway and Industrial Engineering Company, Greensburg, Pa.

K. J. C. Falck	Philadelphia, Pa.
A. H. Powell	Philadelphia, Pa.
J. M. Wallace	East Pittsburgh, Pa.
R. L. Webb	New York, N. Y.
J. C. Woods	Chicago, Ill.

Working Groups:

A—Air Switches and Insulators
B—Interrupter Switches
C—Fuses

Subcommittee on High-Voltage Switching (Where Out-of-Phase Voltages Exist at Time of Arc Interruption)

K. J. C. Falck, *chairman;* Philadelphia Electric Company, 1000 Chestnut Street, Philadelphia 5, Pa.

C. H. Black	Philadelphia, Pa.
W. E. Enns	Portland, Oreg.
R. M. Ferrill	Chattanooga, Tenn.
G. D. Floyd	Toronto, Ontario, Canada
I. W. Gross	New York, N. Y.
E. L. Harder	East Pittsburgh, Pa.
M. H. Hobbs	East Pittsburgh, Pa.
C. B. Kelley	Davenport, Iowa
H. V. Nye	West Allis, Wis.

System Engineering

Earle Wild, *chairman;* Commonwealth

(*Continued in next column*)

Edison Company, 72 West Adams Street, Chicago 90, Ill.

Robert Brandt, *vice-chairman*	Boston, Mass.
O. W. Manz, Jr., *secretary*	Brooklyn, N. Y.
C. G. Adams	Schenectady, N. Y.
C. P. Almon, Jr.	Chattanooga, Tenn.
John Bankus	Portland, Oreg.
M. L. Blair	Spokane, Wash.
W. R. Brownlee	Jackson, Mich.
O. E. Charlton	Birmingham, Ala.
O. A. Demuth	Portland, Oreg.
A. G. Dewars	Minneapolis, Minn.
W. B. Fisk	New York, N. Y.
G. D. Floyd	Toronto, Ontario, Canada
A. P. Fugill	Detroit, Mich.
E. E. George	New York, N. Y.
E. T. B. Gross	Chicago, Ill.
W. D. Hardaway	Denver, Colo.
H. L. Harrington	Buffalo, N. Y.
A. P. Hayward	Pittsburgh, Pa.
R. G. Hooke	Newark, N. J.
L. R. Janes	Chicago, Ill.
A. A. Johnson	East Pittsburgh, Pa.
W. R. Johnson	San Francisco, Calif.
C. B. Kelley	Des Moines, Iowa
C. L. Killgore	Denver, Colo.
M. H. Lovelady	Corpus Christi, Tex.
N. M. Lovell	Tucson, Ariz.
C. W. Mayott	Hartford, Conn.
G. H. McDaniel	New York, N. Y.
W. E. Montgomery	Los Angeles, Calif.
S. B. Morehouse	Philadelphia, Pa.
W. H. Osterle	Pittsburgh, Pa.
H. P. Peters	Atlanta, Ga.
H. W. Phillips	Philadelphia, Pa.
C. B. Signor	Binghamton, N. Y.
W. O. Smith	Mineola, N. Y.
G. M. Tatum	Richmond, Va.
J. J. Tesar	Cleveland, Ohio
R. M. Walker	El Paso, Tex.
C. W. Watchorn	Baltimore, Md.
G. S. Whitlow	St. Louis, Mo.
A. L. Williams	Los Angeles, Calif.

Administrative Subcommittee

Earle Wild, *chairman;* Commonwealth Edison Company, 72 West Adams Street, Room 506, Chicago 90, Ill.

C. P. Almon, Jr.	Chattanooga, Tenn.
R. Brandt	Boston, Mass.
E. E. George	New York, N. Y.
H. L. Harrington	Buffalo, N. Y.
A. P. Hayward	Pittsburgh, Pa.
O. W. Manz, Jr.	Brooklyn, N. Y.
C. W. Mayott	Hartford, Conn.
C. N. Metcalf	New York, N. Y.

System Planning Subcommittee

H. L. Harrington, *chairman;* Buffalo, Niagara and Eastern Power Company, Electric Building, Buffalo 3, N. Y.

C. G. Adams	Schenectady, N. Y.
O. E. Charlton	Birmingham, Ala.
A. G. Dewars	Minneapolis, Minn.
W. B. Fisk	New York, N. Y.
A. P. Fugill	Detroit, Mich.
E. T. B. Gross	Chicago, Ill.
W. D. Hardaway	Denver, Colo.
R. G. Hooke	Newark, N. J.
L. R. James	Chicago, Ill.
A. A. Johnson	East Pittsburgh, Pa.
C. L. Kilgore	Denver, Colo.
A. A. Kroneberg	Los Angeles, Calif.
W. H. Osterle	Pittsburgh, Pa.
H. P. St. Clair	New York, N. Y.
C. B. Signor	Binghamton, N. Y.
N. N. Smeloff	Allentown, Pa.
J. J. Tesar	Cleveland, Ohio
R. M. Walker	El Paso, Tex.
Matthew Ward	Toronto, Ontario, Canada

System Operations Subcommittee

O. W. Manz, Jr., *chairman;* Consolidated Edison Company of New York, Inc., 1 Hudson Avenue, Brooklyn 1, N. Y.

J. Bankus	Portland, Oreg.
M. L. Blair	Spokane, Wash.
O. A. Demuth	Portland, Oreg.
G. D. Floyd	Toronto, Ontario, Canada
W. R. Johnson	San Francisco, Calif.
C. B. Kelley	Des Moines, Iowa
W. E. Montgomery	Los Angeles, Calif.
H. W. Phillips	Philadelphia, Pa.
W. O. Smith	Glenwood Landing, N. Y.
G. M. Tatum	Richmond, Va.

System Economics Subcommittee

A. P. Hayward, *chairman;* Duquesne Light Company, 435—6th Avenue, Pittsburgh 19, Pa.

E. C. Brown	Hartford, Conn.
E. E. George	New York, N. Y.
N. M. Lovell	Tucson, Ariz.
H. P. Peters	Atlanta, Ga.
R. T. Purdy	Chicago, Ill.
J. A. Rose	Newark, N. J.
C. W. Watchorn	Baltimore, Md.
A. L. Williams	Los Angeles, Calif.

System Controls Subcommittee

C. P. Almon, Jr., *chairman;* Tennessee Valley Authority, Chattanooga 4, Tenn.

W. R. Brownlee	Jackson, Mich.
D. H. Cameron	Kansas City, Mo.
T. E. Curtis	San Francisco, Calif.
A. A. Johnson	East Pittsburgh, Pa.
G. H. McDaniel	New York, N. Y.
C. N. Metcalf	New York, N. Y.
S. B. Morehouse	Philadelphia, Pa.
G. S. Whitlow	St. Louis, Mo.

Interconnection Contracts Subcommittee

C. W. Mayott, *chairman;* Hartford Electric Light Company, 266 Pearl Street, Hartford, Conn.

Bradley Cozzens	Los Angeles, Calif.
E. D. Early	Birmingham, Ala.
E. E. George	New York, N. Y.
G. A. Grimm	St. Petersburg, Fla.
C. B. Kelly	Davenport, Iowa
M. H. Lovelady	Corpus Christi, Tex.
Roy Martindale	Los Angeles, Calif.
G. H. McDaniel	New York, N. Y.

Transformers

J. B. Hodtum, *chairman;* Allis-Chalmers Manufacturing Company, N. S. Station, Pittsburgh 12, Pa.

F. J. Vogel, *vice-chairman*	Chicago, Ill
M. K. Brown, *secretary*	Buffalo, N. Y.
J. A. Adams	Philadelphia, Pa.
F. S. Brown	Pittsburgh, Pa.
R. J. Brown	Toronto, Ontario, Canada
J. L. Cantwell	Pittsfield, Mass.
J. H. Chiles	Sharon, Pa.
J. E. Clem	Schenectady, N. Y.
G. W. Clothier	Milwaukee, Wis.
Merrill De Merit	Chattanooga, Tenn.
J. A. Elzi	Jackson, Mich.
M. M. Ewell	Portland, Oreg.
I. W. Gross	New York, N. Y.
W. G. James	Sharon, Pa.
A. A. Johnson	East Pittsburgh, Pa.
H. B. Keath	St. Louis, Mo.
D. L. Levine	Chicago, Ill.
C. M. Lovell	St. Louis, Mo.
M. L. Manning	Canonsburg, Pa.
J. R. Meador	Pittsfield, Mass.
V. M. Montsinger	Pittsfield, Mass.
M. H. Pratt	Syracuse, N. Y.
T. D. Reimers	New York, N. Y.
S. E. Schultz	Portland, Oreg.
W. C. Sealey	Milwaukee, Wis.
F. L. Snyder	Sharon, Pa.
K. M. Stevens	Alhambra, Calif.
J. M. Towner	Baltimore, Md.
P. A. Vance	Fort Wayne, Ind.
H. H. Wagner	Canonsburg, Pa.
C. E. Winegartner	Cleveland, Ohio

Co-ordinated Study of Life of Transformer Insulation Subcommittee

J. L. Cantwell, *chairman;* General Electric Company, 100 Woodlawn Avenue, Pittsfield, Mass.

C. E. Arntzen	East Pittsburgh, Pa.
F. M. Clark	Pittsfield, Mass.
J. F. Dexter	Midland, Mich.
C. F. Hill	East Pittsburgh, Pa.
Myron Kin	Midland, Mich.
V. M. Montsinger	Pittsfield, Mass.
T. D. Reimers	New York, N. Y.
F. L. Snyder	Sharon, Pa.
W. M. Terry, Jr.	Pittsburgh, Pa.
F. J. Vogel	Chicago, Ill.
H. G. Zambell	Pittsburgh, Pa.

Instrument Transformers Subcommittee

J. E. Clem, *chairman;* Central Station Engineering Division, General Electric Company, Schenectady 5, N. Y.

(*Continued in next column*)

Herman Bany	Philadelphia, Pa.
J. H. Chiles	Sharon, Pa.
R. E. Cordray	Philadelphia, Pa.
A. B. Craig	Boston, Mass.
F. E. Davis, Jr.	Jackson, Mich.
K. J. C. Falck	Philadelphia, Pa.
R. E. Franck	Lynn, Mass.
J. M. Geiger	Buffalo, N. Y.
T. R. Halman	Detroit, Mich.
E. K. Huntington	Rochester, N. Y.
L. F. Kennedy	Schenectady, N. Y.
W. E. Marter	Pittsburgh, Pa.
E. V. Sayles	Jackson, Mich.
J. P. Scott	St. Louis, Mo.
F. B. Silsbee	Washington, D. C.
W. K. Sonnemann	Newark, N. J.
W. M. Terry, Jr.	Pittsburgh, Pa.
E. C. Wentz	Sharon, Pa.
C. A. Woods	East Pittsburgh, Pa.

AIEE Papers Subcommittee

J. E. Clem, *chairman;* Central Station Engineering Division, General Electric Company, Schenectady 5, N. Y.

Short-Circuit Thermal Ratings of Induction and Step Type Regulators Subcommittee

A. A. Johnson, *chairman;* Westinghouse Electric Corporation, East Pittsburgh, Pa.

J. E. Clem	Schenectady, N. Y.
W. C. Sealey	Milwaukee, Wis.

Magnetization Characteristics of Transformers Subcommittee

J. E. Clem, *chairman;* Central Station Engineering Divisions, General Electric Company, Schenectady 5, N. Y.

J. A. Elzi	Jackson, Mich.
T. D. Gordy	Pittsfield, Mass.
J. H. Kinghorn	New York, N. Y.
W. E. Marter	Pittsburgh, Pa.
A. J. Maslin	Sharon, Pa.
A. H. Powell	Philadelphia, Pa.
W. C. Sealey	Milwaukee, Wis.
J. M. Wallace	East Pittsburgh, Pa.

Revision of Dielectric Tests Subcommittee

F. J. Vogel, *chairman;* Illinois Institute of Technology, Chicago, Ill.

V. F. Christen	St. Louis, Mo.
I. W. Gross	New York, N. Y.
J. H. Hagenguth	Pittsfield, Mass.
D. M. MacGregor	New York, N. Y.
W. R. McCarty	St. Louis, Mo.
J. R. Meador	Pittsfield, Mass.
C. W. Miller	Sharon, Pa.
V. M. Montsinger	Pittsfield, Mass.
W. C. Sealey	Milwaukee, Wis.
W. L. Teague	Sharon, Pa.

Guides for Operation and Maintenance of Dry-Type Transformers With Class B Insulation

J. A. Adams, *chairman;* Philadelphia Electric Company, 900 Sansom Street, Philadelphia 5, Pa.

G. J. Johnston	Pittsburgh, Pa.
T. D. Reimers	New York, N. Y.
T. E. Rodhouse	Pittsfield, Mass.
W. W. Satterlee	Sharon, Pa.
H. H. Wagner	Canonsburg, Pa.
C. E. Winegartner	Cleveland, Ohio

Co-ordination of Insulation Subcommittee

J. E. Clem, *chairman;* Central Station Engineering Division, General Electric Company, Schenectady 5, N. Y.

Electronic Power Converters Subcommittee

W. C. Sealey, *chairman;* Allis-Chalmers Manufacturing Company, Post Office Box 512, Milwaukee 1, Wis.

Methods of Making Heat Runs Subcommittee

E. Clem, *chairman;* General Electric Company, Central Station Engineering Division, Schenectady 5, N. Y.

J. A. Adams	Philadelphia, Pa.
M. F. Beavers	Pittsfield, Mass.
M. K. Brown	Buffalo, N. Y.
H. B. Keath	St. Louis, Mo.
A. J. Maslin	Sharon, Pa.
W. H. Mutschler	Pittsburgh, Pa.

Power Factor Measurements Subcommittee

J. B. Hodtum *chairman;* Allis-Chalmers Manufacturing Company, N. S. Station, Pittsburgh 12, Pa.

Audible Noise in Transformers Subcommittee

J. H. Chiles, *chairman;* Westinghouse Electric Corporation, Transformer Engineering Department, Sharon, Pa.

J. E. Clem	Schenectady, N. Y.
D. L. Levine	Chicago, Ill.
A. J. Maslin	Sharon, Pa.
J. R. Meador	Pittsfield, Mass.
T. D. Reimers	New York, N. Y.
J. M. Towner	Baltimore, Md.

Transmission and Distribution

I. W. Gross, *chairman;* American Gas and Electric Service Corporation, 30 Church Street, New York 8, N. Y.

S. B. Crary, *vice-chairman*	Schenectady, N. Y.
R. E. Pierce, *secretary*	New York, N. Y.
C. E. Baugh	San Francisco, Calif.
A. E. Beattie	Beaumont, Tex.
P. L. Bellaschi	Sharon, Pa.
H. C. Bingham	Cleveland, Ohio
D. K. Blake	Schenectady, N. Y.
P. W. Blye	New York, N. Y.
C. A. Booker	Boston, Mass.
Bryce Brady	Oklahoma City, Okla.
T. J. Brosnan	Buffalo, N. Y.
Harold Cole	Detroit, Mich.
E. R. Coulbourn	Birmingham, Ala.
A. E. Curtis	Montreal, Quebec, Canada
H. A. Dambly	Philadelphia, Pa.
A. E. Davison	Toronto, Ontario, Canada
T. C. Duncan	New York, N. Y.
H. A. Frey	Baltimore, Md.
J. H. Hagenguth	Pittsfield, Mass.
K. E. Hapgood	Chattanooga, Tenn.
E. R. Hendrickson	Maywood, Ill.
J. B. Hodtum	Pittsburgh, Pa.
E. K. Huntington	Rochester, N. Y.
E. L. Kanouse	Los Angeles, Calif.
C. H. Kraft	St. Louis, Mo.
A. A. Kroneberg	Los Angeles, Calif.
J. T. Lusignan	Mansfield, Ohio
D. W. Nethercut	Elyria, Ohio
J. E. O'Brien	Washington, D. C.
E. W. Oesterreich	Pittsburgh, Pa.
J. S. Parsons	East Pittsburgh, Pa.
C. K. Poarch	New York, N. Y.
L. M. Robertson	Denver, Colo.
R. R. Robinson	Niles, Mich.
W. E. Rushlow	Washington, D. C.
E. V. Sayles	Jackson, Mich.
A. N. Shealy	Baltimore, Md.
F. V. Smith	Chicago, Ill.
K. M. Smith	Chicago, Ill.
E. C. Starr	Corvallis, Oreg.
C. F. Wagner	East Pittsburgh, Pa.
R. L. Witzke	East Pittsburgh, Pa.

Capacitor Subcommittee

F. V. Smith, *chairman;* Sargent and Lundy, 140 South Dearborn Street, Chicago 3, Ill.

M. I. Alimansky	Pittsfield, Mass.
G. Appleman	Allentown, Pa.
C. E. Baugh	San Francisco, Calif.
P. L. Bellaschi	Sharon, Pa.
E. W. Dillard	Boston, Mass.
K. E. Hapgood	Chattanooga, Tenn.
E. R. Hendrickson	Maywood, Ill.
S. Lubin	Washington, D. C.
R. E. Marbury	East Pittsburgh, Pa.
N. A. Miller	Chicago, Ill.
R. B. Miller	Rock Island, Ill.
C. E. Parks	Indianapolis, Ind.
E. V. Sayles	Jackson, Mich.
E. C. Starr	Corvallis, Oreg.
R. A. Wood	South Milwaukee, Wis.

Distribution Subcommittee

T. J. Brosnan, *chairman;* Buffalo Niagara Electric Corporation, 300 Electric Building, Buffalo, N. Y.

(*Continued in next column*)

C. E. Baugh	San Francisco, Calif.
H. C. Bingham	Cleveland, Ohio
D. K. Blake	Schenectady, N. Y.
Bryce Brady	Oklahoma City, Okla.
Harold Cole	Detroit, Mich.
H. A. Dambly	Philadelphia, Pa.
E. R. Hendrickson	Maywood, Ill.
J. B. Hodtum	Pittsburgh, Pa.
E. K. Huntington	Rochester, N. Y.
C. H. Kraft	St. Louis, Mo.
J. E. O'Brien	Washington, D. C.
E. W. Oesterreich	Pittsburgh, Pa.
J. S. Parsons	East Pittsburgh, Pa.
E. V. Sayles	Jackson, Mich.

General Systems Subcommittee

R. L. Witzke, *chairman;* Industry Engineering Department, Westinghouse Electric Corporation, East Pittsburgh, Pa.

P. W. Blye	New York, N. Y.
R. W. Caswell	Chicago, Ill.
O. A. Demuth	Portland, Oreg.
G. D. Floyd	Toronto, Ontario, Canada
R. G. Hooke	Newark, N. J.
E. K. Huntington	Rochester, N. Y.
I. B. Johnson	Schenectady, N. Y.
A. A. Kroneberg	Los Angeles, Calif.
L. B. LeVesconte	Chicago, Ill.
L. M. Robertson	Denver, Colo.
H. M. Trueblood	New York, N. Y.

Lightning and Insulator Subcommittee

J. T. Lusignan, *chairman;* Ohio Brass Company, Engineering Department, Mansfield, Ohio

L. V. Bewley	Bethlehem, Pa.
H. N. Ekvall	Philadelphia, Pa.
H. A. Frey	Baltimore, Md.
I. W. Gross	New York, N. Y.
J. H. Hagenguth	Pittsfield, Mass.
E. L. Harder	East Pittsburgh, Pa.
W. W. Lewis	Schenectady, N. Y.
G. D. McCann, Jr.	Pasadena, Calif.
S. K. Waldorf	Baltimore, Md.
E. R. Whitehead	Chicago, Ill.

Towers, Poles, and Conductors Subcommittee

A. E. Davison, *chairman;* Hydro-Electric Power Commission of Ontario, 620 University Ave., Toronto, Ontario, Canada

H. W. Biskeborn	Newark, Ohio
A. A. Booker	Boston, Mass.
K. E. Hapgood	Chattanooga, Tenn.
E. L. Kanouse	Los Angeles, Calif.
W. H. Knutz	Hammond, Ind.
A. A. Osipovich	Portland, Oreg.
J. A. Rawls	Richmond, Va.
A. S. Runciman	Montreal, Quebec, Canada
F. E. Sanford	Chicago, Ill.
A. N. Shealy	Baltimore, Md.
E. J. Staubitz	Pittsburgh, Pa.
J. Tompkins	Massena, N. Y.

Science and Electronics Group

Science and Electronics Co-ordinating Committee

J. D. Tebo, *chairman,* Bell Telephone Laboratories, Inc., 463 West Street, New York 14, N. Y.

Charles Concordia, *vice-chairman*	Schenectady, N. Y.
W. F. Davidson, *secretary*	New York, N. Y.

Members-at-Large

Walter J. Seeley, representing Board of Directors	Durham, N. C.
I. Melville Stein	Philadelphia, Pa.

Ex-officio members

Chairmen of the technical committees in the Science and Electronics Group

Basic Sciences

Walther Richter, *chairman;* Allis-Chalmers Manufacturing Company, P. O. Box 512, West Allis, Wis.

M. G. Malti, *vice-chairman*	Ithaca, N. Y.
N. S. Hibshman, *secretary*	Brooklyn, N. Y.
J. A. Becker	Murray Hill, N. J.
L. V. Bewley	Bethlehem, Pa.
C. R. Burrows	Ithaca, N. Y.
F. M. Clark	Schenectady, N. Y.
Charles Concordia	Schenectady, N. Y.
P. C. Cromwell	Knoxville, Tenn.

(*Continued on page 1425*)

L. A. Finzi	Pittsburgh, Pa.
E. A. Gaugler	Silver Spring, Md.
W. C. Hall	Washington, D. C.
C. W. Hansell	Rocky Point, N. Y.
F. K. Harris	Washington, D. C.
V. P. Hessler	Urbana, Ill.
T. J. Higgins	Madison, Wis.
G. B. Hoadley	Raleigh, N. C.
W. C. Johnson	Princeton, N. J.
W. A. Lewis, Jr.	Chicago, Ill.
W. H. MacWilliams, Jr.	Whippany, N. J.
P. C. Magnusson	Corvallis, Oreg.
R. C. Mason	East Pittsburgh, Pa.
L. W. Matsch	Chicago, Ill.
G. D. McCann	Pasadena, Cal
K. W. Miller	Chicago, Ill.
W. E. Phillips	Philadelphia, Pa.
H. J. Plumley	Silver Spring, Md.
Dimiter Ramadanoff	Cleveland, Ohio
W. J. Seeley	Durham, N. C.
H. H. Skilling	Stanford, Calif.
Joseph Slepian	East Pittsburgh, Pa.
J. J. Smith	Schenectady, N. Y.
V. G. Smith	Toronto, Ontario, Canada
F. G. Tappan	Norman, Okla.
J. D. Tebo	New York, N. Y.
G. S. Timoshenko	Storrs, Conn.
J. S. Waters	Houston, Tex.
A. H. Waynick	State College, Pa.
T. D. Yensen	East Pittsburgh, Pa.

Applied Mathematics Subcommittee

M. G. Malti, *chairman;* Cornell University, School of Electrical Engineering, Ithaca, N. Y.

P. L. Alger	Schenectady, N. Y.
H. B. Hansteen	Ithaca, N. Y.
E. W. Kimbark	Evanston, Ill.
Joseph Slepian	East Pittsburgh, Pa.
J. J. Smith	Schenectady, N. Y.
Harry Sohon	Philadelphia, Pa.

Electric Circuit Theory Subcommittee

T. J. Higgins, *chairman;* University of Wisconsin, Department of Electrical Engineering, Madison 6, Wis.

J. G. Brainerd	Philadelphia, Pa.
H. J. Carlin	Brooklyn, N. Y.
R. L. Dietzold	Murray Hill, N. J.
E. A. Guillemin	Cambridge, Mass.
F. J. Maginnis	Schenectady, N. Y.
W. O. Osborn	East Pittsburgh, Pa.
E. B. Payne	New York, N. Y.
W. E. Phillips	Philadelphia, Pa.
M. C. Reed	Urbana, Ill.
H. I. Tarpley	State College, Pa.
Ernst Weber	Brooklyn, N. Y.

Energy Sources Subcommittee

L. W. Matsch, *chairman;* Armour Research Foundation, Electrical Engineering Department, 35 West 33rd Street, Chicago 16, Ill.

W. C. Brown	Bradley Beach, N. J.
M. A. Gartens	Washington, D. C.
W. A. Lewis, Jr.	Chicago, Ill.
Walther Richter	Milwaukee, Wis.
J. J. Smith	Schenectady, N. Y.
B. R. Teare	Pittsburgh, Pa.
J. D. Tebo	New York, N. Y.

Electrical Properties of Solids and Liquids Subcommittee

Joseph A. Becker, *chairman;* Bell Telephone Laboratories, Inc., Murray Hill, N. J.

Stephen Angello	East Pittsburgh, Pa.
C. B. Brown	Washington, D. C.
F. M. Clark	Schenectady, N. Y.
W. C. Dunlap	Schenectady, N. Y.
R. W. Engstrom	Lancaster, Pa.
K. Lark-Horovitz	Lafayette, Ind.
P. H. Miller, Jr.	Philadelphia, Pa.
Fred Seitz	Pittsburgh, Pa.
Lloyd Smith	Ithaca, N. Y.
A. von Hippel	Cambridge, Mass.
D. W. Wooldridge	Culver City, Calif.

Magnetics Subcommittee

T. D. Yensen, *chairman;* Westinghouse Research Laboratories, Ardmore Boulevard, East Pittsburgh, Pa.

H. G. Zambell, *secretary*	Pittsburgh, Pa.
J. P. Barton	Pittsburgh, Pa.
R. M. Bozorth	Murray Hill, N. J.
F. J. Brockman	Irvington-on-Hudson, N. Y.
G. H. Cole	Middletown, Ohio
E. A. Gaugler	Silver Spring, Md.

(*Continued in next column*)

S. L. Quimby	New York, N. Y.
W. E. Ruder	Schenectady, N. Y.

The Electrical Properties of Gases Subcommittee

Joseph Slepian, *chairman;* Westinghouse Electric Corporation, Research Laboratories, East Pittsburgh, Pa.

T. B. Jones, *vice-chairman*	Baltimore, Md.
J. D. Cobine	Schenectady, N. Y.
L. H. Fisher	New York, N. Y.
L. H. Germer	New York, N. Y.

Computing Devices

J. G. Brainerd, *chairman;* Moore School of Electrical Engineering, University of Pennsylvania, Philadelphia 4, Pa.

W. H. MacWilliams, Jr., *vice-chairman*	Whippany, N. J.
W. C. Johnson, *secretary*	Princeton, N. J.
S. N. Alexander	Washington, D. C.
S. H. Caldwell	Cambridge, Mass.
Charles Concordia	Schenectady, N. Y.
G. V. Eltgroth	Philadelphia, Pa.
E. L. Harder	East Pittsburgh, Pa.
J. W. Mauchly	Philadelphia, Pa
G. D. McCann	Pasadena, Calif.
M. S. McIllroy	Ithaca, N. Y.
J. C. McPherson	New York, N. Y.
P. L. Morton	Berkeley, Calif.
G. R. Stibitz	Burlington, Vt.

Continuous-Variable Computers Subcommittee

E. L. Harder, *chairman;* Westinghouse Electric Corporation, East Pittsburgh, Pa

(Personnel to be selected)

Digital Computers Subcommittee

John W. Mauchly, *chairman;* Eckert-Mauchly Computer Corporation Broad and Spring Garden Streets, Philadelphia 23, Pa.

Electronic Power Converters

C. C. Herskind, *chairman;* Power Electronics Division, General Electric Company, 1 River Road, Schenectady 5, N. Y.

W. N. Farquhar, *vice-chairman*	Pittsburgh, Pa.
C. R. Marcum, *secretary*	East Pittsburgh, Pa.
H. R. Blomquist	Providence, R. I.
W. F. Bonner	East Newark, N. J.
J. L. Boyer	East Pittsburgh, Pa.
J. H. Cox	Sunnyvale, Calif.
F. W. Cramer	Pittsburgh, Pa.
A. G. Dickinson	Trail, British Columbia, Canada
J. B. Donnelly	Philadelphia, Pa.
I. K. Dortort	Milwaukee, Wis.
William Fraser	Arvida, Quebec, Can.
W. E. Gutzwiller	Milwaukee, Wis.
G. N. Hughes	Alcoa, Tenn.
Otto Jensen	Philadelphia, Pa.
W. K. MacAdam	New York, N. Y.
D. E. Marshall	Bloomfield, N. J.
A. J. Maslin	Sharon, Pa.
L. W. Morton	Schenectady, N. Y.
R. A. Nelson	Pittsfield, Mass.
Waldo Porter	Vancouver, Wash.
M. E. Reagan	East Pittsburgh, Pa.
W. J. L. Rupprecht	Midland, Mich.
August Schmidt, Jr.	Schenectady, N. Y.
W. C. Sealey	Milwaukee, Wis.
F. W. Smith	Easton, Pa.
S. L. Smith	Philadelphia, Pa.
H. C. Steiner	Schenectady, N. Y.
O. J. Swanson	Gary, W. Va.
J. T. Thwaites	Hamilton, Ontario, Canada
D. E. Trucksess	New York, N. Y.
C. H. Willis	Princeton, N. J.
Harold Winograd	Milwaukee, Wis.

Administrative Subcommittee

Main Committee Officers:

C. C. Herskind, *chairman;* General Electric Company, Power Electronics Division, Schenectady 5, N. Y.

W. N. Farquhar, *vice-chairman*	Pittsburgh, Pa.
C. R. Marcum, *secretary*	East Pittsburgh, Pa.
Harold Winograd, *preceding chairman*	Milwaukee, Wis.

(*Continued in next column*)

Subcommittee Chairmen:

J. H. Cox	Sunnyvale, Calif.
I. K. Dortort	Milwaukee, Wis.
R. A. Nelson	Pittsfield, Mass.
August Schmidt, Jr.	Schenectady, N. Y.
D. E. Trucksess	New York, N. Y.

Electronic Converter Application Subcommittee

W. N. Farquhar, *chairman;* Aluminum Company of America, 801 Gulf Building, Pittsburgh 19, Pa.

F. W. Cramer	Pittsburgh, Pa.
J. B. Donnelly	Philadelphia, Pa.
William Fraser	Arvida, Quebec, Canada
C. E. Buterbaugh	
N. W. Geist	
W. E. Gutzwiller	Milwaukee, Wis.
R. N. Hunter	New York, N. Y.
W. K. MacAdam	New York, N. Y.
W. J. L. Rupprecht	Midland, Mich.
S. L. Smith	Philadelphia, Pa.
O. J. Swanson	Gary, W. Va.
J. T. Thwaites	Hamilton, Ontario, Canada

Electronic Converter Circuits Subcommittee

I. K. Dortort, *chairman;* Allis-Chalmers Manufacturing Company, Electrical Department, Milwaukee 1, Wis.

W. F. Bonner	East Newark, N. J.
J. L. Boyer	East Pittsburgh, Pa.
G. N. Hughes	Alcoa, Tenn.
Otto Jensen	Philadelphia, Pa.
W. K. MacAdam	New York, N. Y.
A. H. Mittag	Schenectady, N. Y.
R. A. Nelson	Pittsfield, Mass.
R. J. Pogorzelski	Detroit, Mich.
M. E. Reagan	East Pittsburgh, Pa.
August Schmidt, Jr.	Schenectady, N. Y.

Papers and Speakers Subcommittee

August Schmidt, Jr., *chairman;* General Electric Company, 1 River Road, Schenectady 5, N. Y.

J. B. Rice	West Allis, Wis.
J. T. Thwaites	Hamilton, Ontario, Canada
H. E. Zuvers	Schenectady, N. Y.

Rectifier Transformers Subcommittee

R. A. Nelson, *chairman;* General Electric Company, 100 Woodlawn Avenue, Pittsfield, Mass.

F. D. Kaiser	Sharon, Pa.
A. J. Maslin	Sharon, Pa.
W. C. Sealey	Milwaukee, Wis.

Rectifying Devices Subcommittee

Harold Winograd, *chairman;* Allis-Chalmers Manufacturing Company, Electrical Department, Milwaukee 1, Wis.

D. V. Edwards	Newark, N. J.
D. E. Marshall	Bloomfield, N. J.
H. C. Steiner	Schenectady, N. Y.

West Coast Subcommittee

J. H. Cox, *chairman;* Westinghouse Electric Corporation, Sunnyvale, Calif.

W. M. Allen	Portland, Oreg.
E. D. Barcus	Los Angeles, Calif.
E. S. Benson	San Francisco, Calif.
H. L. Burris	Pittsburg, Calif.
R. J. Corfield	Garfield, Utah
R. H. Denyer	Portland, Oreg.
A. G. Dickinson	Trail, British Columbia, Canada
E. F. Donatic	Ontario, Calif.
Alex. Dovjikov	Portland, Oreg.
R. H. Duguid	Los Angeles, Calif.
S. R. Durand	Milwaukee, Wis.
L. E. Dye	Los Angeles, Calif.
T. F. Hadwin	Vancouver, British Columbia, Canada
D. R. Hoopes	Salt Lake City, Utah
J. V. Kresser	San Francisco, Calif.
C. A. Langlois	Portland, Oreg.
H. F. Lickey	Pullman, Wash.
M. McCoy	Portland, Oreg.
A. L. Munn	San Leandro, Calif.
S. J. Pope	Spokane, Wash.
Waldo Porter	Vancouver, Wash.
H. H. Skilling	Stamford, Calif.
E. C. Starr	Corvallis, Oreg.
H. V. Strandberg	Seattle, Wash.
J. R. Tenney	Geneva, Utah

Hot-Cathode Power Converter Subcommittee

D. E. Trucksess, *chairman;* Bell Telephone Laboratories, Inc., 463 West Street, New York 14, N. Y.

H. R. Butler	Newark, N. J.
F. W. Cramer	Pittsburgh, Pa.
E. V. DeBlieux	Pittsfield, Mass.
D. V. Edwards	Newark, N. J.
C. E. Hamann	Lynn, Mass.
J. F. Harris	Newark, N. J.
E. W. Hutton	Schenectady, N. Y.
W. E. Large	East Pittsburgh, Pa.
F. G. Logan	St. Louis, Mo.
Roman Pogorzelski	Detroit, Mich.
E. J. Rathsack	Milwaukee, Wis.
F. M. Rives	Schenectady, N. Y.
W. C. Rudd	Brooklyn, N. Y.
J. W. Sanborn	Camden, N. J.
H. C. Steiner	Schenectady, N. Y.
H. L. Thorson	Schenectady, N. Y.
J. C. Walter	Washington, D. C.

Electronics

W. G. Dow, *chairman;* Department of Electrical Engineering, University of Michigan, Ann Arbor, Mich.

J. T. Thwaites, *vice-chairman* (East)	Hamilton, Ontario, Canada
A. M. Zarem, *vice-chairman* (West)	Los Angeles, Calif.
E. M. Boone, *secretary*	Columbus, Ohio
W. R. G. Baker	Syracuse, N. Y.
Rawson Bennett	San Diego, Calif.
C. J. Breitwieser	San Diego, Calif.
C. B. Brown	Washington, D. C.
R. S. Burnap	Harrison, N. J.
A. H. Canada	Schenectady, N. Y.
W. R. Clark	Philadelphia, Pa.
L. G. Cowles	Houston, Tex.
C. F. Crandell	Dallas, Tex.
G. J. Crowdes	Cambridge, Mass.
A. V. Eastman	Seattle, Wash.
G. V. Eltgroth	Philadelphia, Pa.
Rudolf Feldt	Clifton, N. J.
G. H. Fett	Urbana, Ill.
D. G. Fink	New York, N. Y.
H. L. Flowers	Washington, D. C.
W. A. Geohegan	New York, N. Y.
E. R. Haberland	Silver Spring, Md.
E. L. Harder	East Pittsburgh, Pa.
G. W. Hewitt	East Pittsburgh, Pa.
J. E. Hobson	Stanford, Calif.
J. R. Hyneman	New York, N. Y.
T. P. Kinn	Baltimore, Md.
K. S. Koon	Endicott, N. Y.
W. A. Leidigh, Jr.	Portland, Oreg.
O. W. Livingston	Schenectady, N. Y.
D. E. Marshall	Bloomfield, N. J.
R. C. Mason	East Pittsburgh, Pa.
S. T. Maunder	Pittsfield, Mass.
H. R. Meahl	Schenectady, N. Y.
H. R. Mimno	Cambridge, Mass.
G. A. Morton	Princeton, N. J.
R. E. Mumma	Dayton, Ohio
B. K. Northrop	Ithaca, N. Y.
J. K. Nunan	Hollywood, Calif.
David Packard	Palo Alto, Calif.
H. L. Palmer	Schenectady, N. Y.
G. W. Penney	Pittsburgh, Pa.
W. H. Pickering	Pasadena, Calif.
O. W. Pike	Schenectady, N. Y.
A. J. Quigley	Notre Dame, Ind.
Simon Ramo	Culver City, Calif.
J. G. Reid, Jr.	Washington, D. C.
L. E. Reukema	Berkeley, Calif.
R. W. Reynolds	Wilmington, Del.
J. D. Ryder	Urbana, Ill.
Hubert Sharp	Denver, Colo.
M. R. Shaw	Corning, N. Y.
E. T. Sherwood	Milwaukee, Wis.
S. W. Smith	Washington, D. C.
E. P. Spandau	Dayton, Ohio
H. C. Steiner	Schenectady, N. Y.
C. M. Summers	Fort Wayne, Ind.
E. R. Thomas	New York, N. Y.
F. N. Tompkins	Providence, R. I.
D. E. Trucksess	New York, N. Y.
D. C. Ulrey	Lancaster, Pa.
R. F. Wall	College Station, Tex.
A. S. Waxman	Sparrows Point, Md.
W. C. White	Schenectady, N. Y.
C. H. Willis	Princeton, N. J.
D. G. Wilson	Lawrence, Kans.
Harold Winograd	Milwaukee, Wis.

Electronic Papers, Meetings, and Section Contacts Subcommittee

W. C. White, *chairman;* General Electric

(*Continued on page 1426*)

Research Laboratory, The Knolls, Schenectady, N. Y.
(Personnel to be selected)

Electronic Standards and Definitions Subcommittee

O. W. Pike, *chairman;* Vacuum Tube Engineering Department, General Electric Company, Schenectady 5, N. Y.
(Personnel to be selected)

Electronic Education Subcommittee

J. D. Ryder, *chairman;* Department of Electrical Engineering, Iowa State College, Ames, Iowa
(Personnel to be selected)

Liaison With IRE Subcommittee

R. S. Burnap, *chairman;* RCA Victor Division, Radio Corporation of America, Harrison, N. J.

Liaison With JETEC Subcommittee

O. W. Pike, *chairman;* Vacuum Tube Engineering Department, General Electric Company, Schenectady 5, N. Y.

Electron Tubes Subcommittee

H. C. Steiner, *chairman;* Electrical Department, General Electric Company Schenectady 5, N. Y.
(Personnel to be selected)

X-Ray Tubes, Apparatus, and Applications Subcommittee

Scott W. Smith, *chairman;* National Bureau of Standards, Washington, D. C.

(Personnel to be selected)

Electronic Precipitation Subcommittee

G. W. Hewitt, *chairman;* Westinghouse Electric Corporation, East Pittsburgh, Pa.

(Personnel to be selected)

High-Frequency Conductors, Cables, and Connectors Subcommittee

G. J. Crowdes, *chairman;* Simplex Wire and Cable Company, 79 Sidney Street, Cambridge 39, Mass.

(Personnel to be selected)

Liaison With Nucleonics Subcommittee

G. A. Morton, *chairman;* RCA Laboratories, Princeton, N. J.

Electronic Aids to Navigation Subcommittee

H. R. Mimno, *chairman;* Gordon McKay Professor, Engineering Sciences and Applied Physics Department, Harvard University, Cambridge, Mass.

Electronic Aids to Geophysics Subcommittee

L. G. Cowles, *chairman;* Superior Oil Company, Houston, Tex.

Infrared Applications Subcommittee

A. H. Canada, *chairman;* General Engineering Laboratory, General Electric Company, Schenectady 5, N. Y.

(Personnel to be selected)

Electronic Processing and Preservation of Foods and Biological Products Subcommittee

(Personnel to be selected)

Magnetic Amplifiers Subcommittee

E. L. Harder, *chairman;* Industry Engineering Department—11-L, Westinghouse Electric Corporation, East Pittsburgh, Pa.

(Personnel to be selected)

Applications of Semiconductor Devices Subcommittee

C. B. Brown, *chairman;* Naval Ordnance Laboratory, Washington, D. C.

(Personnel to be selected)

Electronic Heating Liaison Subcommittee

T. P. Kinn, *chairman;* Westinghouse Electric Corporation, 2519 Wilkens Avenue, Baltimore 3, Md.

Instruments and Measurements

W. R. Clark, *chairman;* Leeds and Northrup Company, 4901 Stenton Avenue, Philadelphia 44, Pa.

J. H. Miller, *vice-chairman* (East)	Newark, N. J.
J. E. Hobson, *vice-chairman* (West)	Stanford, Calif.
J. G. Reid, Jr., *secretary*	Washington, D. C.
P. L. Bellaschi	Sharon, Pa.
H. W. Berry	Camden, N. J.
P. A. Borden	Waterbury, Conn.
D. L. Brown	Portland, Oreg.
G. S. Brown	Cambridge, Mass.
A. L. Brownlee	Chicago, Ill.
L. A. Burckmyer, Jr.	Ithaca, N. Y.
R. T. Capodanno	Philadelphia, Pa.
J. H. Chiles, Jr.	Sharon, Pa.
J. El Clem	Schenectady, N. Y.
A. B. Craig	Boston, Mass.
C. L. Dawes	Cambridge, Mass.
F. C. Doble	Belmont, Mass.
E. D. Doyle	Philadelphia, Pa.
C. K. Duff	Toronto, Ontario, Canada
C. A. Duke	Chattanooga, Tenn.
G. W. Dunlap	Schenectady, N. Y.
W. N. Eddy	Cambridge, Mass.
G. V. Eltgroth	Philadelphia, Pa.
Rudolf Feldt	Clifton, N. J.
F. J. Gaffney	Brooklyn, N. Y.
W. A. Geohegan	New York, N. Y.
T. S. Gray	Cambridge, Mass.
E. I. Green	New York, N. Y.
G. B. Hoadley	Raleigh, N. C.
A. J. Hornfeck	Cleveland, Ohio
W. G. Knickerbocker	Detroit, Mich.
A. E. Knowlton	New York, N. Y.
H. C. Koenig	New York, N. Y.
E. S. Lee	Schenectady, N. Y.
B. E. Lenehan	Newark, N. J.
W. N. Lindblad	Emeryville, Calif.
J. T. Lusignan, Jr.	Mansfield, Ohio
Harold Lyons	Washington, D. C.
L. W. Matsch	Chicago, Ill.
Whitney Matthews	Washington, D. C.
W. J. Mayo-Wells	Silver Spring, Md.
David Packard	Palo Alto, Calif.
H. J. Plumley	Silver Spring, Md.
C. F. Savage	Lynn, Mass.
J. P. Scott	St. Louis, Mo.
F. B. Silsbee	Washington, D. C.
D. B. Sinclair	Cambridge, Mass.
Harry Sohon	Philadelphia, Pa.
M. B. Stout	Ann Arbor, Mich.
J. M. Vanderleck	Toronto, Ontario, Canada
A. H. Wing, Jr.	Evanston, Ill.
C. J. Zeller	New York, N. Y.

Organization Subcommittee

T. S. Gray, *chairman;* Massachusetts Institute of Technology, Cambridge 39, Mass.

C. F. Savage, *secretary*	Marblehead, Mass.
E. I. Green	New York, N. Y.
H. C. Koenig	New York, N. Y.
J. G. Reid, Jr.	Washington, D. C.

Revision of Standard Number 4 Subcommittee (Measurement of Test Voltage in Dielectric Tests)

J. T. Lusignan, *chairman;* Ohio Brass Company, Mansfield, Ohio

P. L. Bellaschi	Sharon, Pa.
A. L. Brownlee	Chicago, Ill.
J. S. Carroll	Stanford University, Calif.
J. J. Clark	East Pittsburgh, Pa.
J. E. Clem	Schenectady, N. Y.
E. W. Davis	Cambridge, Mass.
C. M. Foust	Schenectady, N. Y.
H. A. Frey	Baltimore, Md.
I. W. Gross	New York, N. Y.
J. H. Hagenguth	Pittsfield, Mass.
W. B. Kouwenhoven	Baltimore, Md.
R. S. Lapp	LeRoy, N. Y.
W. W. Lewis	Schenectady, N. Y.
M. Newman	Minneapolis, Minn.
J. H. Park	Washington, D. C.
W. C. Sealey	Milwaukee, Wis.
W. T. Smith	Washington, D. C.
R. W. Sorensen	Pasadena, Calif.
W. L. Teague	Sharon, Pa.
S. W. Zimmerman	Ithaca, N. Y.

(*Continued in next column*)

Watthour Meters Subcommittee

C. F. Savage, *chairman;* General Electric Company, Lynn, Mass.

H. A. Brown	Rochester, N. Y.
D. T. Canfield	Lafayette, Ind.
A. B. Craig	Boston, Mass.
C. K. Duff	Toronto, Ontario, Canada
F. C. Holtz	Springfield, Ill.
W. G. Knickerbocker	Detroit, Mich.
H. C. Koenig	New York, N. Y.
B. E. Lenehan	Newark, N. J.
F. H. Rogers	Baltimore, Md.
M. B. Stout	Ann Arbor, Mich.

Definitions Subcommittee

C. L. Dawes, *chairman;* Harvard University, Cambridge 39, Mass.

J. E. Clem	Schenectady, N. Y.
A. B. Craig	Boston, Mass.
H. C. Dickinson	Lynn, Mass.
W. N. Goodwin, Jr.	Newark, N. J.
E. S. Lee	Schenectady, N. Y.
B. E. Lenehan	Newark, N. J.
P. MacGahan	Haines Falls, N. Y.
F. B. Silsbee	Washington, D. C.

Dielectric Measurements in the Field Subcommittee

F. C. Doble, *chairman;* Doble Engineering Company, Belmont 78, Mass.

D. L. Brown	Portland, Oreg.
A. L. Brownlee	Chicago, Ill.
E. B. Curdts	Philadelphia, Pa.
C. L. Dawes	Cambridge, Mass.
E. D. Doyle	Philadelphia, Pa.
W. F. Dunkle	Hazleton, Pa.
I. G. Easton	New York, N. Y.
W. N. Eddy	Cambridge, Mass.
I. W. Gross	New York, N. Y.
E. S. Lee	Schenectady, N. Y.
W. N. Lindblad	Emeryville, Calif.
G. M. L. Sommerman	Evanston, Ill.
Lynn Wetherill	Pittsfield, Mass.

Master Test Code for Temperature Measurements Subcommittee

L. A. Burckmyer, Jr., *chairman;* Cornell University, Ithaca, N. Y.

P. A. Borden	Waterbury, Conn.
E. D. Doyle	Philadelphia, Pa.
J. L. Fuller	Cleveland, Ohio
W. R. Hough	Cleveland, Ohio
A. K. Joecks	New York, N. Y.
M. D. Ross	East Pittsburgh, Pa.
S. S. Stack	Schenectady, N. Y.

High-Frequency Measurements Subcommittee

H. Lyons, *chairman;* Microwave Measurement Laboratory, National Bureau of Standards, Washington, D. C.

R. T. Capodanno	Philadelphia, Pa.
F. L. Creager	Princeton, N. J.
I. G. Easton	New York, N. Y.
E. P. Felch	New York, N. Y.
F. J. Gaffney	Brooklyn, N. Y.
E. I. Green	New York, N. Y.
F. Hamburger, Jr.	Baltimore, Md.
G. B. Hoadley	Brooklyn, N. Y.
H. R. Meahl	Schenectady, N. Y.
H. J. Plumley	Silver Spring, Md.
H. E. Webber	Great Neck, N. Y.
Ernst Weber	Brooklyn, N. Y.
H. A. Wheeler	Great Neck, N. Y.
A. J. Wing, Jr.	Evanston, Ill.

Marking of Varmeters and Related Instruments

A. B. Craig, *chairman;* Boston Edison Company, 39 Boylston Street, Boston 12, Mass.

A. L. Brownlee	Chicago, Ill.
A. L. Carvill	New York, N. Y.
W. G. Knickerbocker	Detroit, Mich.
A. E. Knowlton	New York, N. Y.
F. A. Redding	Los Angeles, Calif.
F. B. Silsbee	Washington, D. C.
H. B. Smith	Buffalo, N. Y.
J. S. Wantland	Oklahoma City, Okla.

Co-operation With Instrument Society of America Subcommittee

J. P. Scott, *chairman;* Union Electric Company of Missouri, 414 North Tenth Street, St. Louis, Mo.

(*Continued in next column*)

W. R. Clark	Philadelphia, Pa.
J. G. Reid, Jr.	Washington, D. C.

Metallic Rectifiers

L. O. Grondahl, *chairman;* Union Switch and Signal Company and Westinghouse Air Brake Company, Pittsburgh 18, Pa.

I. R. Smith, *vice-chairman*	Buffalo, N. Y.
E. A. Harty, *secretary*	West Lynn, Mass.
W. F. Bonner	East Newark, N. J.
L. W. Burton	Lynn, Mass.
P. G. Cobb	Newark, N. J.
M. E. Gamble	Indianapolis, Ind.
K. S. Geiges	New York, N. Y.
E. A. Groeteke	St. Louis, Mo.
C. E. Hamann	Lynn, Mass.
C. W. Hansell	Rocky Point, N. Y.
C. C. Herskind	Schenectady, N. Y.
W. E. Phillips	Philadelphia, Pa.
N. Y. Priessman	Murray Hill, N. J.
Glen Ramsey	North Chicago, Ill.
D. E. Trucksess	New York, N. Y.
J. L. Wagner	Endicott, N. Y.
E. D. Wilson	East Pittsburgh, Pa.
Stanley Zwerling	Laurelton, N. Y.

Standard Definitions Subcommittee

I. R. Smith, *chairman;* Westinghouse Electric Corporation, Box 2025, Buffalo 5, N. Y.
(Personnel to be selected)

New Definitions Subcommittee

D. E. Trucksess, *chairman;* Bell Telephone Laboratories, Inc., 463 West Street, New York 14, N. Y.
(Personnel to be selected)

Bibliography Subcommittee

E. A. Harty, *chairman;* Lightning Engineering Division, General Electric Company, River Works, West Lynn 3, Mass.
(Personnel to be selected)

Test Standards Subcommittee

W. F. Bonner, *chairman;* Federal Telephone and Radio Corporation, 900 Passaic Avenue, East Newark, N. J.
(Personnel to be selected)

Patent Bibliography Subcommittee

Glen Ramsey, *chairman;* Fansteel Metallurgical Corporation, Rectifier Division, North Chicago, Ill.
(Personnel to be selected)

Papers and Speakers Subcommittee

N. Y. Priessman, *chairman;* Bell Telephone Laboratories, Inc., Murray Hill, N. J.
(Personnel to be selected)

Miniature Rectifiers Subcommittee

P. G. Cobb, *chairman;* Weston Electrical Instrument Corporation, 614 Frelinghuysen Avenue, Newark 5, N. J.
(Personnel to be selected)

Nucleonics

J. J. Smith, *chairman;* General Engineering and Consulting Laboratory, General Electric Company, Schenectady 5, N. Y.

W. F. Davidson, *vice-chariman*	New York, N. Y.
P. N. Ross, *secretary*	Pittsburgh, Pa.
W. E. Barbour, Jr.	Boston, Mass.
H. W. Bibber	Schenectady, N. Y.
H. W. Brackney	Upton, N. Y.
L. J. Bulliet	Rockford, Ill.
J. M. Cork	Ann Arbor, Mich.
G. W. Dunlap	Schenectady, N. Y.
J. R. Dunning	New York, N. Y.
I. A. Getting	Cambridge, Mass.
T. S. Gray	Cambridge, Mass.
M. M. Hubbard	Cambridge, Mass.
H. L. Hull	Chicago, Ill.
T. R. Kohler	Irvington-on-Hudson, N. Y.
E. H. Krieg	Boston, Mass.
R. G. Lorraine	Schenectady, N. Y.

(*Continued on page 1427*)

H. B. Marvin	Syracuse, N. Y.
J. K. Pickard	Washington, D. C.
Walther Richter	Milwaukee, Wis.
Frederick Seitz	Urbana, Ill.
W. E. Shoupp	Homestead, Pa.
Abe Tilles	San Francisco, Calif.
J. G. Trump	Cambridge, Mass.
R. E. Watson	Philadelphia, Pa.
J. T. Wilson	Milwaukee, Wis.

ISOTOPIC TRACERS SUBCOMMITTEE

W. E. Barbour, Jr., *chairman;* Tracerlab, Inc., 55 Oliver Street, Boston 10, Mass.

A. J. Allen	Pittsburgh, Pa.
J. T. Burwell, Jr.	Cambridge, Mass.
C. Herzog	Bellaire, Tex.
M. D. Kamen	St. Louis, Mo.
W. W. Miller	Upton, N. Y.
Morris Muskat	Pittsburgh, Pa.
A. F. Reid	Dallas, Tex.
R. E. Watson	Philadelphia, Pa.
Nathan Woodruff	Oak Ridge, Tenn.

STUDIES OF NUCLEAR THEORY SUBCOMMITTEE

J. T. Wilson, *chairman;* Allis-Chalmers Manufacturing Company, Milwaukee 1, Wis.

(Personnel to be selected)

Therapeutics

H. D. Moreland, *chairman;* Westinghouse Electric Corporation, 2519 Wilkens Avenue, Baltimore 3, Md.

S. Reid Warren, Jr., *vice-chairman*	Philadelphia, Pa.
Scott W. Smith, *secretary*	Washington, D. C.
F. B. Claussen	Portland, Oreg.
M. J. Gross	Milwaukee, Wis.
W. B. Kouwenhoven	Baltimore, Md.
R. H. Morgan	Baltimore, Md.
T. H. Rogers	Springdale, Conn.
C. M. Slack	Bloomfield, N. J.
W. R. Smith	Newark, N. J.
R. E. Watson	Philadelphia, Pa.

No subcommittees

Joint Subcommittees

JOINT SUBCOMMITTEE ON APPLICATION OF PROBABILITY METHODS TO POWER-SYSTEM PROBLEMS (JOINT WITH POWER GENERATION AND SYSTEM ENGINEERING COMMITTEES)

G. Calabrese, *chairman;* Electrical Engineering Department, New York University, University Heights, New York, N. Y.

V. A. Thiemann, *secretary*	New York, N. Y.
W. R. Blakeley	Toronto, Ontario, Canada
Howard Duryea	Birmingham, Ala.
R. G. Hooke	Newark, N. J.
H. A. Lott	Los Angeles, Calif.
W. J. Lyman	Pittsburgh, Pa.
H. W. Phillips	Philadelphia, Pa.
C. A. Roberts	Chattanooga, Tenn.
H. B. Smith	Buffalo, N. Y.
H. P. St. Clair	New York, N. Y.
G. M. Tatum	Richmond, Va.
C. W. Watchorn	Baltimore, Md.

JOINT SUBCOMMITTEE ON CARBON BRUSHES (JOINT AIEE TECHNICAL COMMITTEES ON AIR TRANSPORTATION AND ROTATING MACHINERY, AND IN CO-OPERATION WITH NEMA ON CODES)

T. M. Linville, *chairman;* General Electric Company, 1 River Road, Schenectady 5, N. Y.

W. H. Austry	St. Marys, Pa.
H. R. Brown	Seattle, Wash.
W. E. Clancy (NEMA)	St. Marys, Pa.
J. V. Dobson	St. Marys, Pa.
H. M. Elsey	East Pittsburgh, Pa.
C. J. Herman	Fort Wayne, Ind.
V. P. Hessler	Urbana, Ill.
L. H. Hildebrandt	Dayton, Ohio
H. E. Keneipp	Lima, Ohio
E. A. Lapham	Long Island City, N. Y.
D. Ramadanoff	Cleveland, Ohio
O. C. Rutledge (NEMA)	Schenectady, N. Y.
S. D. Summers	Washington, D. C.
A. L. Van Emden	Washington, D. C.

JOINT SUBCOMMITTEE ON NUCLEONIC INSTRUMENTS (JOINT WITH NUCLEONICS AND INSTRUMENTS AND MEASUREMENTS COMMITTEES)

(Continued in next column)

G. W. Dunlap, *chairman;* General Engineering and Consulting Laboratory, General Electric Company, Schenectady 5, N. Y.

E. E. Goodale, *secretary*	Schenectady, N. Y.
W. E. Barbour, Jr.	Boston, Mass.
L. F. Curtiss	Washington, D. C.
F. J. Gaffney	Brooklyn, N. Y.
W. H. Hall	Syracuse, N. Y.
M. M. Hubbard	Cambridge, Mass.
P. S. Johnson	Washington, D. C.
J. B. H. Kuper	Upton, N. Y.
F. D. Lewis	Cambridge, Mass.
G. A. Morton	Princeton, N. J.
M. A. Princi	West Lynn, Mass.
M. A. Schultz	Pittsburgh, Pa.
R. E. Watson	Philadelphia, Pa.
H. G. Weiss	Los Alamos, N. Mex.

JOINT SUBCOMMITTEE ON ELECTRONIC INSTRUMENTS (JOINT WITH ELECTRONICS AND INSTRUMENTS AND MEASUREMENTS COMMITTEES)

J. G. Reid, Jr., *chairman;* National Bureau of Standards, Division 13.1, Washington 25, D. C.

Rudolf Feldt, *vice-chairman*	Clifton, N. J.
W. H. Tidd, *secretary*	New York, N. Y.
G. E. Beggs, Jr.	Philadelphia, Pa.
H. W. Berry	Camden, N. J.
C. C. Chambers	Philadelphia, Pa.
H. F. Dart	Bloomfield, N. J.
G. V. Eltgroth	Philadelphia, Pa.
D. B. Fisk	West Lynn, Mass.
W. A. Geohegan	New York, N. Y.
Truman S. Gray	Cambridge, Mass.
E. R. Haberland	Silver Spring, Md.
C. E. Hastings	Hampton, Va.
G. B. Hoadley	Raleigh, N. C.
E. J. Isbister	Garden City, N. Y.
C. A. Mabey	West Orange, N. J.
G. N. Mahaffey	Washington, D. C.
C. W. Martel	Newton, Mass.
N. W. Matthews	Washington, D. C.
P. K. McElroy	Cambridge, Mass.
H. R. Meahl	Schenectady, N. Y
David Packard	Palo Alto, Calif.
T. B. Perkins	Harrison, N. J.
J. J. Slattery	Belmar, N. J.
E. R. Thomas	New York, N. Y.
J. T. Thwaites	Hamilton, Ontario, Canada
A. H. Waynick	State College, Pa.
W. P. Wills	Philadelphia, Pa.

(Continued in next column)

JOINT SUBCOMMITTEE ON TELEMETERING (JOINT WITH INSTRUMENTS AND MEASUREMENTS, CARRIER CURRENT, AND SUBSTATIONS COMMITTEES)

C. K. Duff, *chairman;* Hydro-Electric Power Commission, 620 University Avenue, Toronto, Ontario, Canada

J. L. McKee, *secretary*	New York, N. Y.
J. L. Blackburn	Newark, N. J.
P. A. Borden	Waterbury, Conn.
M. A. Bostwick	Newark, N. J.
E. C. Brown	Hartford, Conn.
W. A. Derr	East Pittsburgh, Pa.
A. J. Hornfeck	Cleveland, Ohio
G. S. Lunge	Schenectady, N. Y.
E. E. Lynch	West Lynn, Mass.
W. J. Mayo-Wells	Silver Spring, Md.
W. E. Phillips	Philadelphia, Pa.
Merle Robison	Toledo, Ohio
G. M. Thynell	Waterbury, Conn.
J. H. Vivian	Los Angeles, Calif.
B. G. Walker	Minneapolis, Minn.
C. E. Winegartner	Cleveland, Ohio

JOINT SUBCOMMITTEE ON ELECTRICAL AIDS TO MEDICINE (JOINT WITH ELECTRONICS, INSTRUMENTS AND MEASUREMENTS, AND THERAPEUTICS COMMITTEES)

Dr. W. A. Geohegan, *chairman;* Department of Anatomy, Cornell University Medical College, 1300 York Avenue, New York 21, N. Y.

G. W. Dunlap	Schenectady, N. Y.
Dr. Harry Grundfest	New York, N. Y.
Dr. J. P. Hervey	Baltimore, Md.
L. W. Matsch	Chicago, Ill.
G. A. Morton	Princeton, N. J.
J. G. Reid, Jr.	Washington, D.C.
S. W. Smith	Washington, D. C.
Dr. C. A. Tobias	Berkeley, Calif.
S. R. Warren, Jr.	Philadelphia, Pa.
A. M. Zarem	Los Angeles, Calif.

Institute Representatives

Aeronautical Electrical Equipment Standardization

W. V. Boughton, liaison representative with NASC and SAE	South Pasadena, Calif.

Alfred Noble Prize Committee, ASCE

T. M. Linville	Schenectady, N. Y.

American Association for the Advancement of Science, Council

J. W. Barker	New York, N. Y.
I. Melville Stein	Philadelphia, Pa.

American Research Committee on Grounding

C. T. Sinclair	Pittsburgh, Pa.

American Standards Association, Standards Council

F. E. Harrell	Cleveland, Ohio
Charles Rufus Harte	New Haven, Conn.
R. T. Henry	Buffalo, N. Y.

Alternates

R. C. Bergvall	East Pittsburgh, Pa.
J. R. North	Jackson, Mich.
E. B. Paxton	Schenectady, N. Y.

American Year Book, Advisory Board

H. H. Henline	New York, N. Y.

Construction Industry Advisory Council

Henry A. Lardner	New York, N. Y.

Electrical Standards Committee, ASA

John Grotzinger	Akron, Ohio
F. E. Harrell	Cleveland, Ohio
J. J. Pilliod	New York, N. Y.

Alternates

H. E. Farrer	New York, N. Y.
I. W. Gross	New York, N. Y.
E. B. Paxton	Schenectady, N. Y.

Division Assignment

Division on Communication and Electronics

H. E. Farrer	New York, N. Y.
F. E. Harrell	Cleveland, Ohio
J. J. Pilliod	New York, N. Y.

Division on Power

I. W. Gross	New York, N. Y.
John Grotzinger	Akron, Ohio
E. B. Paxton	Schenectady, N. Y.

Engineering Foundation Board

J. W. Barker	New York, N. Y.
C. G. Suits	Schenectady, N. Y.

Engineering Societies Monographs Committee

W. I. Slichter	New York, N. Y.

Engineers' Council for Professional Development

M. S. Coover	Ames, Iowa
E. W. Davis	Cambridge, Mass.
M. D. Hooven	Newark, N. J.

Engineers Joint Council

J. F. Fairman	New York, N. Y.
H. H. Henline	New York, N. Y.
B. D. Hull	Dallas, Tex.
Everett S. Lee	Schenectady, N. Y.

General Electric Educational Fund Fellowship Committee

J. F. Fairman	New York, N. Y.

Hertz Award Committee

M. S. Coover	Ames, Iowa

Hoover Medal Board of Award

H. S. Osborne	New York, N. Y.
David C. Prince	Schenectady, N. Y.
J. G. Tarboux	Ithaca, N. Y.

Industry Committee on Interior Wiring Design

M. M. Brandon	New York, N. Y.
L. C. Peterman	New York, N. Y.

John Fritz Medal Board of Award

J. F. Fairman	New York, N. Y.
J. Elmer Housley	Alcoa, Tenn.
B. D. Hull	Dallas, Tex.
Everett S. Lee	Schenectady, N. Y.

Joint Committee for Development of Statistical Applications in Engineering and Manufacturing

W. P. Dobson	Toronto, Ontario, Canada

Library Board, United Engineering Trustees, Inc.

N. S. Hibshman	Brooklyn, N. Y.
George Sutherland	New York, N. Y.

Marston Medal Board of Award

Harry B. Gear	Chicago, Ill.

National Association of Corrosion Engineers, Inter-Society Committee on Corrosion

L. J. Gorman	New York, N. Y.
H. M. Trueblood	Dobbs Ferry, N. Y.

National Bureau of Engineering Registration, Advisory Board

Elgin B. Robertson	Dallas, Tex.

National Electronics Conference, Board of Directors

W. G. Dow	Ann Arbor, Mich.

National Fire Protection Association, Electrical Committee

W. B. Kouwenhoven	Baltimore, Md.
Robin Beach, alternate	Brooklyn, N. Y.

National Fire Waste Council

Robin Beach	Brooklyn, N. Y.
W. B. Kouwenhoven	Baltimore, Md.

National Research Council, Division of Engineering and Industrial Research

W. B. Kouwenhoven	Baltimore, Md.

National Technological Advisory Committee Quarterly of Applied Mathematics

M. G. Malti	Ithaca, N. Y.

United Engineering Trustees, Inc.

J. F. Fairman	New York, N. Y.
W. H. Harrison	New York, N. Y.
D. A. Quarles	New York, N. Y.

United States National Committee of the International Commission on Illumination

Ward Harrison	Cleveland, Ohio
Harris Reinhardt	Flushing, N. Y.
E. H. Salter	New York, N. Y.

United States National Committee of the International Electrotechnical Commission

W. P. Dobson	Toronto, Ontario, Canada
H. E. Farrer	New York, N. Y.
F. E. Harrell	Cleveland, Ohio
E. B. Paxton	Schenectady, N. Y.
J. J. Pilliod	New York, N. Y.

Washington Award Commission

W. A. Lewis, Jr.	Chicago, Ill.
Frank V. Smith	Chicago, Ill.

World Power Conference, Executive Committee of United States National Committee

J. F. Fairman	New York, N. Y.

AIEE Transactions Index for 1949

(Complete Index for Parts I and II of Volume 68)

1. Technical Subject Index

Accessory Design Utilizing New Laboratory Techniques, Cable. Bosworth, Farr.......1268–73; disc. 1273
Adjustable-Speed Drive, A Wide-Range. Conrad, Tribken..............................789–90
Adjustable Speed Motors, Limitations in Design of D-C. Opel..............................1095–8
Adjusting Radiosonde Recorder, Automatic Range-. Beggs..............................602–07
Advances in Technique of Lightning Measurements. Brownlee..............................515–19
AIEE-ASME Governor Performance Specification, Governors and the. Schwendner, Astley...1330–2
Ailments, Diagnosis of Rectifier. Dortort....1298–1304
Air-Break Switches for Protection of Transformer Stations, Automatic Grounding and. Ricker..............................851–6; disc. 856
Air Circuit Breaker, A New 69-Kv Compressed-. Baker, Frisch..............................363–8; disc. 369
Air-Core Reactors as Fault Limiting Means on High Interrupting Capacity Controllers, The Use of. Leitch..............................469–74; disc. 474
Aircraft, An Electric Deicing System for. Giloy..............................1051–7
Aircraft Cable, Current Ratings of. Askew, McClinton..............................988–92
Aircraft Carbon-Pile Voltage Regulators, Fundamentals, and Design Improvements. Austin, Richards..............................983–7
Aircraft, Lightning Stroke Damage to. Hagenguth..............................1036–44; disc. 1044
Aircraft Propellers, Development and Application of Electric Heating to Deicing of. Sheets, Sand..............................1068–72
Aircraft Transformers, Lightweight. Stephens..1073–8
Airplane Main Bus, Protection of the. Exner..............................979–82
Alarm and Control System for Remote Control of Coaxial Cable Stations, B-1. Hearn, Weber...622–6
A-C Generators, A Semiempirical Approach to Voltage Dip—with Suddenly Applied Loads on. Rosenberg..............................160–6; disc. 166
A-C Network Analyzer, An Improved. Morgan, Rothe, Winsness..............................891–5; disc. 895
A-C Resistance of Segmental Cables in Steel Pipe. Meyerhoff, Eager..............................816–29; disc. 829
A-C Tripping and Reclosing of Line Sectionalizing Circuit Breakers. Winegartner, Bonheimer...1357–9
A-C Voltmeter, A New Expanded Scale. Millar..............................641–3
Altitudes, A Small High-Voltage Bushing Design for High. Vogel, Hart..............................761–4
Ampere Demand Meter, A New Thermal Volt-. Douglass, Morong..............................79–83; disc. 83
Amplifier—Class A Operation, The Dynamoelectric. Saunders..............................1368–73
Amplifier Stabilized for Gain and for Zero, Wide-Band D-C. Williams, Amey, McAdam..............................811–15; disc. 815
Amplifiers, An Analysis of Rotating. Litman..1111–17
Amplifiers with Feedback, Analytical Determination of Characteristics of Magnetic. VerPlanck, Finzi, Beaumariage..............................565–70
Amplifiers, Self-Saturation in Magnetic. Dornhoefer..............................835–46; disc. 846
Analog Computer to Nonlinear Mechanics and Servomechanisms, Application of the California Institute of Technology Electric. McCann, Wilts, Locanthi..............................652–60
Analog Computers, Comparison of Long-Time and Short-Time. Paschkis..............................70–3
Analogs of Linear Vibration Problems, Summary of Transformations Useful in Constructing. Corbett..............................661–4
Analogy, The Transient Behavior of the Two-Stage Rototrol Main Exciter Voltage Regulating System as Determined by Electrical. Carleton.....59–63
Analysis of Relay Servomechanisms, An. Kahn..............................1079–87; disc. 1087
Analysis of Results to Research Problems in Magnetic Recording, Application of Experimental Test Procedures and Methods of. Thompson......407–17
Analysis of Rotating Amplifiers, An. Litman..1111–17
Analysis, The Torques of the Synchronous Tie—A Steady-State. Finzi, McConnell.........1147–52
Analytical Co-ordination Method, Field Tests of Oil Circuit Recloser Substantiate. Auer, Branflick, Woodward, McKinley..........801–08; disc. 808
Analytical Determination of Characteristics of Magnetic Amplifiers with Feedback. VerPlanck, Finzi, Beaumariage..............................565–70
Analyzer, A New Fourier Coefficient Harmonic. Charp..............................644–9; disc. 649
Analyzer, An Improved A-C Network. Morgan, Rothe, Winsness..............................891–5; disc. 895
Analyzer to Determine Plant Loading Schedules, Coordination of Fuel Cost and Transmission Loss by Use of the Network. George, Page, Ward..............................1152–60; disc. 1160
Application Engineering on Diesel-Electric Locomotives in Railroad Service. Bevan..............125–31
Application of Electric Heating to Deicing of Aircraft Propellers, Development and. Sheets, Sand..............................1068–72
Application of Experimental Test Procedures and Methods of Analysis of Results to Research Problems in Magnetic Recording. Thompson.........407–17
Application of Multifrequency Pulsing in Switching. Dahlbom, Horton, Moody................392–6
Application of the California Institute of Technology Electric Analog Computer to Nonlinear Mechanics and Servomechanisms. McCann, Wilts, Locanthi..............................652–60
Applications, Microwave Channels for Power System. (Committee Report)................40–2; disc. 42
Applications, Two Gas Turbines for Power Generation and Other. Howard............461–5; disc. 465
Approach to the Design of High-Voltage Cable Joints, A Theoretical and Practical. Short..............................1275–80; disc. 1280
Arc, Instrumentation for the Evaluation of the Stability of the Welding. Winsor, Schetky, Wyant..525–33
Arc Paths, Deionization Time of High-Voltage Fault. Harrington, Starr...........997–1003; disc. 1003
Armature Iron Losses in Series Motors. Chang, Karr..............................262–4; disc. 264
Arrangements and Protective Practices for Shunt Capacitor Banks, Connection. Clark, Farnham..............................1226–30; disc. 1231
ASME Governor Performance Specification, Governors and the AIEE-. Schwendner, Astley......1330–2
Aspects of Relay Circuits, Sequential. Ritchie..577–81
Attenuation and Delay Equalizers for Coaxial Lines. Lundry..............................1174–9
Audio Noise Problems on an Electric Power System, Transformer. Murray..........740–7; disc. 747
Automatic Control Systems, The Design of D-C Motors for Use in. Lebenbaum...............1089–94
Automatic Grounding and Air-Break Switches for Protection of Transformer Stations. Ricker..............................851–6; disc. 856
Automatic Load Control for Turbine Generators, A New. Ruud, Farnham.......1337–42; disc. 1342
Automatic Range-Adjusting Radiosonde Recorder. Beggs..............................602–07
Auxiliary Circuits of Single-Phase Induction Motors, Design of. Thacker, Gopalakrishna.......1373–9

B

B-1 Alarm and Control System for Remote Control of Coaxial Cable Stations. Hearn, Weber.....622–6
Backlash, Instrument Inaccuracies in Feed-Back Control Systems with Particular Reference to. Marcy, Yachter, Zauderer..............................778–88
Band Coupling of Power-Line Carrier Equipment, A Proposal for Broad-. Miller...1028–30; disc. 1030
Band D-C Amplifier Stabilized for Gain and for Zero, Wide-. Williams, Amy, McAdam..............................811–15; disc. 815
Band of Governors on Steam Turbine Generators Under System Load Conditions, Determining Incremental Regulation and Dead. Tornquist, Wade..1343–7
Bandwidths, The Distinction Between Effective and Circuit. Kessler..............................98–9
Bank, The Temperature Rise of Cables in a Duct. Neher..............................540–6; disc. 546
Banks, Connection Arrangements and Protective Practices for Shunt Capacitor. Clark, Farnham..............................1226–30; disc. 1231
(Basic Sciences) A Derivation of Heaviside's Operational Calculus Based on the Generalized Functions of Schwartz. Smith, Alger........939–44; disc. 944
(Basic Sciences) Dielectric Strength of Oil with Variations of Pressure and Temperature. Hoover, Hixson..............................1047–50
(Basic Sciences) Formulas and Tables for the Calculation of the Magnetic Field Components of Circular Filaments and Solenoids. Grover............665–75
(Basic Sciences) General Theory, and Experimental Confirmation, of the Moving Coil Fluxmeter. Higgins, Robertson............897–907; disc. 907
(Basic Sciences) Nonharmonic Oscillations as Caused by Magnetic Saturation. Rudenberg........676–85
(Basic Sciences) Self-Saturation in Magnetic Amplifiers. Dornhoefer..................835–46; disc. 846.
(Basic Sciences) The Vector Power Factor of Unbalanced Sinusoidal 3-Phase Circuits. Lamb, Brandt..............................507–09
Behavior of the Two-Stage Rototrol Main Exciter Voltage Regulating System as Determined by Electrical Analogy, The Transient. Carleton.........59–63
Board of Directors, Report of the...........1383–1408
Boilers and of Hydro Units, Outage Rates of Steam Turbines and. (Committee Report).......450–7
Bolometer, Detection of Overheated Transmission Line Joints by Means of a. Leslie, Wait..64–8; disc. 68
Bonneville Power Administration Transmission System, Matching' Electrical Characteristics of McNary Generators and Transformers with. Hoard, Weiner..............................1022–5; disc. 1026
Braking Control of D-C Series Motors—Experimental Study of Speed-Torque Curves, Dynamic. Heumann..............................560–4
Breaker, A New 69-Kv Compressed-Air Circuit. Baker, Frisch..............................363–8; disc. 369
Breakers, A Manually-Operated Spring Mechanism for Medium Voltage Oilless Circuit. Wyman, Casey..............................357–61; disc. 361
Breakers, A-C Tripping and Reclosing of Line Sectionalizing Circuit. Winegartner, Bonheimer....1357–9
Breakers, Insulator Flashover Deionization Times as a Factor in Applying High-Speed Reclosing Circuit. Boisseau, Wyman, Skeats......1058–66; disc. 1066
Broad-Band Coupling of Power-Line Carrier Equipment, A Proposal for. Miller.......1028–30; disc. 1030
Brushes, Progress Report on AIEE Test Code for Electric. (Committee Report)..............1126–8
Buried Cables and Pipes, The Temperature Rise of. Neher..............................9–17; disc. 17
Bus, Protection of the Airplane Main. Exner...979–82
Bushing Design for High Altitudes, A Small High-Voltage. Vogel, Hart..............................761–4
Busses, Trolley Coaches Replace. McMillon...403–06

C

Cable Accessory Design Utilizing New Laboratory Techniques. Bosworth, Farr......1268–73; disc. 1273
Cable Circuits in Philadelphia, 69-Kv Pipe-Type. Kidder, Van Antwerp..............550–6; disc. 556
Cable, Current Ratings of Aircraft. Askew, McClinton..............................988–92
Cable for Carrier Current Installation on Power Lines, Line Tuning Equipment Used With Coaxial. Sutton..............................44–8; disc. 48
Cable Impregnated with Extra-High-Viscosity Oil, High-Pressure Gas-Filled. Sticher, Doan, Atkinson, Meyerhoff................336–45; disc. 345
Cable Joints, A Theoretical and Practical Approach to the Design of High-Voltage. Short..............................1275–80; disc. 1280
Cable Overhead-Conductor 132-Kv Transmission Line at Carrier Current Frequencies, Loss Measurements Made on Underground-. Cornelius, Storer..............................597–601
Cable Stations, B-1 Alarm and Control System for Remote Control of Coaxial. Hearn, Weber...622–6
Cables and Pipes, The Temperature Rise of Buried. Neher..............................9–17; disc. 17
Cables, Heating of Radio-Frequency. Macalpine..............................283–8
Cables in a Duct Bank, The Temperature Rise of. Neher..............................540–6; disc. 546
Cables in Steel Pipe, A-C Resistance of Segmental. Meyerhoff, Eager..............816–29; disc. 829
Cables, The Power Rating of Radio-Frequency. Mildner..............................289–98; disc. 298
Cables, Transient Temperature Phenomena of 3-Conductor. Wollaston..........1284–95; disc. 1295
Cage Induction Motors, General Theory of Multiple. Chang..............................1139–43

Calculated Risk of Operating Capacitors Without Individual Fuses, The. Brooks....1219–25; disc. 1225
Calculation of the Magnetic Field Components of Circular Filaments and Solenoids, Formulas and Tables for the. Grover........665–75
Calculus Based on the Generalized Functions of Schwartz, A Derivation of Heaviside's Operational. Smith, Alger........939–44; disc. 944
California Institute of Technology Electric Analog Computer to Nonlinear Mechanics and Servomechanisms, Application of the. McCann, Wilts, Locanthi........652–60
Capacitor Banks, Connection Arrangements and Protective Practices for Shunt. Clark, Farnham........1226–30; disc. 1231
Capacitors, Report of a Survey on the Performance of Shunt. (Committee Report)..1200–06; disc. 1206
Capacitors, Report on the Operation and Maintenance of Shunt. (Committee Report)..........1208–18
Capacitors Without Individual Fuses, The Calculated Risk of Operating. Brooks....1219–25; disc. 1225
Capacity Controllers, The Use of Air-Core Reactors as Fault Limiting Means on High Interrupting. Leitch........469–74; disc. 474
Carbon-Pile Voltage Regulators, Fundamentals, and Design Improvements, Aircraft. Austin, Richards........983–7
Carrier Current Frequencies, Loss Measurements Made on Underground-Cable Overhead-Conductor 132-Kv Transmission Line at. Cornelius, Storer........597–601
Carrier Current Installation on Power Lines, Line Tuning Equipment Used With Coaxial Cable for. Sutton........44–8; disc. 48
(Carrier Current) Microwave Channels for Power System Applications. (Committee Report)........40–2; disc. 42
Carrier Equipment, A Proposal for Broad-Band Coupling of Power-Line. Miller........1028–30; disc. 1030
Carrier for Relaying and Joint Usage, Power Line. Part I. Hampe, Storer..........864–7; disc. 867
Carrier System, Stability of Tandem Regulators in the L-1. Kinzer........1179–86
Centralized Traffic Control for Railroads. Hailes........1167–73
Changing Temperature, The Measurement of Dielectric Loss at High Frequencies and Under. Whitehead, Rueggeberg........520–4
Channels for Power System Applications, Microwave. (Committee Report)........40–2; disc. 42
Characteristics of D-C Motors and Generators, Transient. McClinton, Brancato, Panoff......1100–06
Characteristics of Distribution Systems, Voltage-Recovery. Witzke........172–80; disc. 180
Characteristics of Lightning Protective Devices, Performance. (Committee Report)........1333–6
Characteristics of Magnetic Amplifiers with Feedback, Analytical Determination of. VerPlanck, Finzi, Beaumariage........565–70
Characteristics of McNary Generators and Transformers with Bonneville Power Administration Transmission System, Matching Electrical. Hoard, Weiner........1022–5; disc. 1026
Characteristics of Two-Phase Servomotors, Operating. Koopman........319–27; disc. 327
Characteristics, Rectifier Transformer. (Committee Report)........1305–11
Charged Dust in Rooms, Electrically. Penney, Hewitt........278–82
Charging Currents on Transmission Lines as Affecting Ground-Fault Neutralizers, Report on Survey of Unbalanced. (Committee Report)........1328; disc. 1329
Chart Methods Applied to Frequency Response Curves, Control. Mundel........424–7
Chesapeake and Ohio Railway Company Steam Turbine-Electric Locomotives, Electric Equipment for. Atwell, Baston........145–8
Circuit Bandwidths, The Distinction Between Effective and. Kessler........98–9
Circuit Breaker, A New 69-Kv Compressed-Air. Baker, Frisch........363–8; disc. 369
Circuit Breakers, A Manually-Operated Spring Mechanism for Medium Voltage Oilless. Wyman, Casey........357–61; disc. 361
Circuit Breakers, A-C Tripping and Reclosing of Line Sectionalizing. Winegartner, Bonheimer...1357–9
Circuit Breakers, Insulator Flashover Deionization Times as a Factor in Applying High-Speed Reclosing. Boisseau, Wyman, Skeats......1058–66; disc. 1066
Circuit Conditions, Heating of Transformers Under Short-. Montsinger, Halsey.....947–52; disc. 952
Circuit Currents and Recovery Voltages on Rural Distribution Systems, Short-. Eason, Johnson, Kalb, Peterson........930–7; disc. 937
Circuit of D-C Motors and Generators, Solid Short. Linville, Ward........119–23; disc. 123
Circuit Recloser Substantiate Analytical Co-ordination Method, Field Tests of Oil. Auer, Branflick, Woodward, McKinley........801–08; disc. 808
Circuits for Power-Flow Studies, Equivalent. Ward........373–80; disc. 380
Circuits for Protective Relays, Novel Test. Sonnemann........1249–54
Circuits in Philadelphia, 69-Kv Pipe-Type Cable. Kidder, Van Antwerp...........550–6; disc. 556
Circuits, Insulation Level of Relay and Control. (Committee Report)........1255–7
Circuits of Single-Phase Induction Motors, Design of Auxiliary. Thacker, Gopalakrishna.......1373–9
Circuits, Relay "Trees" and Symmetric. Washburn........582–6
Circuits, Sequential Aspects of Relay. Ritchie..577–81
Circuits, The Logic of Relay. Keister..........571–6
Circuits, The Vector Power Factor of Unbalanced Sinusoidal 3-Phase. Lamb, Brandt...........507–09
Circular Filaments and Solenoids, Formulas and Tables for the Calculation of the Magnetic Field Components of. Grover........665–75
Clamps, Magnetic Heating of Transmission Line. Crabb, Sheadel........1032–5
Class A Operation, The Dynamoelectric Amplifier—. Saunders........1368–73
Class-H Insulation and Silicone Varnish, Motor Tests Evaluate Thermal Endurance of. Grant, Kauppi, Moses, Gibson........1133–8
Coaches Replace Busses, Trolley. McMillon...403–06
Coaxial Cable for Carrier Current Installation on Power Lines, Line Tuning Equipment Used With. Sutton........44–8; disc. 48
Coaxial Cable Stations, B-1 Alarm and Control System for Remote Control of. Hearn, Weber.....622–6
Coaxial Lines, Attenuation and Delay Equalizers for. Lundry........1174–9
Coaxial Lines, Equalization of. Gould.......1187–92
Coaxial Systems, Power Supplies for. Spencer...627–30
Coaxial Systems, Television Terminals for. Morrison........1193–9
Code for Electric Brushes, Progress Report on AIEE Test. (Committee Report)........1126–8
Code Selector Supervisory System, Space. Forrest, Schirmer........1312–15; disc. 1315
Codes and Translations. Myers........592–7
Coefficient Harmonic Analyzer, A New Fourier. Charp........644–9; disc. 649
Coil Fluxmeter, General Theory and Experimental Confirmation of the Moving. Higgins, Robertson........897–907; disc. 907
(Committee Report) Insulation Level of Relay and Control Circuits........1255–7
(Committee Report) Microwave Channels for Power System Applications........40–2; disc. 42
(Committee Report) Outage Rates of Steam Turbines and Boilers and of Hydro Units........450–7
(Committee Report) Performance Characteristics of Lightning Protective Devices........1333–6
(Committee Report) Progress Report on AIEE Test Code for Electric Brushes........1126–8
(Committee Report) Protection of Stations Without High-Voltage Switching........226–31; disc. 231
(Committee Report) Rectifier Transformer Characteristics........1305–11
(Committee Report) Report of a Survey on the Performance of Shunt Capacitors......1200–06; disc. 1206
(Committee Report) Report on Survey of Unbalanced Charging Currents on Transmission Lines as Affecting Ground-Fault Neutralizers....1328; disc. 1329
(Committee Report) Report on the Operation and Maintenance of Shunt Capacitors........1208–18
(Committee Report) Stray Load Losses Measured in D-C Motors........219–23; disc. 223
(Committee Report) Temperature Rise Values for D-C Machines. Part I........206–12; disc. 212
(Committee Report) Temperature Rise Values for D-C Machines. Part II..........1118–23; disc. 1123
Committees and Officers—1949–1950.........1409–28
(Communication) A Method for Designing Pulse Transformers. Kirschbaum, Warren........971–8
(Communication) A Time Division Multiplexing System. Boothroyd, Creamer........92–7
(Communication) Application of Experimental Test Procedures and Methods of Analysis of Results to Research Problems in Magnetic Recording. Thompson........407–17
(Communication) Application of Multifrequency Pulsing in Switching. Dahlbom, Horton, Moody...392–6
(Communication) Attenuation and Delay Equalizers for Coaxial Lines. Lundry........1174–9
(Communication) B-1 Alarm and Control System for Remote Control of Coaxial Cable Stations. Hearn, Weber........622–6
(Communication) Centralized Traffic Control for Railroads. Hailes........1167–73
(Communication) Codes and Translations. Myers........592–7
(Communication) Considerations on Facsimile Transmission Speed. Burkhard.......418–23; disc. 423
(Communication) Control Chart Methods Applied to Frequency Response Curves. Mundel.......424–7
(Communication) Counting with Relays. Frost........587–91
(Communication) Equalization of Coaxla Lines. Gould........1187–92
Communication Equipment for Land-Mobile Services Operating on Frequencies Between 152–174 Megacycles, Design of Mobile Radio. Beers, Harris, Zappacosta........1232–9
(Communication) Power Supplies for Coaxial Systems. Spencer........627–30
(Communication) Power Supplies for Microwave Relay Systems. Ward........631–6
(Communication) Radio Influence from High-Voltage Corona. Slemon.............198–204; disc. 204
Communication Relay System, A Microwave. Boothroyd, Churchill........637–41
(Communication) Relay "Trees" and Symmetric Circuits. Washburn........582–6
(Communication) Rubber Insulators for Pole Lines. Wheeler, Markley........195–7
(Communication) Sequential Aspects of Relay Circuits. Ritchie........577–81
(Communication) Stability of Tandem Regulators in the L-1 Carrier System. Kinzer.........1179–86
(Communication) Television Terminals for Coaxial Systems. Morrison........1193–9
(Communication) The Distinction Between Effective and Circuit Bandwidths. Kessler...........98–9
(Communication) The Logic of Relay Circuits. Keister........571–6
(Communication) Vibrating Reed Selective Signaling System for Mobile Telephone Use. Pruden, Hoth........387–91
(Communication) Vibrating Reed Selectors for Mobile Radio Systems. Keller, Bostwick..........383–6
Commutation of D-C Machines and Its Effects on Radio Influence Voltage Generation. Motter........491–5; disc. 495
Commutation, Oscillographing. Baldwin........100–05; disc. 105
Commutation with an Indicating Instrument, Measuring. Lundy........1380–2
Commutator Surface Film Conditions on Commutation, Effects of. Lynn, Elsey........106–112; disc. 112
Comparative Performance Records—Steel and Wood Transmission Lines. Booker......686–9; disc. 689
Comparison of Long-Time and Short-Time Analog Computers. Paschkis........70–3
Comparison of Steady-State and Transient Performance of Servomechanisms. Chestnut, Mayer....765–77
Components and Systems, Graphical Determination of Transfer Function Loci for Servomechanism. Thomas, Easton........307–14; disc. 314
Components of Circular Filaments and Solenoids, Formulas and Tables for the Calculation of the Magnetic Field. Grover........665–75
Compressed-Air Circuit Breaker, A New 69-Kv. Baker, Frisch........363–8; disc. 369
Computer to Nonlinear Mechanics and Servomechanisms, Application of the California Institute of Technology Electric Analog. McCann, Wilts, Locanthi........652–60
Computers, Comparison of Long-Time and Short-Time Analog. Paschkis........70–3
(Computing Devices) An Improved A-C Network Analyzer. Morgan, Rothe, Winsness..891–5; disc. 895
(Computing Devices) Summary of Transformations Useful in Constructing Analogs of Linear Vibration Problems. Corbett........661–4
Conditions, Determining Incremental Regulation and Dead Band of Governors on Steam Turbine Generators Under System Load. Tornquist, Wade........1343–7
Conditions During Operation of Hydroelectric Units, Some Oscillographic Methods of Investigating Mechanical and Hydraulic. Engvall.......993–6
Conditions, Fundamental Theory of Inherent-Overheating Protection Under Running Overload. Veinott, Schaefer........266–70; disc. 270
Conditions, Heating of Transformers Under Short-Circuit. Montsinger, Halsey.......947–52; disc. 952
Conditions on Commutation, Effects of Commutator Surface Film. Lynn, Elsey......106–12; disc. 112
Conductor Cables, Transient Temperature Phenomena of 3-. Wollaston........1284–95; disc. 1295
Conductor Economics, Distribution Transformer and Secondary. Brady........483–6; disc. 486
Conductor, Hy-Therm Copper—Improved Overhead Line. Hickernell, Jones, Snyder.....22–7; disc. 27
Conductor 132-Kv Transmission Line at Carrier Current Frequencies, Loss Measurements Made on Underground-Cable Overhead-. Cornelius, Storer........597–601
Confirmation of the Moving Coil Fluxmeter, General Theory and Experimental. Higgins, Robertson........897–907; disc. 907
Connection Arrangements and Protective Practices for Shunt Capacitor Banks. Clark, Farnham........1226–30; disc. 1231
Considerations on Facsimile Transmission Speed. Burkhard........418–23; disc. 423
Contaminants on Electrical Insulation, Methods for Determining the Effects of. Mathes, Sieffert, Walker, Lindsey........113–18; disc. 118

Control Chart Methods Applied to Frequency Response Curves. Mundel........................424–7
Control Circuits, Insulation Level of Relay and. (Committee Report)........................1255–7
Control for Railroads, Centralized Traffic. Hailes..1167–73
Control for Turbine Generators, A New Automatic Load. Ruud, Farnham..............1337–42; disc. 1342
Control of D-C Series Motors—Experimental Study of Speed-Torque Curves, Dynamic Braking. Heumann..560–4
Control System, A New 2-Signal Synchronous Supervisory. Derr.................857–63; disc. 863
Control System for Remote Control of Coaxial Cable Stations, B-1 Alarm and. Hearn, Weber....622–6
Control Systems, The Design of D-C Motors for Use in Automatic. Lebenbaum................1089–94
Control Systems with Particular Reference to Backlash, Instrument Inaccuracies in Feed-Back. Marcy, Yachter, Zauderer......................778–88
Controllable Reactors, Universal Curves for D-C. Johnson, Merrell, Alley............31–7; disc. 37
Controlled Reactors with Resistive Load, Some Fundamentals of D-C. Storm........188–94: disc. 194
Controlled Temperature and Insulation Protection in the Operation of Power Transformers. Satterlee, Reed..................................753–8; disc. 759
Controllers, The Use of Air-Core Reactors as Fault Limiting Means on High Interrupting Capacity. Leitch..........................469–74; disc. 474
Convenient Method for Determining Generator Reserve, A. Seelye..........................1317–20
Converter for Telemetering and Totalizing, A Thermal. Downing......................879–84; disc. 884
Co-ordination Method, Field Tests of Oil Circuit Recloser Substantiate Analytical. Auer, Branflick, Woodward, McKinley..........801–08; disc. 808
Co-ordination of Fuel Cost and Transmission Loss by Use of the Network Analyzer to Determine Plant Loading Schedules. George, Page, Ward..1152–60; disc. 1160
Co-ordination of Oil Insulated Transformers, Extremes in the Insulation. Vogel.........965–8; disc. 968
Copper—An Improved Overhead-Line Conductor, Hy-Therm. Hickernell, Jones, Snyder...22–7; disc. 27
Corona, Radio Influence from High-Voltage. Slemon..............................198–204; disc. 204
Cost and Transmission Loss by Use of the Network Analyzer to Determine Plant Loading Schedules, Co-ordination of Fuel. George, Page, Ward..1152–60; disc. 1160
Counting with Relays. Frost................587–91
Coupling of Power-Line Carrier Equipment, A Proposal for Broad-Band. Miller......1028–30; disc. 1030
Cumulative and Differential Series Fields in the Parallel Operation of D-C Motors, Use of. Kimball..............................1098–1100
Current Frequencies, Loss Measurements Made on Underground-Cable Overhead-Conductor 132-Kv Transmission Line at Carrier. Cornelius, Storer....................................597–601
Current Installation on Power Lines, Line Tuning Equipment Used With Coaxial Cable for Carrier. Sutton..........................44–8; disc. 48
Current Ratings of Aircraft Cable, Askew, McClinton.....................................988–92
Currents and Recovery Voltages on Rural Distribution Systems, Short-Circuit. Eason, Johnson, Kalb, Peterson......................930–7; disc. 937
Currents on Transmission Lines as Affecting Ground-Fault Neutralizers, Report on Survey of Unbalanced Charging. (Committee Report)..1328; disc. 1329
Currents, Rectifier Fault—II. Herskind, Schmidt, Rettig......................243–51; disc. 251
Curves, Control Chart Methods Applied to Frequency Response. Mundel......................424–7
Curves, Dynamic Braking Control of D-C Series Motors—Experimental Study of Speed-Torque. Heumann..560–4
Curves for D-C Controllable Reactors, Universal. Johnson, Merrell, Alley............31–7; disc. 37
Cycle Out-of-Step Relay Equipment, One Slip. Morris..1246–8

D

Damage to Aircraft, Lightning Stroke. Hagenguth.............................1036–44; disc. 1044
Damper Stabilized Instrument Servomechanisms. Hall..........................299–306; disc. 306
Data, Prediction of Ultimate Temperature Rise from Early Heat Run. Ryan..................1164–6
Dead Band of Governors on Steam Turbine Generators Under System Load Conditions, Determining Incremental Regulation and. Tornquist, Wade..1343–7
Deicing of Aircraft Propellers, Development and Application of Electric Heating to. Sheets, Sand..1068–72
Deicing System for Aircraft, An Electric. Giloy..1051–7
Deionization Time of High-Voltage Fault-Arc Paths. Harrington, Starr...........997–1003; disc. 1003
Deionization Times as a Factor in Applying High-Speed Reclosing Circuit Breakers, Insulator Flashover. Boisseau, Wyman, Skeats......1058–66; disc. 1066
Delay Equalizers for Coaxial Lines, Attenuation and. Lundry..................................1174–9
Demand Meter, A New Thermal Volt-Ampere. Douglass, Morong....................79–83; disc. 83
Demand Meters, Heat Flow Ratio as a Design Parameter in Thermal. Nelson...........885–90; disc. 890
Derivation of Heaviside's Operational Calculus Based on the Generalized Functions of Schwartz, A. Smith, Alger.......................939–44; disc. 944
Design and Experience with 4-Kv Primary Network Systems in the Washington Area. Lank..................................712–16; disc. 716
Design and Performance Based on Direct Lightning Strokes, Transmission Line. Harder, Clayton..................................439–46; disc. 447
Design and Ventilation, Transformer Manholes and Vaults: Porter..............................534–9
Design Features—Steam-Electric Generating Station P of the Pacific Gas and Electric Company, Electrical. Benson......................1013–20; disc. 1020
Design for High Altitudes, A Small High-Voltage Bushing. Vogel, Hart.............................761–4
Design Fundamentals of Potheads 69,000 Volts and Below. Nicholas...............1263–7; disc. 1267
Design Improvements, Aircraft Carbon-Pile Voltage Regulators, Fundamentals, and. Austin, Richards..983–7
Design of Auxiliary Circuits of Single-Phase Induction Motors. Thacker, Gopalakrishna.........1373–9
Design of D-C Adjustable Speed Motors, Limitations in. Opel..................................1095–8
Design of D-C Motors for Use in Automatic Control Systems, The. Lebenbaum................1089–94
Design of High-Voltage Cable Joints, A Theoretical and Practical Approach to the. Short..............................1275–80; disc. 1280
Design of Mobile Radio Communication Equipment for Land-Mobile Services Operating on Frequencies Between 152–174 Megacycles. Beers, Harris, Zappacosta..................................1232–9
Design of Traction Motors, Twenty-Five Years Progress in the. Baldwin............................132–7
Design Parameter in Thermal Demand Meters, Heat Flow Ratio as a. Nelson........885–90; disc. 890
Design Utilizing New Laboratory Techniques, Cable Accessory. Bosworth, Farr....1268–73; disc. 1273
Designing Pulse Transformers, A Method for. Kirschbaum, Warren..............................971–8
Detection of Overheated Transmission Line Joints by Means of a Bolometer. Leslie, Wait. 64–8; disc. 68
Determination of Characteristics of Magnetic Amplifiers with Feedback, Analytical. VerPlanck, Finzi, Beaumariage..............................565–70
Determination of Transfer Function Loci for Servomechanism Components and Systems, Graphical. Thomas, Easton..................307–14; disc. 314
Determining Generator Reserve, A Convenient Method for. Seelye..............................1317–20
Determining Incremental Regulation and Dead Band of Governors on Steam Turbine Generators Under System Load Conditions. Tornquist, Wade..1343–7
Determining the Effects of Contaminants on Electrical Insulation, Methods for. Mathes, Sieffert, Walker, Lindsey......................113–18; disc. 118
Development and Application of Electric Heating to Deicing of Aircraft Propellers. Sheets, Sand..1068–72
Developments, Electronic Relay. Loving..............................233–40; disc. 240
(Devices) A New Fourier Coefficient Harmonic Analyzer. Charp..................644–9; disc. 649
(Devices) Comparison of Long-Time and Short-Time Analog Computers. Paschkis..............70–3
Devices, Performance Characteristics of Lightning Protective. (Committee Report)............1333–6
Diagnosis of Rectifier Ailments. Dortort....1298–1304
Die-Cast Rotors for Induction Motors. Packer..............................253–60; disc. 260
Dielectric Loss at High Frequencies and Under Changing Temperature, The Measurement of. Whitehead, Rueggeberg..............................520–4
Dielectric Strength of Oil with Variations of Pressure and Temperature. Hoover, Hixson..........1047–50
Diesel-Electric Locomotives in Railroad Service, Application Engineering on. Bevan...........125–31
Diesel-Electric Rig for Deep-Well Drilling, Performance of. Clark, Moore.......................497–506
Diesel-Electric Switching Locomotives, Industrial Plants Prefer. Woods.............................697–700
Differential Leakage of the Different Patterns of a Fractional Slot Winding. Liwschitz..............................1129–31; disc. 1131
Differential Series Fields in the Parallel Operation of D-C Motors, Use of Cumulative and. Kimball..................................1098–1100
Dip—with Suddenly Applied Loads on A-C Generators, A Semiempirical Approach to Voltage. Rosenberg..........................160–6; disc. 166
D-C Adjustable Speed Motors, Limitations in Design of. Opel..1095–8
D-C Amplifier Stabilized for Gain and for Zero, Wide-Band. Williams, Amey, McAdam..................................811–15; disc. 815
D-C Controllable Reactors, Universal Curves for. Johnson, Merrell, Alley............31–7; disc. 37
D-C Controlled Reactors with Resistive Load, Some Fundamentals of. Storm.......188–94; disc. 194
D-C Machine Performance, The Eccentric Pole Face—A Means to Better. Baker..............1107–10
D-C Machines and Its Effects on Radio Influence Voltage Generation, Commutation of. Motter..................................491–5; disc. 495
D-C Machines, Temperature Rise Values for. Part I. (Committee Report)............206–12; disc. 212
D-C Machines, Temperature Rise Values for. Part II. (Committee Report)..........1118–23; disc. 1123
D-C Motors and Generators, Solid Short Circuit of. Linville, Ward.................119–23; disc. 123
D-C Motors and Generators, Transient Characteristics of. McClinton, Brancato, Panoff........1100–06
D-C Motors for Use in Automatic Control Systems, The Design of. Lebenbaum................1089–94
D-C Motors, Stray Load Losses Measured in. (Committee Report)................219–23; disc. 223
D-C Motors, Use of Cumulative and Differential Series Fields in the Parallel Operation of. Kimball..1098–1100
D-C Series Motors—Experimental Study of Speed-Torque Curves, Dynamic Braking Control of. Heumann..560–4
Direct Lightning Strokes, Transmission Line Design and Performance Based on. Harder, Clayton..................................439–46; disc. 447
Distance Relays During Faults and Power Swings, Graphical Method for Estimating the Performance of. Warrington..............608–20; disc. 620
Distinction Between Effective and Circuit Bandwidths, The. Kessler..............................98–9
Distribution System, Lightning Investigation on a Rural. MacCarthy, Stann, Edge, McKinley..................................428–37; disc. 437
Distribution System, Operation of a Ground-Fault Neutralizer on a Regulated. Tomlinson, Hunt..................................1321–6; disc. 1326
Distribution System, Overcurrent Investigation on a Rural. Lincks, Edge, McKinley, Leh..................................915–27; disc. 927
Distribution Systems, Short-Circuit Currents and Recovery Voltages on Rural. Eason, Johnson, Kalb, Peterson......................930–7; disc. 937
Distribution Systems, Voltage-Recovery Characteristics of. Witzke.................172–80; disc. 180
Distribution Transformer and Secondary Conductor Economics. Brady.............483–6; disc. 486
Division Multiplexing System, A Time. Boothroyd, Creamer..................................92–7
(Domestic) The Heat Pump—Its Significance as a Potential Residential Electric Load. Bary.......1–8
Drilling, Performance of Diesel-Electric Rig for Deep-Well. Clark, Moore...................497–506
Drive, A Wide-Range Adjustable-Speed. Conrad, Tribken..789–90
Drives for Looms, Selection of Electric. Demartini, Lukens..349–56
Duct Bank, The Temperature Rise of Cables in a. Neher..........................540–6; disc. 546
Duquesne Light Company System, Primary Networks on the. Holben, Lange...........704–09; disc. 709
Dust in Rooms, Electrically Charged. Penney, Hewitt..278–82
Dynamic Braking Control of D-C Series Motors—Experimental Study of Speed-Torque Curves. Heumann..560–4
Dynamoelectric Amplifier—Class A Operation, The. Saunders..................................1368–73

E

Eccentric Pole-Face—A Means to Better D-C Machine Performance, The. Baker..............1107–10
Economic Loading of a Transformer System. Levine Smith..........................733–8; disc. 738
Economics, Distribution Transformer and Secondary Conductor. Brady..............483–6; disc. 486
Effect of Leakage Reactance, Saturation. Chang, Lloyd..1144–7
Effective and Circuit Bandwidths, The Distinction Between. Kessler..............................98–9
Effects of Commutator Surface Film Conditions on Commutation. Lynn, Elsey.........106–12; disc. 112
Effects of Contaminants on Electrical Insulation, Methods for Determining the. Mathes, Sieffert, Walker, Lindsey......................113–18; disc. 118
Electric Analog Computer to Nonlinear Mechanics and Servomechanisms, Application of the California Institute of Technology. McCann, Wilts, Locanthi..652–60

Electric Brushes, Progress Report on AIEE Test Code for. (Committee Report)....................1126–8
Electric Deicing System for Aircraft, An. Giloy...1051–7
Electric Drives for Looms, Selection of. Demartini, Lukens................................349–56
Electric Equipment for Chesapeake and Ohio Railway Company Steam Turbine-Electric Locomotives. Atwell, Baston............................145–8
(Electric Heating) The Measurement of Dielectric Loss at High Frequencies and Under Changing Temperature. Whitehead, Rueggeberg.........520–4
Electric Heating to Deicing of Aircraft Propellers, Development and Application of. Sheets, Sand...1068–72
Electric Load, The Heat Pump—Its Significance as a Potential Residential. Bary..................1–8
Electric Locomotives in Railroad Service, Application Engineering on Diesel-. Bevan...........125–31
Electric Motive Power, The Renaissance of. Candee...138–44
Electric Power System, Transformer Audio Noise Problems on an. Murray............740–7; disc. 747
Electric Rig for Deep-Well Drilling, Performance of Diesel-. Clark, Moore................497–506
Electrical Analogy, The Transient Behavior of the Two-Stage Rototrol Main Exciter Voltage Regulating System as Determined by. Carleton........59–63
Electrical Characteristics of McNary Generators and Transformers with Bonneville Power Administration Transmission System, Matching. Hoard, Weiner.....................1022–5; disc. 1026
Electrical Design Features—Steam-Electric Generating Station P of the Pacific Gas and Electric Company. Benson....................1013–20; disc. 1020
Electrical Insulation, Methods for Determining the Effects of Contaminants on. Mathes, Sieffert, Walker, Lindsey...............113–11; disc. 118
Electrically Charged Dust in Rooms. Penney, Hewitt...278–82
(Electronic Instruments) A Square-Law Power-Level Recorder. Clark, Turner, Williams.......476–82
(Electronic Power Converters) Diagnosis of Rectifier Ailments. Dortort...................1298–1304
(Electronic Power Converters) Rectifier Fault Currents—II. Herskind, Schmidt, Rettig..243–51; disc. 251
(Electronic Power Converters) Rectifier Transformer Characteristics. (Committee Report)....1305–11
Electronic Relay Developments Loving...233–40; disc. 240
(Electronics) Analytical Determination of Characteristics of Magnetic Amplifiers with Feedback. VerPlanck, Finzi, Beaumariage..........................565–70
(Electronics) Electrically Charged Dust in Rooms. Penney, Hewitt...........................278–82
(Electronics) Heating of Radio-Frequency Cables. Macalpine.................................283–8
(Electronics) The Power Rating of Radio-Frequency Cables. Mildner..............289–98; disc. 298
Electro-Optical Shutter for Photographic Purposes, An. Zarem, Marshall, Poole..................84–91
Endurance of Class H Insulation and Silicone Varnish, Motor Tests Evaluate Thermal. Grant, Kauppi, Moses, Gibson..........................1133–8
Energy, Measurements of Erythemal. Scott....725–29
Engineering on Diesel-Electric Locomotives in Railroad Service, Application. Bevan..............125–31
Equalization of Coaxial Lines. Gould........1187–92
Equalizers for Coaxial Lines, Attenuation and Delay. Lundry..............................1174–9
Equipment, A Proposal for Broad-Band Coupling of Power-Line Carrier. Miller...1028–30; disc. 1030
Equipment for Chesapeake and Ohio Railway Company Steam Turbine-Electric Locomotives, Electric. Atwell, Baston..............................145–8
Equipment for Land-Mobile Services Operating on Frequencies Between 152–174 Megacycles, Design of Mobile Radio Communication. Beers, Harris, Zappacosta...........................1232–9
Equipment, One Slip Cycle Out-of-Step Relay. Morris.......................................1246–8
Equipment, Reduction of Interference from Radio-Frequency Heating. Klingaman..718–24; disc. 724
Equipment Used With Coaxial Cable for Carrier Current Installation on Power Lines, Line Tuning. Sutton.............................44–8; disc. 48
Equivalent Circuits for Power-Flow Studies. Ward.............................373–80; disc. 380
Erythemal Energy, Measurements of. Scott....725–29
Evaluate Thermal Endurance of Class H Insulation and Silicone Varnish, Motor Tests. Grant, Kauppi, Moses, Gibson..........................1133–8
Evaluation of the Stability of the Welding Arc, Instrumentation for the. Winsor, Schetky, Wyant...525–33
Excitation Relay for Synchronous Generators, A New Loss-of-. Mason.......................1240–5
Exciter Voltage Regulating System as Determined by Electrical Analogy, The Transient Behavior of the Two-Stage Rototrol Main. Carleton.......59–63
Expanded Scale A-C Voltmeter, A New. Millar..641–3
Experience With 4-Kv Primary Network Systems in the Washington Area, Design and. Lank.................................712–16; disc. 716
Experimental Confirmation of the Moving Coil Fluxmeter, General Theory and. Higgins, Robertson...............................897–907; disc. 907
Experimental Study of Speed-Torque Curves, Dynamic Braking Control of D-C Series Motors—. Heumann.....................................560–4
Experimental Test Procedures and Methods of Analysis of Results to Research Problems in Magnetic Recording, Application of. Thompson.......407–17
Extremes in the Insulation Co-ordination of Oil Insulated Transformers. Vogel.......965–8; disc. 968

F

Face—A Means to Better D-C Machine Performance, The Eccentric Pole. Baker.............1107–10
Facsimile Transmission Speed, Considerations on. Burkhard.....................418–23; disc. 423
Factor of Unbalanced Sinusoidal 3-Phase Circuits, The Vector Power. Lamb, Brandt............507–09
Fault-Arc Paths, Deionization Time of High-Voltage. Harrington, Starr...........997–1003; disc. 1003
Fault Currents, Rectifier—II. Herskind, Schmidt, Rettig........................243–51; disc. 251
Fault Limiting Means on High Interrupting Capacity Controllers, The Use of Air-Core Reactors as. Leitch........................469–74; disc. 474
Fault Locator for High-Voltage Transmission Lines, A Transient. Spaulding, Diemond...........................1005–1010; disc. 1010
Fault Neutralizer on a Regulated Distribution System, Operation of a Ground-. Tomlinson, Hunt...........................1321–6; disc. 1326
Fault Neutralizers, Report on Survey of Unbalanced Charging Currents on Transmission Lines as Affecting Ground-. (Committee Report)...............................1328; disc. 1329
Faults and Power Swings, Graphical Method for Estimating the Performance of Distance Relays During. Warrington..................608–20; disc. 620
Features—Steam-Electric Generating Station P of the Pacific Gas and Electric Company, Electrical Design. Benson...............1013–20; disc. 1020
Feedback, Analytical Determination of Characteristics of Magnetic Amplifiers with. VerPlanck, Finzi, Beaumariage..........................565–70
Feed-Back Control Systems with Particular Reference to Backlash, Instrument Inaccuracies in. Marcy, Yachter, Zauderer......................778–88
Field Components of Circular Filaments and Solenoids, Formulas and Tables for the Calculation of the Magnetic. Grover.....................665–75
Field Fluxes of the Shaded-Pole Motor, The. Kimberly..273–7
Field Tests of Oil Circuit Recloser Substantiate Analytical Co-ordination Method. Auer, Branflick, Woodward, McKinley..........801–08; disc. 808
Fields in the Parallel Operation of D-C Motors, Use of Cumulative and Differential Series. Kimball.......................................1098–1100
Filaments and Solenoids, Formulas and Tables for the Calculation of the Magnetic Field Components of Circular. Grover......................665–75
Film Conditions on Commutation, Effects of Commutator Surface. Lynn, Elsey...106–12; disc. 112
Flashover Deionization Times as a Factor in Applying High-Speed Reclosing Circuit Breakers, Insulator. Boisseau, Wyman, Skeats......1058–66; disc. 1066
Flow Ratio as a Design Parameter in Thermal Demand Meters, Heat. Nelson..........885–90; disc. 890
Fluxes of the Shaded-Pole Motor, The Field. Kimberly..273–7
Fluxmeter, General Theory and Experimental Confirmation of the Moving Coil. Higgins, Robertson...........................897–907; disc. 907
Formulas and Tables for the Calculation of the Magnetic Field Components of Circular Filaments and Solenoids. Grover.....................665–75
Fourier Coefficient Harmonic Analyzer, A New. Charp.........................644–9; disc. 649
Fractional Slot Winding, Differential Leakage of the Different Patterns of a. Liwschitz...........................1129–31; disc. 1131
Frames, Stationary Networks and Transmission Lines Along Uniformly Rotating Reference. Kron...690–6
Frequencies and Under Changing Temperature, The Measurement of Dielectric Loss at High. Whitehead, Rueggeberg.......................520–4
Frequencies, Loss Measurements Made on Underground-Cable Overhead-Conductor 132-Kv Transmission Line at Carrier Current. Cornelius, Storer..............................597–601
Frequency Cables, Heating of Radio-. Macalpine...283–8
Frequency Cables, The Power Rating of Radio- Mildner.....................289–98; disc. 298
Frequency Heating Equipment, Reduction of Interference from Radio-. Klingman..718–24; disc. 724
Frequency Response Curves, Control Chart Methods Applied to. Mundel....................424–7
Fuel Cost and Transmission Loss by Use of the Network Analyzer to Determine Plant Loading Schedules, Co-ordination of. George, Page, Ward...........................1152–60; disc. 1160
Function Loci for Servomechanism Components and Systems, Graphical Determination of Transfer. Thomas, Easton...............307–14; disc. 314
Functions of Schwartz, A Derivation of Heaviside's Operational Calculus Based on the Generalized. Smith, Alger..................939–44; disc. 944
Fundamental Theory of Inherent-Overheating Protection Under Running Overload Conditions. Veinott, Schaefer..............266–70; disc. 270
Fundamentals, and Design Improvements, Aircraft Carbon-Pile Voltage Regulators. Austin, Richards...983–7
Fundamentals of D-C Controlled Reactors with Resistive Load, Some. Storm.....188–94; disc. 194
Fundamentals of Potheads 69,000 Volts and Below Design. Nicholas............1263–7; disc. 1267
Fuses, The Calculated Risk of Operating Capacitors Without Individual. Brooks...1219–25; disc. 1225

G

Gain and for Zero, Wide-Band D-C Amplifier Stabilized for. Williams, Amey, McAdam..811–15; disc. 815
Gas-Filled Cable Impregnated with Extra-High-Viscosity Oil, High-Pressure. Sticher, Doan, Atkinson, Meyerhoff..................336–45; disc. 345
Gas Turbine Power Plant, A 3,000-Horsepower. Haverstick............................458–60
Gas Turbines for Power Generation and Other Applications, Two. Howard............461–5; disc. 465
Gas Turbines in Stationary Power Generation. Hague...467–8
General Theory and Experimental Confirmation of the Moving Coil Fluxmeter. Higgins, Robertson...............................897–907; disc. 907
General Theory of Multiple Cage Induction Motors. Chang..............................1139–43
Generating Station P of the Pacific Gas and Electric Company, Electrical Design Features—Steam-Electric. Benson............1013–20; disc. 1020
Generating Station, Relaying of Transmission Lines from the New. Green, Oewel, Ramsaur...............................873–7; disc. 877
Generation and Other Applications, Two Gas Turbines for Power. Howard...........461–5; disc. 465
Generation, Commutation of D-C Machines and Its Effects on Radio Influence Voltage. Motter.............................491–5; disc. 495
Generation, Gas Turbines in Stationary Power. Hague...467–8
Generator Reserve, A Convenient Method for Determining. Seelye.......................1317–20
Generators, A New Automatic Load Control for Turbine. Ruud, Farnham.............1337–42; disc. 1342
Generators, A New Loss-of-Excitation Relay for Synchronous. Mason.......................1240–5
Generators, A Semiempirical Approach to Voltage Dip—with Suddenly Applied Loads on A-C. Rosenberg.....................160–6; disc. 166
Generators and Transformers with Bonneville Power Administration Transmission System, Matching Electrical Characteristics of McNary. Hoard, Weiner.....................1022–5; disc. 1026
Generators, Solid Short Circuit of D-C Motors and. Linville, Ward.................119–23; disc. 123
Generators, Transient Characteristics of D-C Motors and. McClinton, Brancato, Panoff......1100–06
Generators Under System Load Conditions, Determining Incremental Regulation and Dead Band of Governors on Steam Turbine. Tornquist, Wade...1343–7
Geometric Loci of the Synchronous Tie, The. Finzi, Wellard..............................168–71
Governors and the AIEE-ASME Governor Performance Specification. Schwendner, Astley........1330–2
Governors on Steam Turbine Generators Under System Load Conditions, Determining Incremental Regulation and Dead Band of. Tornquist, Wade...1343–7
Graphical Determination of Transfer Function Loci for Servomechanism Components and Systems. Thomas, Easton...............307–14; disc. 314
Graphical Method for Estimating the Performance of Distance Relays During Faults and Power Swings. Warrington..................608–20; disc. 620
Ground-Fault Neutralizer on a Regulated Distribution System, Operation of a. Tomlinson, Hunt...........................1321–6; disc. 1326
Ground-Fault Neutralizers, Report on Survey of Unbalanced Charging Currents on Transmission Lines as Affecting. (Committee Report)...............................1328; disc. 1329

Grounding and Air-Break Switches for Protection of Transformer Stations, Automatic. Ricker........851–6; disc. 856

H

Harmonic Analyzer, A New Fourier Coefficient. Charp........644–9; disc. 649

Heat Flow Ratio as a Design Parameter in Thermal Demand Meters. Nelson.......885–90; disc. 890

Heat Pump—Its Significance as a Potential Residential Electric Load, The. Bary........1–8

Heat Run Data, Prediction of Ultimate Temperature Rise from Early. Ryan........1164–6

Heating Equipment, Reduction of Interference from Radio-Frequency. Klingaman..718–24; disc. 724

Heating of Radio-Frequency Cables. Macalpine..283–8

Heating of Transformers Under Short-Circuit Conditions. Montsinger, Halsey......947–52; disc. 952

Heating of Transmission Line Clamps, Magnetic. Crabb, Sheadel........1032–5

Heating to Deicing of Aircraft Propellers, Development and Application of Electric. Sheets, Sand..1068–72

Heaviside's Operational Calculus Based on the Generalized Functions of Schwartz, A Derivation of. Smith, Alger........939–44; disc. 944

High Frequencies and Under Changing Temperature, The Measurement of Dielectric Loss at. Whitehead, Rueggeberg........520–4

High-Pressure Gas-Filled Cable Impregnated with Extra-High-Viscosity Oil. Sticher, Doan, Atkinson, Meyerhoff........336–45; disc. 345

High-Speed Reclosing Circuit Breakers, Insulator Flashover Deionization Times as a Factor in Applying. Boisseau, Wyman, Skeats......1058–66; disc. 1066

High-Voltage Bushing Design for High Altitudes, A Small. Vogel, Hart........761–4

High-Voltage Cable Joints, A Theoretical and Practical Approach to the Design of. Short........1275–80; disc. 1280

High-Voltage Corona, Radio Influence from. Slemon........198–204; disc. 204

High-Voltage Fault-Arc Paths, Deionization Time of. Harrington, Starr........997–1003; disc. 1003

High-Voltage Switching, Protection of Stations Without. (Committee Report)........226–31; disc. 231

High-Voltage Transmission Lines, A Transient Fault Locator for. Spaulding, Diemond........1005–1010; disc. 1010

Horsepower Gas Turbine Power Plant, A 3,000-. Haverstick........458–60

Hydraulic Conditions During Operation of Hydroelectric Units, Some Oscillographic Methods of Investigating Mechanical and. Engvall....993–6

Hydro Units, Outage Rates of Steam Turbines and Boilers and of. (Committee Report).......450–7

Hydroelectric Units, Some Oscillographic Methods of Investigating Mechanical and Hydraulic Conditions During Operation of. Engvall........993–6

Hy-Therm Copper—An Improved Overhead-Line Conductor. Hickernell, Jones, Snyder........22–7; disc. 27

I

Impregnated with Extra-High-Viscosity Oil, High-Pressure Gas-Filled Cable. Sticher, Doan, Atkinson, Meyerhoff........336–45; disc. 345

Improved A-C Network Analyzer, An. Morgan, Rothe, Winsness........891–5; disc. 895

Improvements, Aircraft Carbon-Pile Voltage Regulators, Fundamentals, and Design. Austin, Richards........983–7

Improvements to Oklahoma City Primary Network. Brady........701–02; disc. 702

Incandescent Lamps, Saving Time in Testing Life of. Purcell........730–2

Incremental Regulation and Dead Band of Governors on Steam Turbine Generators Under System Load Conditions, Determining. Tornquist, Wade........1343–7

Indicating Instrument, Measuring Commutation with an. Lundy........1380–2

Indicator, Polar Vector. Walker, Waynick, Sulzer........154–9

Induction Motors, Die-Cast Rotors for. Packer........253–60; disc. 260

Induction Motors, Design of Auxiliary Circuits of Single-Phase. Thacker, Gopalakrishna....1373–9

Induction Motors, General Theory of Multiple Cage. Chang........1139–43

(Industrial Control) Prediction of Ultimate Temperature Rise from Early Heat Run Data. Ryan...1164–6

(Industrial Control) The Use of Air-Core Reactors as Fault Limiting Means on High Interrupting Capacity Controllers. Leitch.....469–74; disc. 474

(Industrial) Dynamic Braking Control of D-C Series Motors—Experimental Study of Speed-Torque Curves. Heumann........560–4

Industrial Plants Prefer Diesel-Electric Switching Locomotives. Woods........697–700

(Industry) Selection of Electric Drives for Looms. Demartini, Lukens........349–56

Influence from High-Voltage Corona, Radio. Slemon........198–204; disc. 204

Influence Voltage Generation, Commutation of D-C Machines and Its Effects on Radio. Motter........491–5; disc. 495

Inherent-Overheating Protection Under Running Overload Conditions, Fundamental Theory of. Veinott, Schaefer........266–70; disc. 270

Installation on Power Lines, Line Tuning Equipment Used With Coaxial Cable for Carrier Current. Sutton........44–8; disc. 48

Instrument Inaccuracies in Feed-Back Control Systems with Particular Reference to Backlash. Marcy, Yachter, Zauderer........778–88

Instrument, Measuring Commutation with an Indicating. Lundy........1380–2

Instrument Servomechanisms, Damper Stabilized. Hall........299–306; disc. 306

Instrumentation for the Evaluation of the Stability of the Welding Arc. Winsor, Schetky, Wyant........525–33

(Instruments) A New Expanded Scale A-C Voltmeter. Millar........641–3

(Instruments) A New Thermal Volt-Ampere Demand Meter. Douglass, Morong........79–83; disc. 83

(Instruments) A Transient Fault Locator for High-Voltage Transmission Lines. Spaulding, Demond........1005–10; disc. 1010

(Instruments) An Electro-Optical Shutter for Photographic Purposes. Zarem, Marshall, Poole..84–91

(Instruments) Automatic Range-Adjusting Radiosonde Recorder. Beggs........602–07

(Instruments) Polar Vector Indicator. Walker, Waynick, Sulzer........154–9

(Instruments) Surge Protection in a Modern Watt-Hour Meter. Busch, Williams........74–7; disc. 77

(Insulated Conductors) A-C Resistance of Segmental Cables in Steel Pipe. Meyerhoff, Eager........816–29; disc. 829

(Insulated Conductors) High-Pressure Gas-Filled Cable Impregnated with Extra-High-Viscosity Oil. Sticher, Doan, Atkinson, Meyerhoff........336–45; disc. 345

(Insulated Conductors) The Thermal Movement of Moisture in Soil. Mickley.......330–3; disc. 333

(Insulated Conductors) Transformer Manholes and Vaults: Design and Ventilation. Porter....534–9

Insulation and Silicone Varnish, Motor Tests Evaluate Thermal Endurance of Class-H. Grant, Kauppi, Moses, Gibson........1133–8

Insulation Co-ordination of Oil Insulated Transformers, Extremes in the. Vogel........965–8; disc. 968

Insulation Level of Relay and Control Circuits. (Committee Report)........1255–7

Insulation, Methods for Determining the Effects of Contaminants on Electrical. Mathes, Sieffert, Walker, Lindsey........113–18; disc. 118

Insulation Protection in the Operation of Power Transformers, Controlled Temperature and. Satterlee, Reed........753–8; disc. 759

Insulator Flashover Deionization Times as a Factor in Applying High-Speed Reclosing Circuit Breakers. Boisseau, Wyman, Skeats......1058–66; disc. 1066

Insulators for Pole Lines, Rubber. Wheeler, Markley........195–7

Interference from Radio-Frequency Heating Equipment, Reduction of. Klingaman......718–24; disc. 724

Interrupting Capacity Controllers, The Use of Air-Core Reactors as Fault Limiting Means on High-. Leitch........469–74; disc. 474

Investigating Mechanical and Hydraulic Conditions During Operation of Hydroelectric Units, Some Oscillographic Methods of. Engvall.......993–6

Investigation on a Rural Distribution System, Lightning. MacCarthy, Stann, Edge, McKinley........428–37; disc. 437

Investigation on a Rural Distribution System, Overcurrent. Lincks, Edge, McKinley, Leh........915–27; disc. 927

Iron Losses in Series Motors, Armature. Chang, Karr........262–4; disc. 264

J

Joint Usage, Power Line Carrier for Relaying and. Part I. Hampe, Storer........864–7; disc. 871

Joints, A Theoretical and Practical Approach to the Design of High-Voltage Cable. Short........1275–80; disc. 1280

Joints by Means of a Bolometer, Detection of Overheated Transmission Line. Leslie, Wait....64–8; disc. 68

L

Laboratory Techniques, Cable Accessory Design Utilizing New. Bosworth, Farr.....1268–73; disc. 1273

Lamps, Saving Time in Testing Life of Incandescent. Purcell........730–2

Land-Mobile Services Operating on Frequencies Between 152–174 Megacycles, Design of Mobile Radio Communication Equipment for. Beers, Harris, Zappacosta........1232–9

(Land Transportation) Application Engineering on Diesel-Electric Locomotives in Railroad Service. Bevan........125–31

(Land Transportation) Electric Equipment for Chesapeake and Ohio Railway Company Steam Turbine-Electric Locomotives. Atwell, Baston......145–8

(Land Transportation) Industrial Plants Prefer Diesel-Electric Switching Locomotives. Woods..697–700

(Land Transportation) Selenium Rectifiers in Motor Vehicle Power Systems. Ramsey..149–53; disc. 153

(Land Transportation) The Renaissance of Electric Motive Power. Candee........138–44

(Land Transportation) Trackless Trolley Operation in Rhode Island. Blomquist........397–402

(Land Transportation) Trolley Coaches Replace Busses. McMillon........403–06

(Land Transportation) Twenty-Five Years Progress in the Design of Traction Motors. Baldwin....132–7

Law Power-Level Recorder, A Square. Clark, Turner, Williams........476–82

Leakage of the Different Patterns of a Fractional Slot Winding, Differential. Liwschitz........1129–31; disc. 1131

Leakage Reactance, Saturation Effect of. Chang, Lloyd........1144–7

Level of Relay and Control Circuits, Insulation. (Committee Report)........1255–7

Level Recorder, A Square-Law Power-. Clark, Turner, Williams........476–82

Life of Incandescent Lamps, Saving Time in Testing. Purcell........730–2

(Light) Measurements of Erythemal Energy. Scott........725–9

(Light) Saving Time in Testing Life of Incandescent Lamps. Purcell........730–2

Lightning Investigation on a Rural Distribution System. MacCarthy, Stann, Edge, McKinley........428–37; disc. 437

Lightning Measurements, Advances in Technique of. Brownlee........515–19

Lightning Protective Devices, Performance Characteristics of. (Committee Report)........1333–6

Lightning Stroke Damage to Aircraft. Hagenguth........1036–44; disc. 1044

Lightning Strokes, Transmission Line Design and Performance Based on Direct. Harder, Clayton........439–46; disc. 447

Lightweight Aircraft Transformers. Stephens...1073–8

Limitations in Design of D-C Adjustable Speed Motors. Opel........1095–8

Limiting Means on High Interrupting Capacity Controllers, The Use of Air-Core Reactors as Fault. Leitch........469–74; disc. 474

Line at Carrier Current Frequencies, Loss Measurements Made on Underground-Cable Overhead-Conductor 132-Kv Transmission. Cornelius, Storer..597–601

Line Carrier Equipment, A Proposal for Broad-Band Coupling of Power-. Miller...1028–30; disc. 1030

Line Carrier for Relaying and Joint Usage, Power. Part I. Hampe, Storer........864–7; disc. 871

Line Clamps, Magnetic Heating of Transmission. Crabb, Sheadel........1032–5

Line Conductor, Hy-Therm Copper—An Improved Overhead-. Hickernell, Jones, Snyder........22–7; disc. 27

Line Design and Performance Based on Direct Lightning Strokes, Transmission. Harder, Clayton........439–46; disc. 447

Line Joints by Means of a Bolometer, Detection of Overheated Transmission. Leslie, Wait........64–8; disc. 68

Line Sectionalizing Circuit Breakers, A-C Tripping and Reclosing of. Winegartner, Bonheimer....1357–9

Line Tuning Equipment Used With Coaxial Cable for Carrier Current Installation on Power Lines. Sutton........44–8; disc. 48

Linear Vibration Problems, Summary of Transformations Useful in Constructing Analogs of. Corbett........661–4

Lines, A Transient Fault Locator for High-Voltage Transmission. Spaulding, Diemond........1005–10; disc. 1010

Lines Along Uniformly Rotating Reference Frames, Stationary Networks and Transmission. Kron........690–6

Lines as Affecting Ground-Fault Neutralizers, Report on Survey of Unbalanced Charging Currents on Transmission. (Committee Report)....1328; disc. 1329

Lines, Attenuation and Delay Equalizers for Coaxial. Lundry........1174–9

Lines, Comparative Performance Records—Steel and Wood Transmission. Booker.....686–9; disc. 689

Lines, Equalization of Coaxial. Gould.......1187–92

Lines from the New Sunbury Generating Station, Relaying of Transmission. Green, Oewel, Ramsaur........873–7; disc. 877

Lines, Rubber Insulators for Pole. Wheeler, Markley........195–7

Load Conditions, Determining Incremental Regulation and Dead Band of Governors on Steam Turbine Generators Under System. Tornquist, Wade..........1343–7

Load Control for Turbine Generators, A New Automatic. Ruud, Farnham..........1337–42; disc. 1342

Load Losses Measured in D-C Motors, Stray. (Committee Report)..........219–23; disc. 223

Load Reduction, The Performance of a Topped Steam Power Plant on Sudden. Noest..........1348–56

Load, Some Fundamentals of D-C Controlled Reactors with Resistive. Storm..........188–94; disc. 194

Load, The Heat Pump—Its Significance as a Potential Residential Electric. Bary..........1–8

Loading of a Transformer System, Economic. Levine, Smith..........733–8; disc. 738

Loading Schedules, Co-ordination of Fuel Cost and Transmission Loss by Use of the Network Analyzer to Determine Plant. George, Page, Ward..........1152–60; disc. 1160

Loads on A-C Generators, A Semiempirical Approach to Voltage Dip—with Suddenly Applied. Rosenberg..........160–6; disc. 166

Locator for High-Voltage Transmission Lines, A Transient Fault. Spaulding, Diemond..........1005–1010; disc. 1010

Loci for Servomechanism Components and Systems, Graphical Determination of Transfer Function. Thomas, Easton..........307–14; disc. 314

Loci of the Synchronous Tie, The Geometric. Finzi, Wellard..........168–71

Locomotives, Electric Equipment for Chesapeake and Ohio Railway Company Steam Turbine-Electric. Atwell, Baston..........145–8

Locomotives in Railroad Service, Application Engineering on Diesel-Electric. Bevan..........125–31

Locomotives, Industrial Plants Prefer Diesel-Electric Switching. Woods..........697–700

Logic of Relay Circuits, The. Keister..........571–6

Long-Time and Short-Time Analog Computers, Comparison of. Paschkis..........70–3

Loss at High Frequencies and Under Changing Temperature, The Measurement of Dielectric. Whitehead, Rueggeberg..........520–4

Loss by Use of the Network Analyzer to Determine Plant Loading Schedules, Co-ordination of Fuel Cost and Transmission. George, Page, Ward..........1152–60; disc. 1160

Loss Measurements Made on Underground-Cable Overhead-Conductor 132-Kv Transmission Line at Carrier Current Frequencies. Cornelius, Storer..........597–601

Loss-of-Excitation Relay for Synchronous Generators, A New. Mason..........1240–5

Losses in Series Motors, Armature Iron. Chang, Karr..........262–4; disc. 264

Losses Measured in D-C Motors, Stray Load. (Committee Report)..........219–23; disc. 223

M

Machine Performance, The Eccentric Pole Face—A Means to Better D-C. Baker..........1107–10

Machine, The Negative-Sequence Reactances of an Ideal Synchronous. Duesterhoeft..........510–13; disc. 513

Machines and Its Effects on Radio Influence Voltage Generation, Commutation of D-C. Motter..........491–5; disc. 495

Machines, Temperature Rise Values for D-C. Part I. (Committee Report)..........206–12; disc. 212

Machines, Temperature Rise Values for D-C. Part II. (Committee Report)..........1118–23; disc. 1123

Machines, Transients in 2-Phase Synchronous. Wilson..........1360–7

Magnetic Amplifiers, Self-Saturation in. Dornhoefer..........835–46; disc. 846

Magnetic Amplifiers with Feedback, Analytical Determination of Characteristics of. VerPlanck, Finzi, Beaumariage..........565–70

Magnetic Field Components of Circular Filaments and Solenoids, Formulas and Tables for the Calculation of the. Grover..........665–75

Magnetic Heating of Transmission Line Clamps. Crabb, Sheadel..........1032–5

Magnetic Recording, Application of Experimental Test Procedures and Methods of Analysis of Results to Research Problems in. Thompson..........407–17

Magnetic Saturation, Nonharmonic Oscillations as Caused by. Rudenberg..........676–85

Main Bus, Protection of the Airplane. Exner...979–82

Maintenance of Shunt Capacitors, Report on the Operation and. (Committee Report)..........1208–18

Manholes and Vaults: Design and Ventilation, Transformer. Porter..........534–9

Manually-Operated Spring Mechanism for Medium Voltage Oilless Circuit Breakers, A. Wyman, Casey..........357–61; disc. 361

Manufacture of Soldered Porcelain Potheads, A New Technique in the. Papp, Nicholas..........1258–60; disc. 1261

Matching Electrical Characteristics of McNary Generators and Transformers with Bonneville Power Administration Transmission System. Hoard, Weiner..........1022–5; disc. 1026

McNary Generators and Transformers with Bonneville Power Administration Transmission System, Matching Electrical Characteristics of. Hoard, Weiner..........1022–5; disc. 1026

Means on High Interrupting Capacity Controllers, The Use of Air-Core Reactors as Fault Limiting. Leitch..........469–74; disc. 474

Measurement of Dielectric Loss at High Frequencies and Under Changing Temperature, The. Whitehead, Rueggeberg..........520–4

Measurements, Advances in Technique of Lightning. Brownlee..........515–19

Measurements Made on Underground-Cable Overhead-Conductor 132-Kv Transmission Line at Carrier Current Frequencies, Loss. Cornelius, Storer..........597–601

Measurements of Erythemal Energy. Scott.....725–29

Measuring Commutation with an Indicating Instrument Lundy..........1380–2

Mechanical and Hydraulic Conditions During Operation of Hydroelectric Units, Some Oscillographic Methods of Investigating. Engvall..........993–6

Mechanics and Servomechanisms, Application of the California Institute of Technology Electric Analog Computer to Nonlinear. McCann, Wilts, Locanthi..........652–60

Mechanism for Medium Voltage Oilless Circuit Breakers, A Manually-Operated Spring. Wyman, Casey..........357–61; disc. 361

Megacycles, Design of Mobile Radio Communication Equipment for Land-Mobile Services Operating on Frequencies Between 152–174. Beers, Harris, Zappacosta..........1232–9

Meter, A New Thermal Volt-Ampere Demand. Douglass, Morong..........79–83; disc. 83

Meter, Surge Protection in a Modern Watt-Hour. Busch, Williams..........74–7; disc. 77

Meters, Heat Flow Ratio as a Design Parameter in Thermal Demand. Nelson..........885–90; disc. 890

Method, Field Tests of Oil Circuit Recloser Substantiate Analytical Co-ordination. Auer, Branflick, Woodward, McKinley..........801–08; disc. 808

Method for Designing Pulse Transformers, A. Kirschbaum, Warren..........971–8

Method for Determining Generator Reserve, A Convenient. Seelye..........1317–20

Method for Estimating the Performance of Distance Relays During Faults and Power Swings, Graphical. Warrington..........608–20; disc. 620

Methods Applied to Frequency Response Curves, Control Chart. Mundel..........424–7

Methods for Determining the Effects of Contaminants on Electrical Insulation. Mathes, Sieffert, Walker, Lindsey..........113–18; disc. 118

Methods of Analysis of Results to Research Problems in Magnetic Recording, Application of Experimental Test Procedures and. Thompson..........407–17

Methods of Investigating Mechanical and Hydraulic Conditions During Operation of Hydroelectric Units, Some Oscillographic. Engvall..........993–6

Metropolitan Area, A 66-Kv Subtransmission Plan for a. Smith, Michelson..........908–12; disc. 912

Microwave Channels for Power System Applications. (Committee Report)..........40–2; disc. 42

Microwave Communication Relay System, A. Boothroyd, Churchill..........637–41

Microwave Relay Systems, Power Supplies for. Ward..........631–6

Mobile Radio Communication Equipment for Land-Mobile Services Operating on Frequencies Between 152–174 Megacycles, Design of. Beers, Harris, Zappacosta..........1232–9

Mobile Radio Systems, Vibrating Reed Selectors for. Keller, Bostwick..........383–6

Mobile Telephone Use, Vibrating Reed Selective Signaling System for. Pruden, Hoth..........387–91

Modern Watt-Hour Meter, Surge Protection in a. Busch, Williams..........74–7; disc. 77

Moisture in Soil, The Thermal Movement of. Mickley..........330–3; disc. 333

Motive Power, The Renaissance of Electric. Candee..........138–44

Motor Tests Evaluate Thermal Endurance of Class-H Insulation and Silicone Varnish. Grant, Kauppi, Moses, Gibson..........1133–8

Motor, The Field Fluxes of the Shaded-Pole. Kimberly..........273–7

Motor Vehicle Power Systems, Selenium Rectifiers in. Ramsey..........149–53; disc. 153

Motors and Generators, Solid Short Circuit of D-C. Linville, Ward..........119–23; disc. 123

Motors and Generators, Transient Characteristics of D-C. McClinton, Brancato, Panoff......1100–06

Motors, Armature Iron Losses in Series. Chang, Karr..........262–4; disc. 264

Motors, Design of Auxiliary Circuits of Single-Phase Induction. Thacker, Gopalakrishna......1373–9

Motors, Die-Cast Rotors for Induction. Packer..........253–60; disc. 260

Motors—Experimental Study of Speed-Torque Curves, Dynamic Braking Control of D-C Series. Heumann..........560–4

Motors for Use in Automatic Control Systems, The Design of D-C. Lebenbaum..........1089–94

Motors, General Theory of Multiple Cage Induction. Chang..........1139–43

Motors, Limitations in Design of D-C Adjustable Speed. Opel..........1095–8

Motors, Stray Load Losses Measured in D-C. (Committee Report)..........219–23; disc. 223

Motors, Twenty-Five Years Progress in the Design of Traction. Baldwin..........132–7

Motors, Use of Cumulative and Differential Series Fields in the Parallel Operation of D-C. Kimball..........1098–1100

Movement of Moisture in Soil, The Thermal. Mickley..........330–3; disc. 333

Moving Coil Fluxmeter, General Theory and Experimental Confirmation of the. Higgins, Robertson..........897–907; disc. 907

Multifrequency Pulsing in Switching, Application of. Dahlbom, Horton, Moody..........392–6

Multiple Cage Induction Motors, General Theory of. Chang..........1139–43

Multiplexing System, A Time Division. Boothroyd, Creamer..........92–7

N

Negative-Sequence Reactances of an Ideal Synchronous Machine, The. Duesterhoeft....510–13; disc. 513

Network Analyzer, An Improved A-C. Morgan, Rothe, Winsness..........891–5; disc. 895

Network Analyzer to Determine Plant Loading Schedules, Co-ordination of Fuel Cost and Transmission Loss by Use of the. George, Page, Ward..........1152–60; disc. 1160

Network, Improvements to Oklahoma City Primary. Brady..........701–02; disc. 702

Network Systems in the Washington Area, Design and Experience With 4-Kv Primary. Lank..........712–16; disc. 716

Networks and Transmission Lines Along Uniformly Rotating Reference Frames. Kron..........690–6

Networks on the Duquesne Light Company System, Primary. Holben, Lange..........704–09; disc. 709

Neutralizer on a Regulated Distribution System, Operation of a Ground-Fault. Tomlinson, Hunt..........1321–6; disc. 1326

Neutralizers, Report on Survey of Unbalanced Charging Currents on Transmission Lines as Affecting Ground-Fault. (Committee Report)..........1328; disc. 1329

New Automatic Load Control for Turbine Generators, A. Ruud, Farnham..........1337–42; disc. 1342

New Expanded Scale A-C Voltmeter, A. Millar..........641–3

New Loss-of-Excitation Relay for Synchronous Generators, A. Mason..........1240–5

New 69-Kv Compressed-Air Circuit Breaker, A. Baker, Frisch..........363–8; disc. 369

Noise Problems on an Electric Power System, Transformer Audio. Murray..........740–7; disc. 747

Nonharmonic Oscillations as Caused by Magnetic Saturation. Rudenberg..........676–85

Nonlinear Mechanics and Servomechanisms, Application of the California Institute of Technology Electric Analog Computer to. McCann, Wilts, Locanthi..........652–60

Novel Test Circuits for Protective Relays. Sonnemann..........1249–54

O

Officers and Committees—1949–1950..........1409–28

Oil Circuit Recloser Substantiate Analytical Co-ordination Method, Field Tests of. Auer, Branflick, Woodward, McKinley..........801–08; disc. 808

Oil, High-Pressure Gas-Filled Cable Impregnated with Extra-High-Viscosity. Sticher, Doan, Atkinson, Meyerhoff..........336–45; disc. 345

Oil Insulated Transformers, Extremes in the Insulation Co-ordination of. Vogel..........965–8; disc. 968

Oil with Variations of Pressure and Temperature, Dielectric Strength of. Hoover, Hixson.....1047–50

Oilless Circuit Breakers, A Manually-Operated Spring Mechanism for Medium Voltage. Wyman, Casey..........357–61; disc. 361

Oklahoma City Primary Network, Improvements to. Brady..........701–02; disc. 702

One Slip Cycle Out-of-Step Relay Equipment. Morris..........1246–8

Operated Spring Mechanism for Medium Voltage Oilless Circuit Breakers, A Manually-. Wyman, Casey..........357–61; disc. 361

Operating Capacitors Without Individual Fuses, The Calculated Risk of. Brooks...1219–25; disc. 1225

Operating Characteristics of Two-Phase Servomotors. Koopman....................319–27; disc. 327

Operation and Maintenance of Shunt Capacitors, Report on the. (Committee Report)............1208–18

Operation in Rhode Island, Trackless Trolley. Blomquist..............................397–402

Operation of a Ground-Fault Neutralizer on a Regulated Distribution System. Tomlinson, Hunt.................................1321–6; disc. 1326

Operation of D-C Motors, Use of Cumulative and Differential Series Fields in the Parallel. Kimball....................................1098–1100

Operation, The Dynamoelectric Amplifier—Class A. Saunders............................1368–73

Operational Calculus Based on the Generalized Functions of Schwartz, A Derivation of Heaviside's Smith, Alger..................939–44; disc. 944

Optical Shutter for Photographic Purposes, An Electro-. Zarem, Marshall, Poole..................84–91

Oscillations as Caused by Magnetic Saturation, Nonharmonic. Rudenberg.................676–85

Oscillographic Methods of Investigating Mechanical. and Hydraulic Conditions During Operation of Hydroelectric Units. Engvall.............93–6

Oscillographing Commutation. Baldwin..................................100–05; disc..105

Outage Rates of Steam Turbines and Boilers and of Hydro Units. (Committee Report)........450–7

Overcurrent Investigation on a Rural Distribution System. Lincks, Edge, McKinley, Leh....................................915–27; disc. 927

Overhead-Conductor 132-Kv Transmission Line at Carrier Current Frequencies, Loss Measurements Made on Underground-Cable. Cornelius, Storer.......................................597–601

Overhead-Line Conductor, Hy-Therm Copper—An Improved. Hickernell, Jones, Snyder....................................22–7; disc. 27

Overheated Transmission Line Joints by Means of a Bolometer, Detection of. Leslie, Wait....................................64–8; disc. 68

Overheating Protection Under Running Overload Conditions, Fundamental Theory of Inherent-. Veinott, Schaefer.................266–70; disc. 270

Overload Conditions, Fundamental Theory of Inherent-Overheating Protection Under Running. Veinott, Schaefer.....................266–70; disc. 270

P

Pacific Gas and Electric Company, Electrical Design Features—Steam-Electric Generating Station P of the. Benson...............1013–20; disc. 1020

Parallel Operation of D-C Motors, Use of Cumulative and Differential Series Fields in the. Kimball.......................................1098–1100

Parameter in Thermal Demand Meters, Heat Flow Ratio as a Design. Nelson...........885–90; disc. 890

Paths, Deionization Time of High-Voltage Fault-Arc. Harrington, Starr...........997–1003; disc. 1003

Patterns of a Fractional Slot Winding, Differential Leakage of the Different. Liwschitz....................................1129–31; disc. 1131

Performance Based on Direct Lightning Strokes, Transmission Line Design and. Harder, Clayton....................................439–46; disc. 447

Performance Characteristics of Lightning Protective Devices. (Committee Report).............1333–6

Performance of a Topped Steam Power Plant on Sudden Load Reduction, The. Noest...........1348–56

Performance of Diesel-Electric Rig for Deep-Well Drilling. Clark, Moore..................497–506

Performance of Distance Relays During Faults and Power Swings, Graphical Method for Estimating the. Warrington.............608–20; disc. 620

Performance of Servomechanisms, Comparison of Steady-State and Transient. Chestnut, Mayer..765–77

Performance of Shunt Capacitors, Report of a Survey on the. (Committee Report)....1200–06; disc. 1206

Performance Records—Steel and Wood Transmission Lines, Comparative. Booker.....686–9; disc. 689

Performance Specification, Governors and the AIEE-ASME Governor. Schwendner, Astley....1330–2

Performance, The Eccentric Pole Face—A Means to Better D-C Machine. Baker...........1107–10

Phenomena of 3-Conductor Cables, Transient Temperature. Wollaston.............1284–95; disc. 1295

Philadelphia, 69-Kv Pipe-Type Cable Circuits in. Kidder, Van Antwerp..............550–6; disc. 556

Photographic Purposes, An Electro-Optical Shutter for. Zarem, Marshall, Poole.................84–91

Pile Voltage Regulators, Fundamentals, and Design Improvements, Aircraft Carbon-. Austin, Richards...983–7

Pipe, A-C Resistance of Segmental Cables in Steel. Meyerhoff, Eager..............816–29; disc. 829

Pipe-Type Cable Circuits in Philadelphia, 69-Kv. Kidder, Van Antwerp..........550–6; disc. 556

Pipes, The Temperature Rise of Buried Cables and. Neher.........................9–17; disc. 17

Plan for a Metropolitan Area, A 66-Kv Subtransmission. Smith, Michelson..............908–12; disc. 912

Plant, A 3,000-Horsepower Gas Turbine Power. Haverstick.....................................458–60

Plant Loading Schedules, Co-ordination of Fuel Cost and Transmission Loss by Use of the Network Analyzer to Determine. George, Page, Ward................................1152–60; disc. 1160

Plant on Sudden Load Reduction, The Performance of a Topped Steam Power. Noest...........1348–56

Plants Prefer Diesel-Electric Switching Locomotives, Industrial. Woods....................697–700

Polar Vector Indicator. Walker, Waynick, Sulzer...154–9

Pole Face—A Means to Better D-C Machine Performance, The Eccentric. Baker............1107–10

Pole Lines, Rubber Insulators for. Wheeler, Markley...195–7

Pole Motor. The Field Fluxes of the Shaded-. Kimberly.................................273–7

Porcelain Potheads, A New Technique in Manufacture of Soldered. Papp, Nicholas..1258–60; disc. 1261

Potential Residential Electric Load, The Heat Pump—Its Significance as a. Bary...................1–8

Potheads, A New Technique in Manufacture of Soldered Porcelain. Papp, Nicholas.1258–60; disc. 1261

Potheads 69,000 Volts and Below, Design Fundamentals of. Nicholas................1263–7; disc. 1267

Power Factor of Unbalanced Sinusoidal 3-Phase Circuits, The Vector. Lamb, Brandt.........507–09

Power-Flow Studies, Equivalent Circuits for. Ward....................................373–80; disc. 380

(Power Generation) A New Automatic Load Control for Turbine Generators. Ruud, Farnham.................................1337–42; disc. 1342

Power Generation and Other Applications, Two Gas Turbines for. Howard..........461–5; disc. 465

(Power Generation) Determining Incremental Regulation and Dead Band of Governors on Steam Turbine Generators Under System Load Conditions. Tornquist, Wade.............................1343–7

(Power Generation) Electrical Design Features—Steam-Electric Generating Station P of the Pacific Gas and Electric Company. Benson...1013–20; disc. 1020

Power Generation, Gas Turbines in Stationary. Hague...467–8

(Power Generation) Governors and the AIEE-ASME Governor Performance Specification. Schwendner, Astley......................................1330–2

(Power Generation) Outage Rates of Steam Turbines and Boilers and of Hydro Units. (Committee Report)..450–7

(Power Generation) Some Oscillographic Methods of Investigating Mechanical and Hydraulic Conditions During Operation of Hydroelectric Units. Engvall...993–6

(Power Generation) The Transient Behavior of the Two-Stage Rototrol Main Exciter Voltage Regulating System as Determined by Electrical Analogy. Carleton..................................59–63

Power-Level Recorder, A Square-Law. Clark, Turner, Williams..................................476–82

Power-Line Carrier Equipment, A Proposal for Broad-Band Coupling of. Miller.....1028–30; disc. 1030

Power Line Carrier for Relaying and Joint Usage. Part I. Hampe, Storer.............864–7; disc. 871

Power Lines, Line Tuning Equipment Used With Coaxial Cable for Carrier Current Installation on. Sutton..........................44–8; disc. 48

Power Plant, A 3,000-Horsepower Gas Turbine. Haverstick.....................................458–60

Power Plant on Sudden Load Reduction, The Performance of a Topped Steam. Noest.......1348–56

Power Rating of Radio-Frequency Cables, The. Mildner..........................289–98; disc. 298

Power Supplies for Coaxial Systems. Spencer..627–30

Power Supplies for Microwave Relay Systems. Ward...631–6

Power Swings, Graphical Method for Estimating the Performance of Distance Relays During Faults and. Warrington..................608–20; disc. 620

Power System Applications, Microwave Channels for. (Committee Report)..............40–2; disc. 42

Power System, Transformer Audio Noise Problems on an Electric. Murray...............740–7; disc. 747

Power Systems, Selenium Rectifiers in Motor Vehicle. Ramsey.....................149–53; disc. 153

Power, The Renaissance of Electric Motive. Candee...138–44

Power Transformers, Controlled Temperature and Insulation Protection in the Operation of. Satterlee, Reed.........................753–8; disc. 759

Power Transformers, Use of Thyrite. Meador..................................959–64; disc. 964

Practical Approach to the Design of High-Voltage Cable Joints, A Theoretical and. Short................................1275–80; disc. 1280

Practices for Shunt Capacitor Banks, Connection Arrangements and Protective. Clark, Farnham..................................1226–30; disc. 1231

Prediction of Ultimate Temperature Rise from Early Heat Run Data. Ryan..................1164–6

Pressure and Temperature, Dielectric Strength of Oil with Variations of. Hoover, Hixson......1047–50

Pressure-Gas-Filled Cable Impregnated with Extra-High-Viscosity Oil, High-. Sticher, Doan, Atkinson, Meyerhoff.....................336–45; disc. 345

Primary Network, Improvements to Oklahoma City. Brady........................701–02; disc. 702

Primary Network Systems in the Washington Area, Design and Experience With 4-Kv. Lank..................................712–16; disc. 716

Primary Networks on the Duquesne Light Company System. Holben, Lange...........704–09; disc. 709

Problems in Magnetic Recording, Application of Experimental Test Procedures and Methods of Analysis of Results to Research. Thompson..........407–17

Problems on an Electric Power System, Transformer Audio Noise. Murray.............740–7; disc. 747

Problems, Summary of Transformations Useful in Constructing Analogs of Linear Vibration. Corbett...661–4

Procedures and Methods of Analysis of Results to Research Problems in Magnetic Recording, Application of Experimental Test. Thompson...407–17

Progress in the Design of Traction Motors, Twenty-Five Years. Baldwin..........................132–7

Progress Report on AIEE Test Code for Electric Brushes. (Committee Report)...................1126–8

Propellers, Development and Application of Electric Heating to Deicing of Aircraft. Sheets, Sand...1068–72

Proposal for Broad-Band Coupling of Power-Line Carrier Equipment, A. Miller.......1028–30; disc. 1030

Protection in a Modern Watt-Hour Meter, Surge. Busch, Williams..................74–7; disc. 77

Protection in the Operation of Power Transformers, Controlled Temperature and Insulation. Satterlee, Reed......................753–8; disc. 759

Protection of Stations Without High-Voltage Switching. (Committee Report)............226–31; disc. 231

Protection of the Airplane Main Bus. Exner...979–82

Protection of Transformer Stations, Automatic Grounding and Air-Break Switches for. Ricker..................................851–6; disc. 856

Protection, Shipshaw Relay. Madill, Duffy..................................50–5; disc. 55

Protection Under Running Overload Conditions, Fundamental Theory of Inherent-Overheating. Veinott, Schaefer.....................266–70; disc. 270

(Protective Devices) Lightning Investigation on a Rural Distribution System. MacCarthy, Stana, Edge, McKinley....................428–37; disc. 437

(Protective Devices) Operation of a Ground-Fault Neutralizer on a Regulated Distribution System. Tomlinson, Hunt.................1321–6; disc. 1326

Protective Devices, Performance Characteristics of Lightning. (Committee Report).................1333–6

(Protective Devices) Report on Survey of Unbalanced Charging Currents on Transmission Lines as Affecting Ground-Fault Neutralizers (Committee Report)..............................1328; disc. 1329

(Protective Devices) Voltage-Recovery Characteristics of Distribution Systems. Witzke..172–80; disc. 180

Protective Practices for Shunt Capacitor Banks, Connection Arrangements and. Clark, Farnham..................................1226–30; disc. 1231

Protective Relays, Novel Test Circuits for. Sonnemann...1249–54

Pulse Transformers, A Method for Designing. Kirschbaum, Warren...........................971–8

Pulsing in Switching, Application of Multifrequency. Dahlbom, Horton, Moody....................392–6

Pump—Its Significance as a Potential Residential Electric Load, The Heat. Bary....................1–8

R

Radio Communication Equipment for Land-Mobile Services Operating on Frequencies Between 152–174 Megacycles, Design of Mobile. Beers, Harris, Zappacosta.................................1232–9

Radio-Frequency Cables, Heating of. Macalpine...283–8

Radio-Frequency Cables, The Power Rating of. Mildner.........................289–98; disc. 298

Radio-Frequency Heating Equipment, Reduction of Interference from. Klingaman..718–24; disc. 724

Radio Influence from High-Voltage Corona. Slemon................................198–204; disc. 204

Radio Influence Voltage Generation, Commutation of D-C Machines and Its Effects on. Motter..................................491–5; disc. 495

Radio Systems, Vibrating Reed Selectors for Mobile. Keller, Bostwick............................383–6

Radiosonde Recorder, Automatic Range-Adjusting. Beggs..602–07

Railroad Service, Application Engineering on Diesel-Electric Locomotives in. Bevan...........125–31

Railroads, Centralized Traffic Control for. Hailes...1167–73

Railway Company Steam Turbine-Electric Locomotives, Electric Equipment for Chesapeake and Ohio. Atwell, Baston....145–8
Range Adjustable-Speed Drive, A Wide-. Conrad, Tribken....789–90
Range-Adjusting Radiosonde Recorder, Automatic. Beggs....602–07
Rates of Steam Turbines and Boilers and of Hydro Units, Outage. (Committee Report)....450–7
Rating of Radio-Frequency Cables, The Power. Mildner....289–98; disc. 298
Ratings of Aircraft Cable, Current. Askew, McClinton....988–92
Ratings, Standardization of Reactor. Kierstead, Thomason....955–7; disc. 957
Ratio as a Design Parameter in Thermal Demand Meters, Heat Flow. Nelson....885–90; disc. 890
Reactance, Saturation Effect of Leakage. Chang, Lloyd....1144–7
Reactances of an Ideal Synchronous Machine, The Negative-Sequence. Duesterhoeft....510–13; disc. 513
Reactor Ratings, Standardization of. Kierstead, Thomason....955–7; disc. 957
Reactors as Fault Limiting Means on High Interrupting Capacity Controllers, The Use of Air-Core. Leitch....469–74; disc. 474
Reactors, Universal Curves for D-C Controllable. Johnson, Merrell, Alley....31–7; disc. 37
Reactors with Resistive Load, Some Fundamentals of D-C Controlled. Storm....188–94; disc. 194
Recloser Substantiate Analytical Co-ordination Method, Field Tests of Oil Circuit. Auer, Branflick, Woodward, McKinley....801–08; disc. 808
Reclosing Circuit Breakers, Insulator Flashover Deionization Times as a Factor in Applying High-Speed. Boisseau, Wyman, Skeats....1058–66; disc. 1066
Reclosing of Line Sectionalizing Circuit Breakers, A-C Tripping and. Winegartner, Bonheimer....1357–9
Recorder, A Square-Law Power-Level. Clark, Turner, Williams....476–82
Recorder, Automatic Range-Adjusting Radiosonde. Beggs....602–07
Recording, Application of Experimental Test Procedures and Methods of Analysis of Results to Research Problems in Magnetic. Thompson....407–17
Records—Steel and Wood Transmission Lines, Comparative Performance. Booker....686–9; disc. 689
Recovery Characteristics of Distribution Systems, Voltage-. Witzke....172–80; disc. 180
Recovery Voltages on Rural Distribution Systems, Short-Circuit Currents and. Eason, Johnson, Kalb, Peterson....930–7; disc. 937
Rectifier Ailments, Diagnosis of. Dortort....1298–1304
Rectifier Fault Currents—II. Herskind, Schmidt, Rettig....243–51; disc. 251
Rectifier Transformer Characteristics. (Committee Report)....1305–11
Rectifiers in Motor Vehicle Power Systems, Selenium. Ramsey....149–53; disc. 153
Reduction of Interference from Radio-Frequency Heating Equipment. Klingaman....718–24; disc. 724
Reduction, The Performance of a Topped Steam Power Plant on Sudden Load. Noest....1348–56
Reed Selective Signaling System for Mobile Telephone Use, Vibrating. Pruden, Hoth....387–91
Reed Selectors for Mobile Radio Systems, Vibrating. Keller, Bostwick....383–6
Reference Frames, Stationary Networks and Transmission Lines Along Uniformly Rotating. Kron....690–6
Regulated Distribution System, Operation of a Ground-Fault Neutralizer on a. Tomlinson, Hunt....1321–6; disc. 1326
Regulating System as Determined by Electrical Analogy, The Transient Behavior of the Two-Stage Rototrol Main Exciter Voltage. Carleton....59–63
Regulation and Dead Band of Governors on Steam Turbine Generators Under System Load Conditions, Determining Incremental. Tornquist, Wade....1343–7
Regulators, Fundamentals, and Design Improvements, Aircraft Carbon-Pile Voltage. Austin, Richards....983–7
Regulators in the L-1 Carrier System, Stability of Tandem. Kinzer....1179–86
Relay and Control Circuits, Insulation Level of. (Committee Report)....1255–7
Relay Circuits, Sequential Aspects of. Ritchie....577–81
Relay Circuits, The Logic of. Keister....571–6
Relay Developments, Electronic. Loving....233–40; disc. 240
Relay Equipment, One Slip Cycle Out-of-Step. Morris....1246–8
Relay for Synchronous Generators, A New Loss-of-Excitation. Mason....1240–5
Relay Protection, Shipshaw. Madill, Duffy....50–5; disc. 55
Relay Servomechanisms, An Analysis of. Kahn....1079–87; disc. 1087
Relay System, A Microwave Communication. Boothroyd, Churchill....637–41
Relay Systems, Power Supplies for Microwave. Ward....631–6
Relay "Trees" and Symmetric Circuits. Washburn....582–6
Relaying and Joint Usage, Power Line Carrier for, Part I. Hampe, Storer....864–7; disc. 871
Relaying of Transmission Lines from the New Sunbury Generating Station. Green, Oewel, Ramsaur....873–7; disc. 877
Relays, Counting with. Frost....587–91
Relays During Faults and Power Swings, Graphical Method for Estimating the Performance of Distance. Warrington....608–20; disc. 620
Relays, Novel Test Circuits for Protective. Sonnemann....1249–54
Remote Control of Coaxial Cable Stations, B-1 Alarm and Control System for. Hearn, Weber....622–6
Renaissance of Electric Motive Power, The. Candee....138–44
Report of a Survey on the Performance of Shunt Capacitors. (Committee Report)....1200–06; disc. 1206
Report of the Board of Directors....1383–1408
Report on AIEE Test Code for Electric Brushes, Progress. (Committee Report)....1126–8
Report on Survey of Unbalanced Charging Currents on Transmission Lines as Affecting Ground-Fault Neutralizers. (Committee Report)...1328; disc. 1329
Report on the Operation and Maintenance of Shunt Capacitors. (Committee Report)....1208–18
Research Problems in Magnetic Recording, Application of Experimental Test Procedures and Methods of Analysis of Results to. Thompson....407–17
Reserve, A Convenient Method for Determining Generator. Seelye....1317–20
Residential Electric Load, The Heat Pump—Its Significance as a Potential. Bary....1–8
Resistance of Segmental Cables in Steel Pipe, A-C. Meyerhoff, Eager....816–29; disc. 829
Resistive Load, Some Fundamentals of D-C Controlled Reactors with. Storm....188–94; disc. 194
Response Curves, Control Chart Methods Applied to Frequency. Mundel....424–7
Results to Research Problems in Magnetic Recording, Application of Experimental Test Procedures and Methods of Analysis of. Thompson....407–17
Rhode Island, Trackless Trolley Operation in. Blomquist....397–402
Rig for Deep-Well Drilling, Performance of Diesel-Electric. Clark, Moore....497–506
Rise from Early Heat Run Data, Prediction of Ultimate Temperature. Ryan....1164–6
Rise of Buried Cables and Pipes, The Temperature. Neher....9–17; disc. 17
Rise of Cables in a Duct Bank, The Temperature. Neher....540–6; disc. 546
Rise Values for D-C Machines, Temperature. Part I. (Committee Report)....206–12; disc. 212
Rise Values for D-C Machines, Temperature. Part II. (Committee Report)....1118–23; disc. 1123
Risk of Operating Capacitors Without Individual Fuses, The Calculated. Brooks....1219–25; disc. 1225
Rooms, Electrically Charged Dust in. Penney, Hewitt....278–82
Rotating Amplifiers, An Analysis of. Litman....1111–17
(Rotating Machinery) A Semiempirical Approach to Voltage Dip—with Suddenly Applied Loads on A-C Generators. Rosenberg....160–6; disc. 166
(Rotating Machinery) A Wide-Range Adjustable-Speed Drive. Conrad, Tribken....789–90
(Rotating Machinery) Armature Iron Losses in Series Motors. Chang, Karr....262–4; disc. 264
(Rotating Machinery) Design of Auxiliary Circuits of Single-Phase Induction Motors. Thacker, Gopalakrishna....1373–9
(Rotating Machinery) Die-Cast Rotors for Induction Motors. Packer....253–60; disc. 260
(Rotating Machinery) Differential Leakage Patterns of a Fractional Slot Winding. Liwschitz....1129–31; disc. 1131
(Rotating Machinery) Effects of Commutator Surface Film Conditions on Commutation. Lynn, Elsey....106–12; disc. 112
(Rotating Machinery) Fundamental Theory of Inherent-Overheating Protection Under Running Overload Conditions. Veinott, Schaefer....266–70; disc. 270
(Rotating Machinery) General Theory of Multiple Cage Induction Motors. Chang....1139–43
(Rotating Machinery) Limitations in Design of D-C Adjustable Speed Motors. Opel....1095–8
(Rotating Machinery) Measuring Commutation with an Indicating Instrument. Lundy....1380–2
(Rotating Machinery) Methods for Determining the Effects of Contaminants on Electrical Insulation. Mathes, Sieffert, Walker, Lindsey....113–18; disc. 118
(Rotating Machinery) Motor Tests Evaluate Thermal Endurance of Class-H Insulation and Silicone Varnish. Grant, Kauppi, Moses, Gibson....1133–8
(Rotating Machinery) Operating Characteristics of Two-Phase Servomotors. Koopman....319–27; disc. 327
(Rotating Machinery) Oscillographing Commutation. Baldwin....100–05; disc. 105
(Rotating Machinery) Saturation Effect of Leakage Reactance. Chang, Lloyd....1144–7
(Rotating Machinery) Solid Short Circuit of D-C Motors and Generators. Linville, Ward....119–23; disc. 123
(Rotating Machinery) Stray Load Losses Measured in D-C Motors. (Committee Report)....219–23; disc. 223
(Rotating Machinery) The Design of D-C Motors for Use in Automatic Control Systems. Lebenbaum....1089–94
(Rotating Machinery) The Dynamoelectric Amplifier—Class A Operation. Saunders....1368–73
(Rotating Machinery) The Eccentric Pole Face—A Means to Better D-C Machine Performance. Baker....1107–10
(Rotating Machinery) The Field Fluxes of the Shaded-Pole Motor. Kimberly....273–7
(Rotating Machinery) The Geometric Loci of the Synchronous Tie. Finzi, Wellard....168–71
(Rotating Machinery) The Negative-Sequence Reactances of an Ideal Synchronous Machine. Duesterhoeft....510–13; disc. 513
(Rotating Machinery) The Torques of the Synchronous Tie—A Steady-State Analysis. Finzi, McConnell....1147–52
(Rotating Machinery) Transient Characteristics of D-C Motors and Generators. McClinton, Brancato, Panoff....1100–06
(Rotating Machinery) Transients in 2-Phase Synchronous Machines. Wilson....1360–7
(Rotating Machinery) Use of Cumulative and Differential Series Fields in the Parallel Operation of D-C Motors. Kimball....1098–1100
Rotating Reference Frames, Stationary Networks and Transmission Lines Along Uniformly. Kron....690–6
Rotors for Induction Motors, Die-Cast. Packer....253–60; disc. 260
Rototrol Main Exciter Voltage Regulating System as Determined by Electrical Analogy, The Transient Behavior of the Two-Stage. Carleton....59–63
Rubber Insulators for Pole Lines. Wheeler, Markley....195–7
Run Data, Prediction of Ultimate Temperature Rise from Early Heat. Ryan....1164–6
Running Overload Conditions, Fundamental Theory of Inherent-Overheating Protection Under. Veinott, Schaefer....266–70; disc. 270
Rural Distribution System, Lightning Investigation on a. MacCarthy, Stann, Edge, McKinley....428–37; disc. 437
Rural Distribution System, Overcurrent Investigation on a. Lincks, Edge, McKinley, Leh....915–27; disc. 927
Rural Distribution Systems, Short-Circuit Currents and Recovery Voltages on. Eason, Johnson, Kalb, Peterson....930–7; disc. 937

S

Saturation Effect of Leakage Reactance. Chang, Lloyd....1144–7
Saturation in Magnetic Amplifiers, Self-. Dornhoefer....835–46; disc. 846
Saturation, Nonharmonic Oscillations as Caused by Magnetic. Rudenberg....676–85
Saving Time in Testing Life of Incandescent Lamps. Purcell....730–2
Scale A-C Voltmeter, A New Expanded. Millar....641–3
Schedules, Co-ordination of Fuel Cost and Transmission Loss by Use of the Network Analyzer to Determine Plant Loading. George, Page, Ward....1152–60; disc. 1160
Schwartz, A Derivation of Heaviside's Operational Calculus Based on the Generalized Functions of. Smith, Alger....939–44; disc. 944
Secondary Conductor Economics, Distribution Transformer and. Brady....483–6; disc. 486
Sectionalizing Circuit Breakers, A-C Tripping and Reclosing of Line. Winegartner, Bonheimer....1357–9
Segmental Cables in Steel Pipe, A-C Resistance of. Meyerhoff, Eager....816–29; disc. 829
Selection of Electric Drives for Looms. Demartini, Lukens....349–56
Selective Signaling System for Mobile Telephone Use, Vibrating Reed. Pruden, Hoth....387–91
Selector Supervisory System, Space Code. Forrest, Schirmer....1312–15; disc. 1315
Selectors for Mobile Radio Systems, Vibrating Reed. Keller, Bostwick....383–6
Selenium Rectifiers in Motor Vehicle Power Systems. Ramsey....149–53; disc. 153
Self-Saturation in Magnetic Amplifiers. Dornhoefer....835–46; disc. 846
Semiempirical Approach to Voltage Dip—with Suddenly Applied Loads on A-C Generators, A. Rosenberg....160–6; disc. 166
Sequence Reactances of an Ideal Synchronous Machine, The Negative-. Duesterhoeft....510–13; disc. 513

Sequential Aspects of Relay Circuits. Ritchie...577–81
Series Fields in the Parallel Operation of D-C Motors, Use of Cumulative and Differential. Kimball..........1098–1100
Series Motors, Armature Iron Losses in. Chang, Karr..........262–4; disc. 264
Series Motors—Experimental Study of Speed-Torque Curves, Dynamic Braking Control of D-C. Heumann..........560–4
Services Operating on Frequencies Between 152–174 Megacycles, Design of Mobile Radio Communication Equipment for Land-Mobile. Beers, Harris, Zappacosta..........1232–9
Servomechanism Components and Systems, Graphical Determination of Transfer Function Loci for. Thomas, Easton..........307–14; disc. 314
Servomechanisms, An Analysis of Relay. Kahn..........1079–87; disc. 1087
Servomechanisms, Application of the California Institute of Technology Electric Analog Computer to Nonlinear Mechanics and. McCann, Wilts, Locanthi..........652–60
Servomechanisms, Comparison of Steady-State and Transient Performance of. Chestnut, Mayer..........675–77
Servomechanisms, Damper Stabilized Instrument. Hall..........299–306; disc. 306
(Servomechanisms) Instrument Inaccuracies in Feed-Back Control Systems with Particular Reference to Backlash. Marcy, Yachter, Zauderer.....778–88
Servomotors, Operating Characteristics of Two-Phase. Koopman..........319–27; disc. 327
Shaded-Pole Motor, The Field Fluxes of the. Kimberly..........273–7
Shipshaw Relay Protection. Madill, Duffy..........50–5; disc. 55
Short-Circuit Conditions, Heating of Transformers Under. Montsinger, Halsey.....947–52; disc. 952
Short-Circuit Currents and Recovery Voltages on Rural Distribution Systems. Eason, Johnson, Kalb, Peterson..........930–7; disc. 937
Short Circuit of D-C Motors and Generators, Solid. Linville, Ward..........119–23; disc. 123
Short-Time Analog Computers, Comparison of Long-Time and. Paschkis..........70–3
Shunt Capacitor Banks, Connection Arrangements and Protective Practices for. Clark, Farnham..........1226–30; disc. 1231
Shunt Capacitors, Report of a Survey on the Performance of. (Committee Report)......1200–06; disc. 1206
Shunt Capacitors, Report on the Operation and Maintenance of. (Committee Report)........1208–18
Shutter for Photographic Purposes, An Electro-Optical. Zarem, Marshall, Poole..........84–91
Signal Synchronous Supervisory Control System, A New 2-. Derr..........857–63; disc. 863
Signaling System for Mobile Telephone Use, Vibrating Reed Selective. Pruden, Hoth..........387–91
Silicone Varnish, Motor Tests Evaluate Thermal Endurance of Class-H Insulation and. Grant, Kauppi, Moses, Gibson..........1133–8
Single-Phase Induction Motors, Design of Auxiliary Circuits of. Thacker, Gopalakrishna.........1373–9
Sinusoidal 3-Phase Circuits, The Vector Power Factor of Unbalanced. Lamb, Brandt..........507–09
66-Kv Subtransmission Plan for a Metropolitan Area, A. Smith, Michelson..........908–12; disc. 913
Slip Cycle Out-of-Step Relay Equipment, One. Morris..........1246–8
Slot Winding, Differential Leakage of the Different Patterns of a Fractional. Liwschitz..........1129–31; disc. 1131
Soil, The Thermal Movement of Moisture in. Mickley..........330–3; disc. 333
Soldered Porcelain Potheads, A New Technique in the Manufacture of. Papp, Nicholas..........1258–60; disc. 1261
Solenoids, Formulas and Tables for the Calculation of the Magnetic Field Components of Circular Filaments and. Grover..........665–75
Solid Short Circuit of D-C Motors and Generators. Linville, Ward..........119–23; disc. 123
Some Fundamentals of D-C Controlled Reactors with Resistive Load. Storm.........188–94; disc. 194
Space Code Selector Supervisory System. Forrest, Schirmer..........1312–15; disc. 1315
Specification, Governors and the AIEE-ASME Governor Performance. Schwendner, Astley...1330–2
Speed, Considerations on Facsimile Transmission. Burkhard..........418–23; disc. 423
Speed Drive, A Wide-Range Adjustable-. Conrad, Tribken..........789–90
Speed Motors, Limitations in Design of D-C Adjustable. Opel..........1095–8
Speed-Torque Curves, Dynamic Braking Control of D-C Series Motors—Experimental Study of. Heumann..........560–4
Spring Mechanism for Medium Voltage Oilless Circuit Breakers, A Manually-Operated. Wyman, Casey..........357–61; disc. 261
Square-Law Power-Level Recorder, A. Clark, Turner, Williams..........476–82
Stability of Tandem Regulators in the L-1 Carrier System. Kinzer..........1179–86
Stability of the Welding Arc, Instrumentation for the Evaluation of the. Winsor, Schetky, Wyant..........525–33
Stabilized Instrument Servomechanisms, Damper. Hall..........299–306; disc. 306
Standardization of Reactor Ratings. Kierstead, Thomason..........955–7; disc. 957
Station P of the Pacific Gas and Electric Company, Electrical Design Features—Steam-Electric Generating. Benson..........1013–20; disc. 1020
Station, Relaying of Transmission Lines from the New Sunbury Generating. Green, Oewel, Ramsaur..........873–7; disc. 877
Stationary Networks and Transmission Lines Along Uniformly Rotating Reference Frames. Kron..........690–6
Stationary Power Generation, Gas Turbines in. Hague..........467–8
Stations, Automatic Grounding and Air-Break Switches for Protection of Transformer. Ricker..........851–6; disc. 856
Stations, B-1 Alarm and Control System for Remote Control of Coaxial Cable. Hearn, Weber......622–6
Stations Without High-Voltage Switching, Protection of. (Committee Report)........226–31; disc. 231
Steady-State Analysis, The Torques of the Synchronous Tie—A. Finzi, McConnell..........1147–52
Steady-State and Transient Performance of Servomechanisms, Comparison of. Chestnut, Mayer..........765–77
Steam-Electric Generating Station P of the Pacific Gas and Electric Company, Electrical Design Features—. Benson..........1013–20; disc. 1020
Steam Power Plant on Sudden Load Reduction, The Performance of a Topped. Noest........1348–56
Steam Turbine-Electric Locomotives, Electric Equipment for Chesapeake and Ohio Railway Company. Atwell, Baston..........145–8
Steam Turbine Generators Under System Load Conditions, Determining Incremental Regulation and Dead Band of Governors on. Tornquist, Wade..........1343–7
Steam Turbines and Boilers and of Hydro Units, Outage Rates of. (Committee Report)..........450–7
Steel and Wood Transmission Lines, Comparative Performance Records—. Booker....686–9; disc. 689
Steel Pipe, A-C Resistance of Segmental Cables in. Meyerhoff, Eager..........816–29; disc. 829
Stray Load Losses Measured in D-C Motors. (Committee Report)..........219–23; disc. 223
Strength of Oil with Variations of Pressure and Temperature, Dielectric. Hoover, Hixson....1047–50
Stroke Damage to Aircraft, Lightning. Hagenguth..........1036–44; disc. 1044
Strokes, Transmission Line Design and Performance Based on Direct Lightning. Harder, Clayton..........439–46; disc. 447
Studies, Equivalent Circuits for Power-Flow. Ward..........373–80; disc. 380
(Substations) A New 2-Signal Synchronous Supervisory Control System. Derr.........857–63; disc. 863
(Substations) Automatic Grounding and Air-Break Switches for Protection of Transformer Stations. Ricker..........851–6; disc. 856
(Substations) Space Code Selector Supervisory System. Forrest, Schirmer..........1312–15; disc. 1315
Subtransmission Plan for a Metropolitan Area, A 66-Kv. Smith, Michelson..........908–12; disc. 912
Suddenly Applied Loads on A-C Generators, A Semi-empirical Approach to Voltage Dip—with. Rosenberg..........160–6; disc. 166
Summary of Transformations Useful in Constructing Analogs of Linear Vibration Problems. Corbett..........661–4
Sunbury Generating Station, Relaying of Transmission Lines from the New. Green, Oewel, Ramsaur..........873–7; disc. 877
Supervisory Control System, A New 2-Signal Synchronous. Derr..........857–63; disc. 863
Supervisory System, Space Code Selector. Forrest, Schirmer..........1312–15; disc. 1315
Supplies for Coaxial Systems, Power. Spencer..627–30
Supplies for Microwave Relay Systems, Power. Ward..........631–6
Surface Film Conditions on Commutation, Effects of Commutator. Lynn, Elsey......106–12; disc. 112
Surge Protection in a Modern Watt-Hour Meter. Busch, Williams..........74–7; disc. 77
Survey of Unbalanced Charging Currents on Transmission Lines as Affecting Ground-Fault Neutralizers, Report on. (Committee Report)..........1328; disc. 1329
Survey on the Performance of Shunt Capacitors, Report of a. (Committee Report)....1200–06; disc. 1206
Swings, Graphical Method for Estimating the Performance of Distance Relays During Faults and Power. Warrington..........608–20; disc. 620
Switches for Protection of Transformer Stations, Automatic Grounding and Air-Break. Ricker..........851–6; disc. 856
(Switchgear) A Manually-Operated Spring Mechanism for Medium Voltage Oilless Circuit Breakers. Wyman, Casey..........357–61; disc. 361
(Switchgear) A New 69-Kv Compressed-Air Circuit Breaker. Baker, Frisch..........363–8; disc. 369
(Switchgear) A-C Tripping and Reclosing of Line Sectionalizing Circuit Breakers. Winegartner, Bonheimer..........1357–9
(Switchgear) Field Tests of Oil Circuit Recloser Substantiate Analytical Co-ordination Method. Auer, Branflick, Woodward, McKinley..........801–08; disc. 808-
(Switchgear) Insulator Flashover Deionization Times as a Factor in Applying High-Speed Reclosing Circuit Breakers. Boisseau, Wyman, Skeats..........1058–66; disc. 1066
Switching, Application of Multifrequency Pulsing in. Dahlbom, Horton, Moody..........392–6
Switching Locomotives, Industrial Plants Prefer Diesel-Electric. Woods..........697–700
Switching, Protection Stations Without High-Voltage. (Committee Report)..........226–31; disc. 231
Symmetric Circuits, Relay "Trees" and. Washburn..........582–6
Synchronous Generators, A New Loss-of-Excitation Relay for. Mason..........1240–5
Synchronous Machine, The Negative-Sequence Reactances of an Ideal. Duesterhoeft..........510–13; disc. 513
Synchronous Machines, Transients in 2-Phase. Wilson..........1360–7
Synchronous Supervisory Control System, A New 2-Signal. Derr..........857–63; disc. 863
Synchronous Tie—A Steady-State Analysis, The Torques of the. Finzi, McConnell.......1147–52
Synchronous Tie, The Geometric Loci of the. Finzi, Wellard..........168–71
System, A Microwave Communication Relay. Boothroyd, Churchill..........637–41
System, A New 2-Signal Synchronous Supervisory Control. Derr..........857–63; disc. 863
System, A Time Division Multiplexing. Boothroyd, Creamer..........92–7
System Applications, Microwave Channels for Power. (Committee Report)..........40–2; disc. 42
System as Determined by Electrical Analogy, The Transient Behavior of the Two-Stage Rototrol Main Exciter Voltage Regulating. Carleton......59–63
System, Economic Loading of a Transformer. Levine, Smith..........733–8; disc. 738
System for Aircraft, An Electric Deicing. Giloy..........1051–7
System for Mobile Telephone Use, Vibrating Reed Selective Signaling. Pruden, Hoth.......387–91
System for Remote Control of Coaxial Cable Stations, B-1 Alarm and Control. Hearn, Weber....622–6
System, Lightning Investigation on a Rural Distribution. MacCarthy, Stann, Edge, McKinley..........428–37; disc. 437
System, Matching Electrical Characteristics of McNary Generators and Transformers with Bonneville Power Administration Transmission. Hoard, Weiner..........1022–5; disc. 1026
System, Operation of a Ground-Fault Neutralizer on a Regulated Distribution. Tomlinson, Hunt..........1321–6; disc. 1326
System, Overcurrent Investigation on a Rural Distribution. Lincks, Edge, McKinley, Leh..........915–27; disc. 927
System, Primary Networks on the Duquesne Light Company. Holben, Lange......704–09; disc. 709
System, Space Code Selector Supervisory. Forrest, Schirmer..........1312–15; disc. 1315
System, Stability of Tandem Regulators in the L-1 Carrier. Kinzer..........1179–86
System, Transformer Audio Noise Problems on an Electric Power. Murray.........740–7; disc. 747
Systems, Graphical Determination of Transfer Function Loci for Servomechanism Components and. Thomas, Easton..........307–14; disc. 314
Systems in the Washington Area, Design and Experience with 4-Kv Primary Network. Lank..........712–16; disc. 716
Systems, Power Supplies for Coaxial. Spencer..627–30
Systems, Power Supplies for Microwave Relay. Ward..........631–6
Systems, Selenium Rectifiers in Motor Vehicle Power. Ramsey..........149–53; disc. 153
Systems, Short-Circuit Currents and Recovery Voltages on Rural Distribution. Eason, Johnson, Kalb, Peterson..........930–7; disc. 937
Systems, Television Terminals for Coaxial. Morrison..........1193–9
Systems, The Design of D-C Motors for Use in Automatic Control. Lebenbaum..........1089–94
Systems, Vibrating Reed Selectors for Mobile Radio. Keller, Bostwick..........383–6
Systems, Voltage-Recovery Characteristics of Distribution. Witzke..........172–80; disc. 180
Systems with Particular Reference to Backlash, Instrument Inaccuracies in Feed-Back Control. Marcy, Yachter, Zauderer..........778–88

T

Tables for the Calculation of the Magnetic Field Components of Circular Filaments and Solenoids, Formulas and. Grover..................665–75
Tandem Regulators in the L-1 Carrier System, Stability of. Kinzer..........................1179–86
Technique in the Manufacture of Soldered Porcelain Potheads, A New. Papp, Nicholas..1258–60; disc. 1261
Technique of Lightning Measurements, Advances in. Brownlee...............................515–19
Techniques, Cable Accessory Design Utilizing New Laboratory. Bosworth, Farr..1268–73; disc. 1273
Telemetering and Totalizing, A Thermal Converter for. Downing.....................879–84; disc. 884
Telephone Use, Vibrating Reed Selective Signaling System for Mobile. Pruden, Hoth........387–91
Television Terminals for Coaxial Systems. Morrison...1193–9
Temperature and Insulation Protection in the Operation of Power Transformers, Controlled. Satterlee, Reed.........................753–8; disc. 759
Temperature, Dielectric Strength of Oil with Variations of Pressure and. Hoover, Hixson........1047–50
Temperature Phenomena of 3-Conductor Cables, Transient. Wollaston........1284–95; disc. 1295
Temperature Rise from Early Heat Run Data, Production of Ultimate. Ryan.................1164–6
Temperature Rise of Buried Cables and Pipes, The. Neher..........................9–17; disc. 17
Temperature Rise of Cables in a Duct Bank, The. Neher.........................540–6; disc. 546
Temperature Rise Values for D-C Machines. Part I. (Committee Report)............206–12; disc. 212
Temperature Rise Values for D-C Machines. Part II. (Committee Report)..........1118–23; disc. 1123
Temperature, The Measurement of Dielectric Loss at High Frequencies and Under Changing. Whitehead, Rueggeberg.......................520–4
Terminals for Coaxial Systems, Television. Morrison...1193–9
Test Circuits for Protective Relays, Novel. Sonnemann...1249–54
Test Code for Electric Brushes, Progress Report in AIEE. (Committee Report)....................1126–8
Test Procedures and Methods of Analysis of Results to Research Problems in Magnetic Recording, Application of Experimental. Thompson.......407–17
Tests Evaluate Thermal Endurance of Class H Insulation and Silicone Varnish, Motor. Grant, Kauppi, Moses, Gibson..........................1133–8
Tests of Oil Circuit Recloser Substantiate Analytical Co-ordination Method, Field. Auer, Branflick, Woodward, McKinley..........801–08; disc. 808
Theoretical and Practical Approach to the Design of High-Voltage Cable Joints, A. Short.....................................1275–80; disc. 1280
Theory and Experimental Confirmation of the Moving Coil Fluxmeter, General. Higgins, Robertson................................897–907; disc. 907
Theory of Inherent-Overheating Protection Under Running Overload Conditions, Fundamental. Veinott, Schaefer..............266–70; disc. 270
Theory of Multiple Cage Induction Motors, General. Chang...............................1139–43
Thermal Converter for Telemetering and Totalizing, A. Downing.....................879–84; disc. 884
Thermal Demand Meters, Heat Flow Ratio as a Design Parameter in. Nelson...........885–90; disc. 890
Thermal Endurance of Class H Insulation and Silicone Varnish, Motor Tests Evaluate. Grant, Kauppi, Moses, Gibson..........................1133–8
Thermal Movement of Moisture in Soil, The. Mickley.............................330–3; disc. 333
Thermal Volt-Ampere Demand Meter, A New. Douglass, Morong......................79–83; disc. 83
Three-Phase Circuits, The Vector Power Factor of Unbalanced Sinusoidal. Lamb, Brandt....507–09
Thyrite in Power Transformers, Use of. Meador..................................959–64; disc. 964
Tie—A Steady-State Analysis, The Torques of the Synchronous. Finzi, McConnell.........1147–52
Tie, The Geometric Loci of the Synchronous. Finzi, Wellard..............................168–71
Time Division Multiplexing System, A. Boothroyd, Creamer..................................92–7
Time in Testing Life of Incandescent Lamps, Saving. Purcell.................................730–2
Time of High-Voltage Fault-Arc Paths, Deionization. Harrington, Starr..........997–1003; disc. 1003
Times as a Factor in Applying High-Speed Reclosing Circuit Breakers, Insulator Flashover Deionization. Boisseau, Wyman, Skeats......1058–66; disc. 1066
Topped Steam Power Plant on Sudden Load Reduction, The Performance of a. Noest..........1348–56
Torque Curves, Dynamic Braking Control of D-C Series Motors—Experimental Study of Speed-. Heumann...............................560–4
Torques of the Synchronous Tie—A Steady-State Analysis, The. Finzi, McConnell............1147–52
Totalizing, A Thermal Converter for Telemetering and. Downing.....................879–84; disc. 884
Trackless Trolley Operation in Rhode Island. Blomquist..............................397–402
Traction Motors, Twenty-Five Years Progress in the Design of. Baldwin.....................132–7
Traffic Control for Railroads, Centralized. Hailes...1167–73
Transfer Function Loci for Servomechanism Components and Systems, Graphical Determination of. Thomas, Easton................307–14; disc. 314
Transformations Useful in Constructing Analogs of Linear Vibration Problems, Summary of. Corbett...661–4
Transformer and Secondary Conductor Economics, Distribution. Brady............483–6; disc. 486
Transformer Audio Noise Problems on an Electric Power System. Murray..............740–7; disc. 747
Transformer Characteristics, Rectifier. (Committee Report)..............................1305–11
Transformer Manholes and Vaults: Design and Ventilation. Porter...........................534–9
(Transformer) Some Fundamentals of D-C Controlled Reactors with Resistive Load. Storm.....................................188–94; disc. 194
Transformer Stations, Automatic Grounding and Air-Break Switches for Protection of. Ricker.....................................851–6; disc. 856
Transformer System, Economic Loading of a. Levine, Smith.........................733–8; disc. 738
(Transformer) Universal Curves for D-C Controllable Reactors. Johnson, Merrell, Alley...31–7; disc. 37
Transformers, A Method for Designing Pulse. Kirschbaum, Warren..........................971–8
(Transformers) A Small High-Voltage Bushing Design for High Altitudes. Vogel, Hart..........761–4
Transformers, Controlled Temperature and Insulation Protection in the Operation of Power. Satterlee, Reed.........................753–8; disc. 759
Transformers, Extremes in the Insulation Co-ordination of Oil Insulated. Vogel..........965–8; disc. 968
Transformers, Lightweight Aircraft. Stephens..1073–8
(Transformers) Standardization of Reactor Ratings. Kierstead, Thomason............955–7; disc. 957
Transformers Under Short-Circuit Conditions, Heating of. Montsinger, Halsey.........947–52; disc. 952
Transformers, Use of Thyrite in Power. Meador..................................959–64; disc. 964
Transformers with Bonneville Power Administration Transmission System, Matching Electrical Characteristics of McNary Generators and. Hoard, Weiner.....................1022–5; disc. 1026
Transient Behavior of the Two-Stage Rototrol Main Exciter Voltage Regulating System as Determined by Electrical Analogy, The. Carleton......59–63
Transient Characteristics of D-C Motors and Generators. McClinton, Brancato, Panoff............1100–06
Transient Fault Locator for High-Voltage Transmission Lines, A. Spaulding, Diemond.................................1005–10; disc. 1010
Transient Performance of Servomechanisms, Comparison of Steady-State and. Chestnut, Mayer...765–77
Transient Temperature Phenomena of 3-Conductor Cables. Wollaston..........1284–95; disc. 1295
Transients in 2-Phase Synchronous Machines. Wilson...1360–7
Translations, Codes and. Myers..............592–7
(Transmission) A 66-Kv Subtransmission Plan for a Metropolitan Area. Smith, Michelson.................................908–12; disc. 912
(Transmission) Connection Arrangements and Protective Practices for Shunt Capacitor Banks. Clark, Farnham...................1226–30; disc. 1231
(Transmission) Deionization Time of High-Voltage Fault-Arc Paths. Harrington, Starr.................................997–1003; disc. 1003
(Transmission) Design and Experience With 4-Kv Primary Network Systems in the Washington Area. Lank.........................712–16; disc. 716
(Transmission) Hy-Therm Copper—An Improved Overhead-Line Conductor. Hickernell, Jones, Snyder.........................22–7; disc. 27
(Transmission) Improvements to Oklahoma City Primary Network. Brady.......701–02; disc. 702
Transmission Line at Carrier Current Frequencies, Loss Measurements Made on Underground-Cable Overhead-Conductor 132-Kv. Cornelius, Storer...597–601
Transmission Line Clamps, Magnetic Heating of. Crabb, Sheadel...................... 1032–5
Transmission Line Design and Performance Based on Direct Lightning Strokes. Harder, Clayton.................................439–46; disc. 447
Transmission Line Joints by Means of a Bolometer, Detection of Overheated. Leslie, Wait.................................64–8; disc. 68
Transmission Lines, A Transient Fault Locator for High-Voltage. Spaulding, Diemond.................................1005–10; disc. 1010
Transmission Lines Along Uniformly Rotating Reference Frames, Stationary Networks and. Kron...690–6
Transmission Lines as Affecting Ground-Fault Neutralizers, Report on Survey of Unbalanced Charging Currents on. (Committee Report)...1328; disc. 1329
Transmission Lines, Comparative Performance Records—Steel and Wood. Booker..686–9; disc. 689
Transmission Lines from the New Sunbury Generating Station, Relaying of. Green, Oewel, Ramsaur.................................873–7; disc. 877
Transmission Loss by Use of the Network Analyzer to Determine Plant Loading Schedules, Co-ordination of Fuel Cost and. George, Page, Ward.................................1152–60; disc. 1160
(Transmission) Primary Networks on the Duquesne Light Company System. Holben, Lange.................................704–09; disc. 709
(Transmission) Report of a Survey on the Performance of Shunt Capacitors. (Committee Report).................................1200–06; disc. 1206
(Transmission) Report on the Operation and Maintenance of Shunt Capacitors. (Committee Report).................................1208–18
Transmission Speed, Considerations on Facsimile. Burkhard......................418–23; disc. 423
Transmission System, Matching Electrical Characteristics of McNary Generators and Transformers with Bonneville Power Administration. Hoard, Weiner.................................1022–5; disc. 1026
(Transmission) The Calculated Risk of Operating Capacitors Without Individual Fuses. Brooks.................................1219–25; disc. 1225
(Transportation, Air) Aircraft Carpon-Pile Voltage Regulators, Fundamentals, and Design Improvements. Austin, Richards.................983–7
(Transportation, Air) An Electric Deicing System for Aircraft. Giloy.........................1051–7
(Transportation, Air) Current Ratings of Aircraft Cable. Askew, McClinton.....................988–92
(Transportation, Air) Development and Application of Electric Heating to Deicing of Aircraft Propellers. Sheets, Sand.........................1068–72
(Transportation, Air) Lightning Stroke Damage to Aircraft. Hagenguth...........1036–44; disc. 1044
(Transportation, Air) Lightweight Aircraft Transformers. Stephens.........................1073–8
(Transportation, Air) Protection of the Airplane Main Bus. Exner...........................979–82
(Transportaton, Land) Application Engineering on Diesel-Electric Locomotives in Railroad Service. Bevan...............................125–31
(Transportation, Land) Centralized Traffic Control for Railroads. Hailes...................1167–73
(Transportation, Land) Electric Equipment for Chesapeake and Ohio Railway Company Steam Turbine-Electric Locomotives. Atwell, Baston......145–8
(Transportation, Land) Industrial Plants Prefer Diesel-Electric Switching Locomotives. Woods..697–700
(Transportation, Land) Selenium Rectifiers in Motor Vehicle Power Systems. Ramsey..149–53; disc. 153
(Transportation, Land) The Renaissance of Electric Motive Power. Candee.................138–44
(Transportation, Land) Trackless Trolley Operation in Rhode Island. Blomquist..............397–402
(Transportation, Land) Trolley Coaches Replace Busses. McMillon.........................403–06
(Transportation, Land) Twenty-Five Years Progress in the Design of Traction Motors. Baldwin....132–7
"Trees" and Symmetric Circuits, Relay. Washburn...582–6
Tripping and Reclosing of Line Sectionalizing Circuit Breakers, A-C. Winegartner, Bonheimer...1357–9
Trolley Coaches Replace Busses. McMillon....403–06
Trolley Operation in Rhode Island, Trackless. Blomquist..............................397–402
Tuning Equipment Used With Coaxial Cable for Carrier Current Installation on Power Lines, Line. Sutton.................................44–8; disc. 48
Turbine-Electric Locomotives, Electric Equipment for Chesapeake and Ohio Railway Company Steam. Atwell, Baston.............................145–8
Turbine Generators, A New Automatic Load Control for. Ruud, Farnham........1337–42; disc. 1342
Turbine Generators Under System Load Conditions, Determining Incremental Regulation and Dead Band of Governors on Steam. Tornquist, Wade...1343–7
Turbine Power Plant, A 3,000-Horsepower Gas. Haverstick...............................458–60
Turbines and Boilers and of Hydro Units, Outage Rates of Steam. (Committee Report)...........450–7
Turbines for Power Generation and Other Applications, Two Gas. Howard.............461–5; disc. 465
Turbines in Stationary Power Generation, Gas. Hague...467–8
Twenty-Five Years Progress in the Design of Traction Motors. Baldwin......................132–7
Two Gas Turbines for Power Generation and Other Applications. Howard..........461–5; disc. 465
Two-Phase Servomotors, Operating Characteristics of. Koopman.....................319–27; disc. 327
Two-Stage Rototrol Main Exciter Voltage Regulating System as Determined by Electrical Analogy, The Transient Behavior of the. Carleton.......59–63

U

Unbalanced Charging Currents on Transmission Lines as Affecting Ground-Fault Neutralizers, Report on Survey of. (Committee Report)............1328; disc. 1329
Unbalanced Sinusoidal 3-Phase Circuits, The Vector Power Factor of. Lamb, Brandt..........507–09
Underground-Cable Overhead-Conductor 132-Kv Transmission Line at Carrier Current Frequencies, Loss Measurements Made on. Cornelius, Storer.........597–601
Units, Some Osvillographic Methods of Investigating Mechanical and Hydraulic Conditions During Operation of Hydroelectric. Engvall.......993–6
Universal Curves for D-C Controllable Reactors. Johnson, Merrell, Alley............31–7; disc. 37
Usage, Power Line Carrier for Relaying and Joint. Part I. Hampe, Storer..........864–7; disc. 871
Use of Air-Core Reactors as Fault Limiting Means on High Interrupting Capacity Controllers, The. Leitch......................469–74; disc. 474
Use of Thyrite in Power Transformers. Meador.........959–64; disc. 964

V

Values for D-C Machines, Temperature Rise. Part I. (Committee Report)............206–12; disc. 212
Values for D-C Machines, Temperature Rise. Part II. (Committee Report)..........1118–23; disc. 1123
Variations of Pressure and Temperature, Dielectric Strength of Oil with. Hoover, Hixson....1047–50
Varnish, Motor Tests Evaluate Thermal Endurance of Class-H Insulation and Silicone. Grant, Kauppi, Moses, Gibson.............................1133–8
Vaults: Design and Ventilation, Transformer Manholes and. Porter..........................534–9
Vector Indicator, Polar. Walker, Waynick, Sulzer.........154–9
Vector Power Factor of Unbalanced Sinusoidal 3-Phase Circuits, The. Lamb, Brandt............507–09
Vehicle Power Systems, Selenium Rectifiers in Motor. Ramsey.....................149–53; disc. 153
Ventilation, Transformer Manholes and Vaults: Design and. Porter..........................534–9
Vibrating Reed Selective Signaling System for Mobile Telephone Use. Pruden, Hoth...........387–91
Vibrating Reed Selectors for Mobile Radio Systems. Keller, Bostwick..........................383–6
Vibration Problems, Summary of Transformations Useful in Constructing Analogs of Linear. Corbett.........661–4
Viscosity Oil, High-Pressure Gas-Filled Cable Impregnated with Extra-High-. Sticher, Doan, Atkinson, Meyerhoff...........336–45; disc. 345
Volt-Ampere Demand Meter, A New Thermal. Douglass, Morong.....................79–83; disc. 83
Voltage Dip—with Suddenly Applied Loads on A-C Generators, A Semiempirical Approach to. Rosenberg.........................160–6; disc. 166
Voltage Fault-Arc Paths, Deionization Time of High-. Harrington, Starr...........997–1003; disc. 1003
Voltage Generation, Commutation of D-C Machines and Its Effects on Radio Influence. Motter.........491–5; disc. 495
Voltage Oilless Circuit Breakers, A Manually-Operated Spring Mechanism for Medium. Wyman, Casey.........357–61; disc. 361
Voltage Recovery Characteristics of Distribution Systems. Witzke.................172–80; disc. 180
Voltage Regulating System as Determined by Electrical Analogy, The Transient Behavior of the Two-Stage Rototrol Main Exciter. Carleton.....59–63
Voltage Regulators, Fundamentals, and Design Improvements, Aircraft Carbon-Pile. Austin, Richards.................................983–7
Voltage Transmission Lines, A Transient Fault Locator for High-. Spaulding, Diemond.........1005–10; disc. 1010
Voltages on Rural Distribution Systems, Short-Circuit Currents and Recovery. Eason, Johnson, Kalb, Peterson......................930–7; disc. 937
Voltmeter, A New Expanded Scale A-C. Millar..641–3
Volts and Below, Design Fundamentals of Potheads 69,000. Nicholas..............1263–7; disc. 1267

W

Washington Area, Design and Experience With 4-Kv Primary Network Systems in the. Lank.........712–16; disc. 716
Watt-Hour Meter, Surge Protection in a Modern. Busch, Williams...................74–7; disc. 77
Welding Arc, Instrumentation for the Evaluation of the Stability of the. Winsor, Schetky, Wyant..525–33
Well Drilling, Performance of Diesel-Electric Rig for Deep. Clark, Moore......................497–506
Wide-Band D-C Amplifier Stabilized for Gain and for Zero. Williams, Amey, McAdam.811–15; disc. 815
Wide-Range Adjustable-Speed Drive, A. Conrad, Tribken.................................789–90
Winding, Differential Leakage of the Different Patterns of a Fractional Slot. Liwschitz.........1129–31; disc. 1131
Wood Transmission Lines, Comparative Performance Records—Steel and. Booker.....686–9; disc. 689

Z

Zero, Wide-Band D-C Amplifier Stabilized for Gain and for. Williams, Amey, McAdam.........811–15; disc. 815

2. Author Index

Abetti, P. A. Disc.............................944
Adams, J. A. Disc.............................749
Alexander, E. H. Disc.........................474
Alger, P. L. Disc.........................214, 1131
Alger, P. L.; J. J. Smith. A Derivation of Heaviside's Operational Calculus Based on the Generalized Functions of Schwartz..........939–44; disc. 945
Alley, R. E., Jr.; W. C. Johnson, B. C. Merrell. Universal Curves for D-C Controllable Reactors.........31–7; disc. 39
Amey, W. G.; A. J. Williams, Jr., Will McAdam. Wide-Band D-C Amplifier Stabilized for Gain and for Zero....................811–15; disc. 815
Andrews, F. E. Disc...........................447
Asbury, C. E. Disc..................871, 877, 927
Askew, Wade H.; A. T. McClinton. Current Ratings of Aircraft Cable........................988–92
Astley, Wayne C.; A. F. Schwendner. Governors and the AIEE–ASME Governor Performance Specifications................................1330–2
Atkinson, R. W. Disc.....................557, 1281
Atkinson, R. W.; Joseph Sticher, G. H. Doan, Louis Meyerhoff. High-Pressure Gas-Filled Cable Impregnated with Extra-High-Viscosity Oil.........336–45; disc. 347
Atwell, C. A.; C. E. Baston. Electric Equipment for Chesapeake and Ohio Railway Company Steam Turbine-Electric Locomotives..............145–8
Auer, G. G.; R. A. Branflick, L. J. Woodward, W. C. McKinley. Field Tests of Oil Circuit Recloser Substantiate Analytical Co-ordination Method.........801–08; disc. 810
Austin, B. O.; H. H. C. Richards. Aircraft Carbon-Pile Voltage Regulators, Fundamentals and Design Improvements..........................983–7

B

Baker, B. P.; E. Frisch. A New 69-Kv Compressed-Air Circuit Breaker.................363–8; disc. 371
Baker, M. A. The Eccentric Pole Face—A Means to Better D-C Machine Performance........1107–10
Baker, M. A. Disc.......................215, 1123
Baldwin, M. J. Oscillographing Commutation.........100–05; disc. 105
Baldwin, M. J. Twenty-Five Years Progress in the Design of Traction Motors..................132–7
Barnett, H. G.; D. F. Shankle. Disc..............914
Bary, Constantine. The Heat Pump—Its Significance as a Potential Residential Electric Load.......1–8
Baston, C. E.; C. A. Atwell. Electric Equipment for Chesapeake and Ohio Railway Company Steam Turbine-Electric Locomotives..............145–8
Bauer, C. A.; H. Halperin. Disc................1296
Beaumariage, D. C.; D. W. VerPlanck, L. A. Finzi. Analytical Determination of Characteristics of Magnetic Amplifiers with Feedback............565–70
Beaumariage, D. C.; L. A. Finzi, D. W. VerPlanck. Disc..................................846
Beck, E. Disc..................................185
Beers, R. A.; W. A. Harris, A. D. Zappacosta. Design of Mobile Radio Communication Equipment for Land-Mobile Services Operating on Frequencies Between 152–174 Megacycles.............1232–9
Beggs, George E., Jr. Automatic Range-Adjusting Radiosonde Recorder.......................602–07
Behn, Eric R. Disc.............................306
Bellaschi, P. L. Disc.................447, 759, 969
Bennet, W. R. Disc.............................260
Bennon, S. Disc................................748
Benson, Francis S. Electrical Design Features—Steam-Electric Generating Station P of the Pacific Gas and Electric Company.......................1013–20
Berkowitz, R. Disc.............................649
Bevan, G. T. Application Engineering on Diesel-Electric Locomotives in Railroad Service...125–31
....447
Bewley, L. V. Disc.............................381
Bills, Glenn W. Disc...........................913
Blake, D. K. Disc..............................747
Blomquist, E. A. Disc..........................
Blomquist, H. R. Trackless Trolley Operation in Rhode Island........................397–402
Boisseau, A. C.; B. W. Wyman, W. F. Skeats. Insulator Flashover Deionization Times as a Factor in Applying High-Speed Reclosing Circuit Breakers.........1058–66; disc. 1066
Bonheimer, H. E.; C. E. Winegartner. A-C Tripping and Reclosing of Line Sectionalizing Circuit Breakers..1357–9
Bonneville Power Administration, Engineers of. Disc.........27
Bonneville Power Administration, Engineers of the. Disc.........69
Booker, C. A. Comparative Performance Records—Steel and Wood Transmission Lines.........686–9; disc. 689
Boothroyd, Wilson P.; H. J. Churchill. A Microwave Communication Relay System..............637–41
Boothroyd, W. P.; E. M. Creamer, Jr. A Time Division Multiplexing System..................92–7
Borden, Perry A. Disc..........................884
Bostwick, L. G.; A. C. Keller. Vibrating Reed Selectors for Mobile Radio Systems.............383–6
Bosworth, Phillips N.; H. K. Farr. Cable Accessory Design Utilizing New Laboratory Techniques.........1268–73; disc. 1274
Brady, Bryce. Distribution Transformer and Secondary Conductor Economics...........483–6; disc. 489
Brady, Bryce. Improvements to Oklahoma City Primary Network................701–02; disc. 703
Brancato, E. L.; A. T. McClinton, R. Panoff. Transient Characteristics of D-C Motors and Generators.........1100–06
Brandt, D. B.; J. F. Lamb. The Vector Power Factor of Unbalanced Sinusoidal 3-Phase Circuits..507–09
Branflick, R. A.; G. G. Auer, L. J. Woodward, W. C. McKinley. Field Tests of Oil Circuit Recloser Substantiate Analytical Co-ordination Method.........801–08; disc. 810
Brooks, Hamilton. The Calculated Risk of Operating Capacitors Without Individual Fuses.....1219–25
Brown, J. Stanley. Disc........................361
Brown, W. T. Disc..............................488
Browne, T. E., Jr. Disc.......................1003
Brownlee, Theodore. Advances in Technique of Lightning Measurements........................515–19
Brownlee, W. R. Disc...........................856
Brownlee, W. R.; J. A. Elzi. Disc..............738
Buckland, Mrs. B. O.; T. M. Linville. Disc.......218
Buller, F. H. Disc.......................546, 830
Burkhard, Henry F. Considerations on Facsimile Transmission Speed............418–23; disc. 423
Busch, F. H.; G. D. Williams. Surge Protection in a Modern Watt-Hour Meter.........74–7; disc. 78
Butler, F. H. Disc...............................17

C

Cahen, Francois. Disc..........................204
Caldwell, B. H. Disc.....................223, 1124
Caldwell, B. H.; G. H. Gunnoe. Disc............212
Campbell, H. E. Disc.....................486, 702
Candee, A. H. The Renaissance of Electric Motive Power...................................138–44
Carleton, James T. The Transient Behavior of the Two-Stage Rototrol Main Exciter Voltage Regulating System as Determined by Electrical Analogy.........59–63
Carlisle, R. W. Disc...........................423
Casey, E. J.; B. W. Wyman. A Manually Operated Spring Mechanism for Medium Voltage Oilless Circuit Breakers................357–61; disc. 362
Chang, Sheldon S. L. General Theory of Multiple Cage Induction Motors................1139–43
Chang, S. S. L. Disc...........................327
Chang, Sheldon S. L.; J. H. Karr. Armature Iron Losses in Series Motors..........262–4; disc. 265
Chang, Sheldon S. L.; T. C. Lloyd. Saturation Effect of Leakage Reactance........................1144–7
Charp, S. A New Fourier Coefficient Harmonic Analyzer.........................644–9; disc. 649
Chestnut, Harold; R. W. Mayer. Comparison of Steady-State and Transient Performance of Servomechanisms..........................765–77
Churchill, Harold J.; W. P. Boothroyd. A Microwave Communication Relay System............637–41
Ciscel, Ben. Disc.............................1087
Clark, E. C.; B. L. Moore. Performance of Diesel-Electric Rig for Deep Well Drilling.......497–506
Clark, N. R.; S. B. Farnham. Connection Arrangements and Protective Practices for Shunt Capacitor Banks....................1226–30; disc. 1231
Clark, W. R.; W. R. Turner, A. J. Williams, Jr. A Square-Law Power-Level Recorder.......476–82
Clayton, J. M.; E. L. Harder. Transmission Line Design and Performance Based on Direct Lightning Strokes.....................439–46; disc. 448

Cohen, S. B. Disc..........846
Cole, Harold. Disc..........487
Concordia, C. Disc..........166
Conrad, A. G.; E. R. Tribken. A Wide-Range Adjustable-Speed Drive..........789–90
Corbett, James P. Summary of Transformations Useful in Constructing Analogs of Linear Vibration Problems..........661–4
Cornelius, H. A.; B. W. Storer. Loss Measurements Made on Underground-Cable Overhead-Conductor 132-Kv Transmission Line at Carrier Current Frequencies..........597–601
Crabb, V. L.; J. M. Sheadel. Magnetic Heating of Transmission Line Clamps..........1032–5
Crary, S. B. Disc..........1161
Creamer, E. M., Jr.; W. P. Boothroyd. A Time Division Multiplexing System..........92–7

D

Dahlbom, C. A.; A. W. Horton, Jr., D. L. Moody. Application of Multifrequency Pulsing in Switching..........392–6
Davis, S. Disc..........327
deBellis, A. M. Disc..........370
Del Mar, W. A. Disc..........17, 832
Demartini, R. J.; A. F. Lukens. Selection of Electric Drives for Looms..........349–56
Dent, William U. Disc..........1030
DePuy, S. M. Disc..........270
Derr, W. A. A New 2-Signal Synchronous Supervisory Control System..........857–63; disc. 863
Derr, W. A. Disc..........1315
Diemond, C. C.; L. R. Spaulding. A Transient Fault Locator for High-Voltage Transmission Lines..........1005–10; disc. 1011
Dillow, N. E. Disc..........958
Doan, G. H.; Joseph Sticher, R. W. Atkinson, Louis Meyerhoff. High-Pressure Gas-Filled Cable Impregnated with Extra-High-Viscosity Oil..........336–45; disc. 347
Dodds, G. B.; R. S. Smithley. Disc..........716
Dornhoefer, W. J. Self-Saturation in Magnetic Amplifiers..........835–46; disc. 846
Dortort, I. K. Diagnosis of Rectifier Ailments..........1298–1304
Douglass, M. E.; W. H. Morong. A New Thermal Volt-Ampere Demand Meter......79–83; disc. 83
Downing, William C., Jr. A Thermal Converter for Telemetering and Totalizing.....879–84; disc. 884
Downing, Wm. C., Jr. Disc..........800
Duesterhoeft, William C. The Negative-Sequence Reactances of an Ideal Synchronous Machine..........510–13; disc. 513
Duffy, F. H.; J. T. Madill. Shipshaw Relay Protection..........50–5; disc. 57
Dunn, Charles. Disc..........489
Dupuis, H. P. Disc..........620
Dupuis, H. P.; D. C. Harker. Disc..........57
Dwight, H. B. Disc..........20

E

Eager, G. S., Jr.; L. Meyerhoff. A-C Resistance of Segmental Cables in Steel Pipe...816–29; disc. 833
Earle, R. H. Disc..........187
Eason, W. H. Disc..........964
Eason, W. H.; I. B. Johnson, J. W. Kalb, H. A. Peterson. Short-Circuit Currents and Recovery Voltages on Rural Distribution Systems..........930–7; disc. 938
Easton, E. C.; C. H. Thomas. Graphical Determination of Transfer Function Loci for Servomechanism Components and Systems........307–14; disc. 318
Edge, D. R.; G. F. Lincks, W. C. McKinley, J. H. Leh. Overcurrent Investigation on a Rural Distribution System..........915–27; disc. 928
Edge, D. R.; D. D. MacCarthy, D. A. Stann, W. C. McKinley. Lightning Investigation on a Rural Distribution System..........428–37; disc. 437
Elsey, H. M.; C. Lynn. Effects of Commutator Surface Film Conditions on Commutation..........106–12; disc. 112
Elton, M. B. Disc..........489
Elzi, J. A. Disc..........231, 957
Elzi, J. A.; W. R. Brownlee. Disc..........738
Engvall, Lenard R. Some Oscillographic Methods of Investigating Mechanical and Hydraulic Conditions During Operation of Hydroelectric Units..........993–6
Estcourt, V. F. Disc..........1020
Exner, D. W. Protection of the Airplane Main Bus..........979–82

F

Falck, Knute. Disc..........361
Farnham, S. B.; N. R. Clark. Connection Arrangements and Protective Practices for Shunt Capacitor Banks..........1226–30; disc. 1231
Farnham, S. B.; H. B. Ruud. A New Automatic Load Control for Turbine Generators..........1337–42
Farr, Harold K.; P. N. Bosworth. Cable Accessory Design Utilizing New Laboratory Techniques..........1268–73; disc. 1274
Finzi, L. A. Disc..........39
Finzi, L. A.; H. M. McConnell. The Torques of the Synchronous Tie—A Steady-State Analysis..........1147–52
Finzi, L. A.; D. W. VerPlanck, D. C. Beaumariage. Analytical Determination of Characteristics of Magnetic Amplifiers with Feedback..........565–70
Finzi, L. A.; D. W. VerPlanck, D. C. Beaumariage. Disc..........846
Finzi, L. A.; L. C. Wellard. The Geometric Loci of the Synchronous Tie..........168–71
Forrest, E. F.; P. W. Schirmer. Space Code Selector Supervisory System..........1312–15; disc. 1315
Frick, C. W. Disc..........495, 724
Frisch, E.; B. P. Baker. A New 69-Kv Compressed-Air Circuit Breaker..........363–8; disc. 371
Frost, G. R. Counting with Relays..........587–91
Fugill, A. P. Disc..........747
Fujimura, Y. J. Disc..........1010
Fuller, John L. Disc..........214

G

Gault, J. S. Disc..........1131
George, E. E.; H. W. Page, J. B. Ward. Co-ordination of Fuel Cost and Transmission Loss by Use of the Network Analyzer to Determine Plant Loading Schedules..........1152–60; disc. 1162
Gibson, G. P.; G. Grant, T. A. Kauppi, G. L. Moses. Motor Tests Evaluate Thermal Endurance of Class-H Insulation and Silicone Varnish..........1133–8
Gilchrist, A. D. Disc..........153
Giloy, E. W. An Electric Deicing System for Aircraft..........1051–7
Gmeiner, John B. Disc..........241, 808
Goldberg, Edwin A. Disc..........815
Goodrich, R. D., Jr. Disc..........380
Gopalakrishna, H. V.; M. S. Thacker. Design of Auxiliary Circuits of Single-Phase Induction Motors..........1373–9
Gordy, T. D. Disc..........750
Gould, K. E. Equalization of Coaxial Lines...1187–92
Grant, George. Disc..........216
Grant, George; T. A. Kauppi, G. L. Moses, G. P. Gibson. Motor Tests Evaluate Thermal Endurance of Class-H Insulation and Silicone Varnish..........1133–8
Green, H. H.; J. A. G. Oewel, O. Rasmaur. Relaying of Transmission Lines from the New Sunbury Generating Station..........873–7; disc. 877
Gross, Eric T. B. Disc..........895, 914, 1329
Gross, I. W. Disc..........1206
Gross, I. W.; Philip Sporn. Disc..........969
Grosser, E. H. Disc..........1281, 1295, 1336
Grosser, E. H.; H. L. Pedley. Disc..........1273
Grover, Frederick W. Formulas and Tables for the Calculation of the Magnetic Field Components of Circular Filaments and Solenoids..........665–75
Guess, L. T. Disc..........487
Gunnoe, G. H.; B. H. Caldwell. Disc..........212

H

Hadley, W. A. Disc..........20, 334
Hagenguth, J. H. Lightning Stroke Damage to Aircraft..........1036–44; disc. 1046
Hague, F. T. Gas Turbines in Stationary Power Generation..........467–8
Hailes, W. D. Centralized Traffic Control for Railroads..........1167–73
Halfmann, E. S. Disc..........346
Hall, Albert C. Damper Stabilized Instrument Servomechanisms..........299–306; disc. 306
Halperin, Herman. Disc..........19, 333, 1336
Halperin, Herman; C. A. Bauer. Disc..........1296
Halsey, G. H.; V. M. Montsinger. Heating of Transformers Under Short-Circuit Conditions..........947–52; disc. 954
Hampe, G. W.; B. W. Storer. Power Line Carrier for Relaying and Joint Usage. Part I..864–7; disc. 872
Harder, E. L.; J. M. Clayton. Transmission Line Design and Performance Based on Direct Lightning Strokes..........439–46; disc. 448
Harker, D. C.; H. P. Dupuis. Disc..........57
Harrington, Everett J.; E. C. Starr. Deionization Time of High-Voltage Fault-Arc Paths..........997–1003; disc. 1004
Harris, W. A.; R. A. Beers, A. D. Zappacosta. Design of Mobile Radio Communication Equipment for Land-Mobile Services Operating on Frequencies Between 152–174 Megacycles..........1232–9
Harrison, L. P. Disc..........1044
Hart, H. A.; F. J. Vogel. A Small High-Voltage Bushing Design for High Altitudes..........761–4
Hart, Stephen V. Disc..........241
Hatcher, C. T. Disc..........558, 829
Hathaway, C. B. Disc..........217, 224
Haverstick, J. S. A 3,000-Horsepower Gas Turbine Power Plant..........458–60
Hayes, V. J. Disc..........231
Hearn, R. B.; L. A. Weber. B-1 Alarm and Control System for Remote Control of Coaxial Cable Stations..........622–6
Herskind, C. C.; A. Schmidt, Jr.; C. E. Rettig. Rectifier Fault Currents—II..........243–51; disc. 252
Heumann, G. W. Dynamic Braking Control of D-C Series Motors—Experimental Study of Speed-Torque Curves..........560–4
Hewitt, G. W.; G. W. Penney. Electrically Charged Dust in Rooms..........278–82
Hickernell, L. F.; A. A. Jones, C. J. Snyder. Hy-Therm Copper—An Improved Overhead-Line Conductor..........22–7; disc. 29
Higgins, Thomas J.; Glenn Robertson. General Theory and Experimental Confirmation of the Moving Coil Fluxmeter..........897–907; disc. 907
Hill, V. E. Disc..........913
Hinton, C. B. Disc..........1326
Hixson, W. A.; W. G. Hoover. Dielectric Strength of Oil with Variations of Pressure and Temperature..........1047–50
Hoard, B. V.; Lewis Weiner. Matching Electrical Characteristics of McNary Generators and Transformers with Bonneville Power Administration Transmission System..........1022–5; disc. 1027
Holben, W. P.; J. E. Lange. Primary Networks on the Duquesne Light Company System..........704–09; disc. 711
Holtz, F. C. Disc..........77
Hoover, D. B. Disc..........105
Hoover, W. G.; W. A. Hixson. Dielectric Strength of Oil with Variations of Pressure and Temperature..........1047–50
Horton, A. W., Jr.; C. A. Dahlbom, D. L. Moody. Application of Multifrequency Pulsing in Switching..........392–6
Hoth, Daniel F.; Harold M. Pruden. Vibrating Reed Selective Signaling System for Mobile Telephone Use..........387–91
Howard, Alan Two Gas Turbines for Power Generation and Other Applications..........461–5
Hull, Francis M.; W. A. Sinclair. Disc..........558
Hunt, F. B.; H. R. Tomlinson. Operation of a Ground-Fault Neutralizer on a Regulated Distribution System..........1321–6; disc. 1326
Hutchinson, Chase. Disc..........489, 809

I

Imburgia, C. A.; H. P. St. Clair. Disc..........1160

J

Jancke, G. Disc..........1004
Johnson, I. B.; W. H. Eason, J. W. Kalb, H. A. Peterson. Short-Circuit Currents and Recovery Voltages on Rural Distribution Systems.......930–7; disc. 938
Johnson, I. B.; J. W. Kalb. Disc..........181
Johnson, Walter C. Disc..........194, 649
Johnson, Walter C.; B. C. Merrell, R. E. Alley, Jr. Universal Curves for D-C Controllable Reactors..........31–7; disc. 39
Jones, A. A.; L. F. Hickernell, C. J. Snyder. Hy-Therm-Copper—An Improved Overhead-Line Conductor..........22–7; disc. 29

K

Kahn, David A. An Analysis of Relay Servomechanisms..........1079–87; disc. 1088
Kalb, J. W.; W. H. Eason, I. B. Johnson, H. A. Peterson. Short-Circuit Currents and Recovery Voltages on Rural Distribution Systems.......930–7; disc. 938
Kalb, J. W.; I. B. Johnson. Disc..........181
Karr, J. H.; S. S. L. Chang. Armature Iron Losses in Series Motors..........262–4; disc. 265
Kauppi, T. A.; G. Grant, G. L. Moses, G. P. Gibson. Motor Tests Evaluate Thermal Endurance of Class H Insulation and Silicone Varnish....1133–8
Keister, W. M. The Logic of Relay Circuits....571–6
Keller, A. C.; L. G. Bostwick. Vibrating Reed Selectors for Mobile Radio Systems..........383–6
Kenefake, E. W. Disc..........1031
Kessler, William J. The Distinction Between Effective and Circuit Bandwidths..........98–9
Kidd, K. H.; J. R. Leslie. Disc..........1011
Kidder, A. H. Disc..........831
Kidder, A. H.; G. S. Van Antwerp. 69-Kv Pipe-Type Cable Circuits in Philadelphia.....550–6; disc. 559
Kierstead, F. H. Disc..........475
Kierstead, F. H.; J. L. Thomason. Standardization of Reactor Ratings..........955–7; disc. 958
Kimball, A. W. Use of Cumulative and Differential Series Fields in the Parallel Operation of D-C Motors..........1098–1100

Kimbark, Edward W. Disc.................382, 895
Kimberly, E. E. The Field Fluxes of the Shaded-Pole Motor..............................273–7
Kinzer, J. P. Stability of Tandem Regulators in the L-1 Carrier System.........................1179–86
Kirschbaum, H. S.; C. E. Warren. A Method for Designing Pulse Transformers...............971–8
Klingaman, G. W. Reduction of Interference from Radio-Frequency Heating Equipment..............................718–24; disc. 724
Kochenburger, R. J. Disc.....................1087
Komives, L. I. Disc...........1261, 1267, 1273, 1280
Koopman, R. J. W. Operating Characteristics of Two-Phase Servomotors.............319–27; disc. 328
Kron, Gabriel. Stationary Networks and Transmission Lines Along Uniformly Rotating Reference Frames...................................690–6
Kusters, N. L. Disc............................68

L

Lamb, J. F.; D. B. Brandt. The Vector Power Factor of Unbalanced Sinusoidal 3-Phase Circuits..507–09
Lange, J. E.; W. P. Holben. Primary Networks on the Duquesne Light Company System...........................704–09; disc. 711
Lank, W. J. Design and Experience with 4-Kv Primary Network Systems in the Washington Area.............................712–16; disc. 716
Lawton, F. L. Disc............................56
Lebenbaum, Paul, Jr. The Design of D-C Motors for Use in Automatic Control Systems.......1089–94
Leeds, W. M. Disc...........................1066
Leh, J. H.; G. F. Lincks, D. R. Edge, W. C. McKinley. Overcurrent Investigation on a Rural Distribution System.......................915–27; disc. 928
Leitch, John D. The Use of Air-Core Reactors as Fault Limiting Means on High Interrupting Capacity Controllers...................469–74; disc. 475
Lensner, H. W. Disc..................48, 240, 871
Leonard, M. G. Disc..........................488
Leslie, J. R.; K. H. Kidd. Disc...............1011
Leslie, J. R.; J. R. Wait. Detection of Overheated Transmission Line Joints by Means of a Bolometer................................64–8; disc. 69
Levine, D. L.; H. E. Smith. Economic Loading of a Transformer System.............733–8; disc. 739
Lewis, W. A. Disc............................513
Lincks, G. Fred. Disc.........................937
Lincks, G. F.; D. R. Edge, W. C. McKinley, J. H. Leh. Overcurrent Investigation on a Rural Distribution System.......................915–27; disc. 928
Lindsey, R. H.; K. N. Mathes, L. E. Sieffert, H. P. Walker. Methods for Determining the Effects of Contaminants on Electrical Insulation.............................113–18; disc. 118
Linville, T. M.; H. C. Ward, Jr. Solid Short Circuit of D-C Motors and Generators.....119–23; disc. 123
Linville, T. M. Disc..........................225
Linville, T. M.; Mrs. B. O. Buckland. Disc.......218
Litman, Bernard. An Analysis of Rotating Amplifiers.....................................1111–17
Liwschitz, M. M. Differential Leakage of the Different Patterns of a Fractional Slot Winding.............................1129–31; disc. 1132
Lloyd, T. C.; Sheldon S. L. Chang. Saturation Effect of Leakage Reactance...................1144–7
Locanthi, B. N.; G. D. McCann, C. H. Wilts. Application of the California Institute of Technology Electric Analog Computer to Nonlinear Mechanics and Servomechanisms..................652–60
Looms, Selection of Electric Drives for. Demartini Lukens................................349–56
Loving, James J., Jr. Electronic Relay Developments..............................233–40; disc. 242
Lukens, A. F.; R. J. Demartini. Selection of Electric Drives for Looms.........................349–56
Lundry, W. R. Attenuation and Delay Equalizers for Coaxial Lines...........................1174–9
Lundy, R. T. Measuring Commutation with an Indicating Instrument.......................1380–2
Lunge, G. S. Disc..................884, 1315, 1342
Lush, Morley J. Disc..........................907
Lynn, C.; H. M. Elsey. Effects of Commutator Surface Film Conditions on Commutation.106–12; disc. 112
Lynn, C. Disc..............................224

M

Macalpine, William W. Heating of Radio-Frequency Cables..............................283–8
MacCarthy, D. D.; D. A. Stann, D. R. Edge, W. C McKinley. Lightning Investigation on a Rural Distribution System.............428–37; disc. 437
Madill, J. T.; F. H. Duffy. Shipshaw Relay Protection.................................50–5; disc. 57
Mahoney, J. H. Disc..........................750
Mansfield, C. G. Disc.........................333
Marcy, H. Tyler; M. Yachter, J. Zauderer. Instrumen Inaccuracies in Feed-Back Control Systems with Particular Reference to Backlash..........778–88
Markley, W. F.; H. H. Wheeler. Rubber Insulators for Pole Lines...........................195–7
Marshall, F. R.; A. M. Zarem, F. L. Poole. An Electro-Optical Shutter for Photographic Purposes....84–91
Maslin, A. J. Disc............................251
Mason, C. R. A New Loss-of-Excitation Relay for Synchronous Generators.....................1240–5
Mason, V. V. Disc............................952
Mathes, K. N.; L. E. Sieffert, H. P. Walker, R. H. Lindsey. Methods for Determining the Effects of Contaminants on Electrical Insulation.............................113–18; disc. 118
Mayer, Robert W.; H. Chestnut. Comparison of Steady-State and Transient Performance of Servomechanisms...............................765–77
McAdam, Will; A. J. Williams, Jr., W. G. Amey. Wide-Band D-C Amplifier Stabilized for Gain and for Zero.....................811–15; disc. 815
McCann, G. D.; C. H. Wilts, B. N. Locanthi. Application of the California Institute of Technology Electric Analog Computer to Nonlinear Mechanics and Servomechanisms..................652–60
McClinton, A. T. Disc..................118, 1123
McClinton, A. T.; Wade H. Askew. Current Ratings of Aircraft Cable.......................988–92
McClinton, A. T.; E. L. Brancato, R. Panoff. Transient Characteristics of D-C Motors and Generators................................1100–06
McConnell, H. M.; L. A. Finzi. The Torques of the Synchronous Tie—A Steady-State Analysis.....................................1147–52
McGrath, M. H. Disc...................18, 1295
McKean, A. L. Disc..................1267, 1281
McKenzie, D. A. Disc..........................55
McKinley, W. C.; G. G. Auer, R. A. Branflick, L. J. Woodward. Field Tests of Oil Circuit Recloser Substantiate Analytical Co-ordination Method.............................801–08; disc. 810
McKinley, W. C.; G. F. Lincks, D. R. Edge, J. H. Leh. Overcurrent Investigation on a Rural Distribution System.......................915–27; disc. 928
McKinley, W. C.; D. D. McCarthy, D. A. Stann, D. R. Edge. Lightning Investigation on a Rural Distribution System.................428–37; disc. 437
McMillon, A. B. Trolley Coaches Replace Busses.....................................403–06
Meador, J. R. Use of Thyrite in Power Transformers.....................................959–64
Meador, J. R. Disc............................968
Merrell, B. C.; W. C. Johnson, R. E. Alley, Jr. Universal Curves for D-C Controllable Reactors.............................31–7; disc. 39
Merrell, Edwin J. Disc.................345, 1273
Meyerhoff, L.; G. S. Eager, Jr. A-C Resistance of Segmental Cables in Steel Pipe...816–29; disc. 833
Meyerhoff, Louis; Joseph Sticher, G. H. Doan, R. W. Atkinson. High-Pressure Gas-Filled Cable Impregnated with Extra-High-Viscosity Oil.............................336–45; disc. 347
Michelson, E. L. Disc.........................232
Michelson, E. L.; K. M. Smith. A 66-Kv Subtransmission Plan for a Metropolitan Area.............................908–12; disc. 914
Mickley, A. S. The Thermal Movement of Moisture in Soil.........................330–3; disc. 334
Mildner, R. C. The Power Rating of Radio-Frequency Cables.......................289–98; disc. 298
Millar, N. P. A New Expanded Scale A-C Voltmeter.....................................641–3
Miller, R. H. A Proposal for Broad-Band Coupling of Power-Line Carrier Equipment.............................1028–30; disc. 1031
Montgomery, T. B. Disc.......................166
Montsinger, V. M. Disc........................759
Montsinger, V. M.; G. H. Halsey. Heating of Transformers Under Short-Circuit Conditions.............................947–52; disc. 954
Moody, D. L.; C. A. Dahlbom, A. W. Horton, Jr. Application of Multifrequency Pulsing in Switching.....................................392–6
Moore, B. L.; E. C. Clark. Performance of Diesel-Electric Rig for Deep Well Drilling.......497–506
Morgan, W. A. Disc..........................1026
Morgan, W. A.; F. S. Rothe, J. J. Winsness. An Improved A-C Network Analyzer....891–5; disc. 896
Morong, W. H.; M. E. Douglass. A New Thermal Volt-Ampere Demand Meter......79–83; disc. 83
Morris, W. C. One Slip Cycle Out-of-Step Relay Equipment............................1246–8
Morrison, L. W., Jr. Television Terminals for Coaxial Systems.............................1193–9
Moses, Graham Lee; G. Grant, T. A. Kauppi, G. P. Gibson. Motor Tests Evaluate Thermal Endurance of Class-H Insulation and Silicone Varnish.....................................1133–8
Motter, D. P. Commutation of D-C Machines and Its Effects on Radio Influence Voltage Generation.............................491–5; disc. 495
Mundel, A. B. Control Chart Methods Applied to Frequency Response Curves...................424–7
Murray, C. S. Transformer Audio Noise Problems on an Electric Power System........740–7; disc. 751
Murray, C. S. Disc............................751
Myers, E. H. Disc............................495
Myers, Oscar. Codes and Translations.........592–7

N

Narbutovskih, Paul. Disc......................953
Nau, Robert H. Disc...................361, 369
Neher, J. H. The Temperature Rise of Buried Cables and Pipes.........................9–17; disc. 20
Neher, J. H. The Temperature Rise of Cables in a Duct Bank........................540–6; disc. 548
Neher, J. H. Disc............................830
Nelson, J. S. Heat Flow Ratio as a Design Parameter in Thermal Demand Meters.......885–90; disc. 890
Nesbit, D. E. Disc...........................710
Nicholas, J. H. Design Fundamentals of Potheads 69,000 Volts and Below........1263–7; disc. 1267
Nicholas, J. H.; A. E. Papp. A New Technique in the Manufacture of Soldered Porcelain Potheads.............................1258–60; disc. 1261
Noest, John G. The Performance of a Topped Steam Power Plant on Sudden Load Reduction..1348–56
Nye, H. V. Disc.......................362, 370

O

Oewel, J. A. G.; H. H. Green, O. Ramsaur. Relaying of Transmission Lines from the New Sunbury Generating Station..............873–7; disc. 877
Olmsted, Leonard M. Disc......28, 69, 183, 709, 809
Opel, L. G. Limitations in Design of D-C Adjustable Speed Motors..........................1095–8
Opsahl, A. M. Disc....................181, 437

P

Packer, L. C. Die-Cast Rotors for Induction Motors.............................253–60; disc. 261
Packer, L. C. Disc.....................264, 271
Page, H. W.; E. E. George, J. B. Ward. Coordination of Fuel Cost and Transmission Loss by Use of the Network Analyzer to Determine Plant Loading Schedules...................1152–60; disc. 1162
Panoff, Robert; A. T. McClinton, E. L. Brancato. Transient Characteristics of D-C Motors and Generators................................1100–06
Papp, A. E.; J. H. Nicholas. A New Technique in the Manufacture of Soldered Porcelain Potheads.............................1258–60; disc.1261
Parks, C. E. Disc.....................381, 1207
Paschkis, Victor. Comparison of Long-Time and Short-Time Analog Computers....................70–3
Pease, W. M. Disc............................306
Pedley, H. L.; E. H. Grosser. Disc............1273
Peltosalo, A. E. Disc.........................1231
Penney, G. W.; G. W. Hewitt. Electrically Charged Dust in Rooms..........................278–82
Peterson, H. A.; W. H. Eason, I. B. Johnson, J. W. Kalb. Short-Circuit Currents and Recovery Voltages on Rural Distribution Systems.......930–7; disc. 938
Poole, F. L.; A. M. Zarem, F. R. Marshall. An Electro-Optical Shutter for Photographic Purposes...84–91
Porter, L. F. Transformer Manholes and Vaults: Design and Ventilation......................534–9
Pruden, Harold M.; Daniel F. Hoth. Vibrating Reed Selective Signaling System for Mobile Telephone Use...............................387–91
Puchstein, A. F. Disc.........................264
Purcell, W. R. Saving Time in Testing Life of Incandescent Lamps..........................730–2

R

Ramsaur, O.; H. H. Green, J. A. G. Oewel. Relaying of Transmission Lines from the New Sunbury Generating Station..................873–7; disc. 877
Ramsey, Glen. Selenium Rectifiers in Motor Vehicle Power Systems................149–53; disc. 153
Rauth, A. W. Disc.............................56
Raymond, A. E. Disc.........................1045
Reed, R. D.; W. W. Satterlee. Controlled Temperature and Insulation Protection in the Operation of Power Transformers.............753–8; disc. 759
Rehberg, Charles F. Disc......................314
Rettig, C. E.; C. C. Herskind, A. Schmidt, Jr. Rectifier Fault Currents—II..............243–51; disc.252
Richards, H. H. G.; B. O. Austin. Aircraft Carbon-Pile Voltage Regulators, Fundamentals, and Design Improvements...........................983–7
Ricker, E. A. Automatic Grounding and Air-Break Switches for Protection of Transformer Stations.............................851–6; disc. 856
Ridgway, W. Disc...........................1021
Ritchie, A. E. Sequential Aspects of Relay Circuits.....................................571–81

Robertson, Glenn; Thomas J. Higgins. General Theory and Experimental Confirmation of the Moving Coil Fluxmeter........897–907; disc. 907
Roehmann, L. F. Disc........................298
Rorden, Harold L. Disc........................204
Rosenberg, L. T. A Semiempirical Approach to Voltage Dip—With Suddenly Applied Loads on A-C Generators........................160–6; disc. 167
Rothe, F. S.; W. A. Morgan, J. J. Winsness. An Improved A-C Network Analyzer....891–5; disc. 896
Rudenberg, Reinhold. Nonharmonic Oscillations as Caused by Magnetic Saturation...........676–85
Rueggeberg, W.; J. B. Whitehead. The Measurement of Dielectric Loss at High Frequencies and Under Changing Temperature..................520–4
Runciman, A. S. Disc..........................68
Ruud, H. B.; S. B. Farnham. A New Automatic Load Control for Turbine Generators..........1337–42
Ryan, J. E. Prediction of Ultimate Temperature Rise from Early Heat Run Data...............1164–6
Rydbeck, V. A. Disc..........................716

S

St. Clair, Harry P. Disc..................1067, 1231
St. Clair, H. P.; C. A. Imburgia. Disc..........1160
Sand, Edward. Disc..........................224
Sand, Edward J.; Jack H. Sheets. Development and Application of Electric Heating to Deicing of Aircraft Propellers..........................1068–72
Satterlee, W. W.; R. D. Reed. Controlled Temperature and Insulation Protection in the Operation of Power Transformers.............753–8; disc. 759
Saunders, Robert M. The Dynamoelectric Amplifier—Class A Operation......................1368–73
Schaefer, L. C.; C. G. Veinott. Fundamental Theory of Inherent-Overheating Protection Under Running Overload Conditions...........266–70; disc. 272
Schart, W. J. Disc..........................1087
Schetky, L. McDonald; L. P. Winsor, R. A. Wyant. Instrumentation for the Evaluation of the Stability of the Welding Arc........................525–33
Schifreen, C. S. Disc..........................1273
Schirmer, P. W.; E. F. Forrest. Space Code Selector Supervisory System...........1312–15; disc. 1315
Schirmer, P. W.; J. J. Winsness. Disc............863
Schmidt, A., Jr.; C. C. Herskind, C. E. Rettig. Rectifier Fault Currents—II.........243–51; disc. 252
Schwendner, A. F.; Wayne C. Astley. Governors and the AIEE-ASME Governor Performance Specification..............................1330–2
Scott, F. M. Disc......................112, 123, 216
Scott, Hoyt S. Measurements of Erythemal Energy.................................725–29
Scoville, M. E. Disc..........................1206
Seely, R. E. Disc..........................271
Seelye, Howard P. A Convenient Method for Determining Generator Reserve...............1317–20
Seelye, Howard P. Disc..........................487
Shankle, D. F.; H. G. Barnett. Disc.............914
Shanklin, G. B. Disc......................346, 558
Sheadel, J. M.; V. L. Crabb. Magnetic Heating of Transmission Line Clamps................032–5
Sheets, Jack H.; E. J. Sand. Development and Application of Electric Heating to Deicing of Aircraft Propellers..............................1068–72
Shores, R. B. Disc..........................369
Short, H. D. A Theoretical and Practical Approach to the Design of High-Voltage Cable Joints.................................1275–80; disc. 1282
Short, H. D. Disc..........................1296
Sieffert, L. E.; K. N. Mathes, H. P. Walker, R. H. Lindsey. Methods for Determining the Effects of Contaminants on Electrical Insulation.................................113–18; disc. 118
Silsbee, F. B. Disc..........................1046
Sinclair, W. Allistair; F. M. Hull. Disc..........558
Skeats, W. F. Disc..........................370
Skeats, W. F.; A. C. Boisseau, B. W. Wyman. Insulator Flashover Deionization Times as a Factor in Applying High-Speed Reclosing Circuit Breakers.................................1058–66; disc. 1066
Slemon, Gordon R. Radio Influence from High-Voltage Corona..................198–204; disc. 205
Smith, Frank V. Disc......................19, 547
Smith, H. B. Disc..........................1161
Smith, H. E.; D. L. Levine. Economic Loading of a Transformer System.............733–8; disc. 739
Smith, J. J.; P. L. Alger. A Derivation of Heaviside's Operational Calculus Based on the Generalized Functions of Schwartz..........939–44; disc. 945
Smith, K. M.; E. L. Michelson. A 66-Kv Subtransmission Plan for a Metropolitan Area.................................908–12; disc. 914
Smith, S. R., Jr. Disc..........................809
Smithley, R. S.; G. B. Dodds. Disc...............716
Snyder, C. J.; L. F. Hickernell, A. A. Jones. Hy-Therm Copper—An Improved Overhead-Line Conductor.................................22–7; disc. 29
Sonnemann, W. K. Novel Test Circuits for Protective Relays..............................1249–54
Spaulding, L. R. Disc..........................1031
Spaulding, L. R.; C. C. Diemond. A Transient Fault Locator for High-Voltage Transmission Lines.................................1005–10; disc. 1011
Specht, T. R.; E. C. Wentz. Disc.................38
Spencer, H. H. Power Supplies for Coaxial Systems.................................627–30
Sporn, Philip. Disc......................466, 1067
Sporn, Philip; I. W. Gross. Disc.................969
Squires, R. B. Disc..........................895
Staats, G. W. Disc..........................166
Stann, D. A.; D. D. MacCarthy, D. R. Edge, W. C. McKinley. Lightning Investigation on a Rural Distribution System............428–37; disc. 437
Starr, E. C.; E. J. Harrington. Deionization Time of High-Voltage Fault-Arc Paths.................................997–1003; disc. 1004
Steinberg, M. J. Disc..........................1160
Stephens, D. S. Lightweight Aircraft Transformers.................................1073–8
Stevens, R. F. Disc..........................42
Sticher, Joseph; G. H. Doan, R. W. Atkinson, Louis Meyerhoff. High-Pressure Gas-Filled Cable Impregnated with Extra-High-Viscosity Oil.................................336–45; disc. 347
Stoelting, H. O. Disc..........................186
Stokes, H. E. Disc..........................215
Storer, B. Wade; H. A. Cornelius. Loss Measurements Made on Underground-Cable Overhead-Conductor 132-Kv Transmission Line at Carrier Current Frequencies..............................597–601
Storer, B. W.; G. W. Hampe. Power Line Carrier for Relaying and Joint Usage. Part I.864–7; disc. 872
Storm, H. F. Some Fundamentals of D-C Controlled Reactors with Resistive Load.............188–94
Storm, H. F. Disc......................37, 847
Streiff, A. Disc..........................489
Sulzer, P. G.; E. A. Walker, A. H. Waynick. Polar Vector Indicator........................154–9
Sutton, Howard J. Line Tuning Equipment Used With Coaxial Cable for Carrier Current Installation on Power Lines.....................44–8; disc. 49

T

Tallent, R. J. Disc..........................381
Taylor, P. L. Disc..........................369
Teague, Wm. L. Disc..........................969
Terrant, F. R. Disc..........................214
Terwilliger, D. D. Disc..........................318
Thacker, M. S.; H. V. Gopalakrishna. Design of Auxiliary Circuits of Single-Phase Induction Motors..............................1373–9
Thomas, C. H.; E. C. Easton. Graphical Determination of Transfer Function Loci for Servomechanism Components and Systems........307–14; disc. 318
Thomas, E. R. Disc................1273, 1281, 1295
Thomason, J. L.; F. H. Kierstead. Standardization of Reactor Ratings................955–7; disc. 958
Thompson, C. S. Application of Experimental Test Procedures and Methods of Analysis of Results to Research Problems in Magnetic Recording..407–17
Tomlinson, H. R. Disc..........................181
Tomlinson, H. R.; F. B. Hunt. Operation of a Ground-Fault Neutralizer on a Regulated Distribution System........................1321–6; disc. 1326
Tornquist, E. L.; I. L. Wade. Determining Incremental Regulation and Dead Band of Governors on Steam Turbine Generators Under System Load Conditions..............................1343–7
Travers, Paul. Disc..........................315
Tribken, E. R.; A. G. Conrad. A Wide-Range Adjustable-Speed Drive........................789–90
Turner, W. R.; W. R. Clark, A. J. Williams, Jr. A Square-Law Power-Level Recorder........476–82

U

Umphrey, D. M. Disc..........................1004

V

Van Antwerp, G. S.; A. H. Kidder. 69-Kv Pipe-Type Cable Circuits in Philadelphia....550–6; disc. 559
Veinott, C. G.; L. C. Schaefer. Fundamental Theory of Inherent-Overheating Protection Under Running Overload Conditions...........266–70; disc. 272
VerPlanck, D. W.; L. A. Finzi, D. C. Beaumariage. Analytical Determination of Characteristics of Magnetic Amplifiers with Feedback...........565–70
VerPlanck, D. W.; L. A. Finzi, D. C. Beaumariage. Disc..........................846
Vikoren, O. B. Disc..........................371
Vogel, F. J. Extremes in the Insulation Co-ordination of Oil Insulated Transformers.....965–8; disc. 970
Vogel, F. J. Disc..........................759
Vogel, F. J.; H. A. Hart. A Small High-Voltage Bushing Design for High Altitudes..............761–4
Von Voigtlander, F. Disc......................153, 751

W

Wade, I. L.; E. L. Tornquist. Determining Incremental Regulation and Dead Band of Governors on Steam Turbine Generators Under System Load Conditions..............................1343–7
Wait, J. R.; J. R. Leslie. Detection of Overheated Transmission Line Joints by Means of a Bolometer.................................64–8; disc. 69
Waite, G. G. Disc......................77, 83
Walker, E. A.; A. H. Waynick, P. G. Sulzer. Polar Vector Indicator........................154–9
Walker, H. P.; K. N. Mathes, L. E. Sieffert, R. H. Lindsey. Methods for Determining Effects of Contaminants on Electrical Insulation.113–18; disc. 118
Wall, H. R. Disc..........................912
Ward, H. C., Jr.; T. M. Linville. Solid Short Circuit of D-C Motors and Generators...119–23; disc. 123
Ward, H. M. Power Supplies for Microwave Relay Systems..............................631–6
Ward, J. B. Equivalent Circuits for Power-Flow Studies......................373–80; disc. 382
Ward, J. B.; E. E. George, H. W. Page. Coordination of Fuel Cost and Transmission Loss by Use of the Network Analyzer to Determine Plant Loading Schedules..................1152–60; disc. 1162
Warren, C. E.; H. S. Kirschbaum. A Method for Designing Pulse Transformers................971–8
Warrington, A. R. Van C. Graphical Method for Estimating the Performance of Distance Relays During Faults and Power Swings........608–20; disc. 621
Washburn, S. H. Relay "Trees" and Symmetric Circuits..............................582–6
Watchorn, C. W. Disc..........................1161
Waynick, A. H.; E. A. Walker, P. G. Sulzer. Polar Vector Indicator........................154–9
Weber, Laurance A.; R. B. Hearn. B-1 Alarm and Control System for Remote Control of Coaxial Cable Stations..............................622–6
Weiner, Lewis; B. V. Hoard. Matching Electrical Characteristics of McNary Generators and Transformers with Bonneville Power Administration Transmission System..........1022–5; disc. 1027
Welchel, C. C. Disc..........................1021
Wellard, L. C.; L. A. Finzi. The Geometric Loci of the Synchronous Tie.....................168–71
Wenner, Frank. Disc..........................907
Wentz, E. C.; T. R. Specht. Disc.................38
Wheeler, H. H.; W. F. Markley. Rubber Insulators for Pole Lines..............................195–7
Whitehead, E. R. Disc..........................968
Whitehead, J. B.; W. Rueggeberg. The Measurement of Dielectric Loss at High Frequencies and Under Changing Temperature............520–4
Wild, Earle. Disc..........................689
Williams, A. J., Jr.; W. G. Amey, Will McAdam. Wide-Band D-C Amplifier Stabilized for Gain and for Zero......................811–15; disc. 815
Williams, A. J., Jr.; W. R. Clark, W. R. Turner. A Square-Law Power-Level Recorder........476–82
Williams, G. D.; F. H. Busch. Surge Protection in a Modern Watt-Hour Meter.........74–7; disc. 78
Wilson, N. E. Transients in 2-Phase Synchronous Machines..............................1360–7
Wilson, W. A. Disc..........................465
Wilts, C. H.; G. D. McCann, B. N. Locanthi. Application of the California Institute of Technology Electric Analog Computer to Nonlinear Mechanics and Servomechanisms..................652–60
Winegartner, C. E. Disc..........................362
Winegartner, C. E.; H. E. Bonheimer. A-C Tripping and Reclosing of Line Sectionalizing Circuit Breakers..............................1357–9
Winsness, J. J.; W. A. Morgan, F. S. Rothe. An Improved A-C Network Analyzer....891–5; disc. 896
Winsness, J. J.; P. W. Schirmer. Disc............863
Winsor, Lauriston P.; L. M. Schetky, R. A. Wyant. Instrumentation for the Evaluation of the Stability of the Welding Arc......................525–33
Wiseman, Robert J. Disc..............546, 556, 832
Witzke, R. L. Voltage-Recovery Characteristics of Distribution Systems.............172–80; disc. 187
Witzke, R. L. Disc..........................938
Wollaston, F. O. Transient Temperature Phenomena of 3-Conductor Cables........1284–95; disc. 1297
Woodrow, C. A. Disc..........................913
Woods, Thomas J. Industrial Plants Prefer Diesel-Electric Switching Locomotives..........697–700
Woodward, L. J.; G. G. Auer, R. A. Branflick, W. C. McKinley. Field Tests of Oil Circuit Recloser Substantiate Analytical Co-ordination Method.................................801–08; disc. 810
World, Richard W. Disc..........................620
Wray, G., Jr. Disc..........................748
Wyant, Robert A.; L. P. Winsor, L. M. Schetky. Instrumentation for the Evaluation of the Stability of the Welding Arc............................525–33
Wyatt, K. S. Disc..........................29
Wyman, B. W.; A. C. Boisseau, W. F. Skeats. Insulator Flashover Deionization Times as a Factor in Applying High-Speed Reclosing Circuit Breakers.................................1058–66; disc. 1066

Wyman, B. W.; E. J. Casey. A Manually-Operated Spring Mechanism for Medium Voltage Oilless Circuit Breakers...............357–61; disc. 362

Y

Yachter, M. Disc.............................315

Yachter, Morris; H. T. Marcy, J. Zauderer. Instrument Inaccuracies in Feed-Back Control Systems with Particular Reference to Backlash......778–88

Z

Zappacosta, A. D.; R. A. Beers, W. A. Harris Design of Mobile Radio Communication Equipment for Land-Mobile Services Operating on Frequencies Between 152–174 Megacycles.............1232–9

Zarem, A. M.; F. R. Marshall, F. L. Poole. An Electro-Optical Shutter for Photographic Purposes....84–91

Zauderer, Jerome; H. T. Marcy, M. Yachter. Instrument Inaccuracies in Feed-Back Control Systems with Particular Reference to Backlash......778–88